

Surge Protection Anthology

All Papers – Part 2

Development of Standards – Reality Checks

FOREWORD

The papers included in this Part 2 of the Anthology report on the development of standards concerned with surges in low-voltage AC power, bringing “reality checks” based on field performance of surge-protective devices, which in some cases raise questions on the validity of the requirements promulgated in these standards, or in other cases confirm the validity of these standards. The pre-1985 papers were copyrighted by the respective publishers who graciously gave permission for reprinting. The post-1985 papers were published under the auspices of the National Institute of Standards and Technology and are therefore in the public domain. The citations of Annex A of Part 2 were collected for the working group that developed the IEEE SPD Trilogy (C62.41.1TM-2002; C62.41.2TM-2002; and C62.45TM-2002) – but do not purport to be an exhaustive listing. Although acknowledged and appreciated, for obvious copyright limitations, these 12 papers from other researchers cannot be reprinted here.

CONTENTS

Transient control levels: A proposal for insulation coordination in low-voltage systems (1976)
Transient control level philosophy and Implementation – Part 1: Reasoning behind the philosophy (1977)
A guideline on surge voltages in AC power circuits rated up to 600 V (1979)
The development of a guideline on surge voltages in low-voltage AC Power (1979)
Varistor versus environment: Winning the rematch (1986)
Real, realistic ring waves for surge testing (1991)
Incompatibility between the 100/1300 surge test and varistor failure rates (1991)
Testing varistors against the VDE 0160 standard (1991)
Standards: Transnational Aspects (1991)
Validating surge test standards by field experience: High-energy tests and varistor performance (1992)
Applying reality checks to standards on the surge environment (1996)
Using incandescent lamp failure levels for assessment of the surge environment (1997)
Driving high surge currents into long cables: More begets less (1997)
Developing a consumer-oriented guide on surge protection (1997)
The effect of neutral earthing practices on lightning current dispersion in a low-voltage installation (1998)
The dilemma of surge protection vs. overvoltage scenarios: Implications for low-voltage SPDs (1998)
The fallacy of monitoring surge voltages: SPDs and PCs Galore ! (1999)
On the dispersion of lightning current after a direct flash to a building (2000)
A new IEC standard on the measurement of power quality parameters (2000)
The Trilogy update of IEEE C62.41 (2000)
The role and stress of surge-protective devices in sharing lightning current (2002)
New IEEE standards foster next-generation system compatibility (2002)

TRANSIENT CONTROL LEVELS:

A PROPOSAL FOR INSULATION COORDINATION IN LOW-VOLTAGE SYSTEMS

Transient Control Levels: A proposal for Insulation Coordination in Low-Voltage Systems

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Significance:

Part 1 Development of standards – Reality checks
Part 6 Textbooks and tutorial reviews

One of the first papers addressing the issues of surge protection in low-voltage AC power circuits, making a proposal for a departure from the traditional unidirectional and separate 1.2/50 and 8/20 waveforms, on the basis of the results of monitoring the occurrence of surges in these circuits. Nevertheless, the concept is emphasized that surge test waveforms should not attempt to duplicate the environment, but only to apply “representative” waveforms and levels that will demonstrate the equipment withstand capability.

The proposal also included the concept of establishing **first** a level of surges that will not be exceeded, thanks to the application of appropriate SPDs, and **only then** designing equipment that will withstand level higher than the allowable level of surges. This was nothing new, having been applied successfully in the high-voltage utility environment. However, the proposal was new for the low-voltage community.

Unfortunately, the *fait accompli* of equipment being designed and placed on the market without such coordination prevented application of that proposal. Thus, industry is left with the situation where equipment failures under surge conditions can occur, after which remedies must be found as retrofits.

In 1976, the following statement appeared in the paper and should be kept in mind when questions arise on the selection of “representative waveforms” in IEEE Std C62.41.2:

These BIL amplitudes, while assigned somewhat arbitrarily, were (and are) kept in touch with reality by the fact that equipment designed in accordance with standards do not fail when exposed to surges produced by lightning, in contrast to equipment designed prior to the development of the philosophy of insulation coordination and the establishment of standard BILs.

TRANSIENT CONTROL LEVELS

A Proposal for Insulation Coordination in Low-Voltage Systems

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ABSTRACT

Failure and circuit upset of electronic equipment due to transients is a problem now and is one which has promise of becoming more of a problem in the future as trends continue toward miniaturization and circuit complexity. Protection methods are used more or less extensively and often haphazardly.

At present, there does not appear to be a clear approach toward achieving compatibility between the transient withstand capability of devices and the transients to which such devices are exposed. A more scientific approach is needed to guide manufacturers and users of equipment.

The purpose of this paper is to promote a concept of transient coordination for electronic and other low-voltage equipment through the establishment of a system of Transient Control Levels, similar to the concept of Basic Insulation Levels so successfully used for many years in the electric power industry. Specific suggestions for possible Transient Control Levels and standard test wave shapes are made, in order to promote wide discussion as to whether these waveforms and levels are the best that can be developed toward good transient coordination for the electronic industry.

INTRODUCTION

Failure and circuit upset of electronic equipment due to transients is a problem now and is one which has promise of becoming more of a problem in the future as trends continue toward miniaturization and circuit complexity. At present, there does not appear to be a clear approach toward achieving compatibility between the transient withstand capability of devices and the transients to which such devices are exposed. This situation appears somewhat as illustrated on Figure 1. A similar situation prevailed many years ago in the electric power industry. Transients produced by lightning frequently caused failure of such vital and expensive power equipment as transformers and generators. Those transient problems were solved by engineering design guided by the concept of insulation coordination and the establishment of a series of Basic Insulation Levels (BIL's).

The purpose of this paper is to promote a concept of transient coordination for electronic and other low-voltage equipment through the establishment of a system of Transient Control Levels (TCL's), similar to the concept of BIL's so successfully used for many years in the electric power industry. In the following sections, specific suggestions for possible standard Transient Control Levels and standard test wave shapes will be made. While the waveforms here suggested are chosen somewhat arbitrarily, they are well grounded in physical reality. The purpose of making such suggestions is to promote wide discussion as to whether these waveforms and levels are the best that can be developed, or if indeed the establishment of such standards is the best way to promote good transient coordination for the electronics industry. The ultimate purpose of any system of transient coordination would be to achieve greater product reliability at minimum cost to the user.

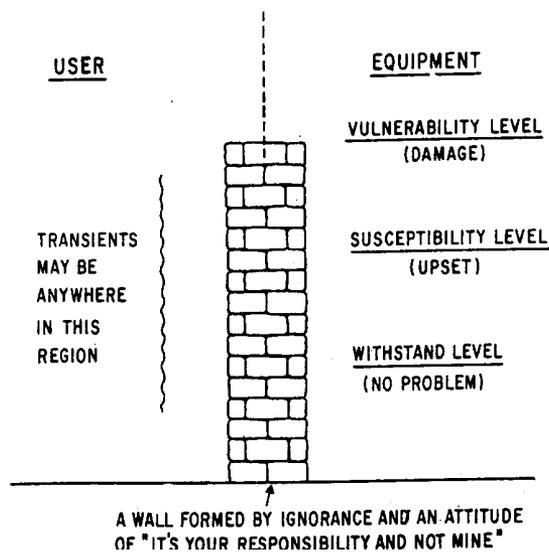


Fig. 1. The present situation.

AN EXAMPLE OF THE PROBLEM

TCL concepts would be of benefit to all users of electronic and other low voltage equipment, such as railroad, telephone, power, oil industry, aircraft, and high frequency communications. The source of transients to which equipment is exposed may be either external (lightning and power system switching) or internal (switching of inductive loads, contactor restrikes or cross talk from adjacent circuits). While the concept of TCL's is intended to apply to the full spectrum of frequencies and voltages (DC, 120 V, 60 Hz AC, 400 Hz) the problem of transient coordination will

Paper F 75 466-3, recommended and approved by the IEEE Surge Protective Devices Committee of the IEEE Power Engineering Society for presentation at IEEE PES Summer Meeting, San Francisco, Calif., July 20-25, 1975. Manuscript submitted February 3, 1975; made available for printing April 28, 1975.

here be illustrated by discussion of 120 volt AC systems intended for consumer and residential use. During the introduction of electronic equipment into consumer appliances and other residential use, the importance of *transient* coordination was not always sufficiently recognized. In some cases, excessive failure rates occurred as a result of transients having amplitudes greater than the *withstand* level of the equipment.

In residential circuits, transients can occur from two main sources: internally, from the switching of appliances, and externally, most typically from the effects of lightning. One study of internally generated transients¹ has indicated that in about three percent of U.S. households transients greater than 1200 volts occur one or more times per week. Several studies have been made of externally generated transients. One such study² indicates two percent of recorded transients exceed 1500 volts. The data also indicate that at the location studied, approximately two surges per year would exceed 1000 volts. Field experience¹ indicated that a 100:1 drop occurred in the failure rate of clock motors when the withstand level was increased from 2000 to 6000 volts. These data indicate that the exposure rate to surges of 2000-volt amplitude was sufficient to be of concern, but that surges exceeding 6000 volts were quite rare, at least on a national basis. Another study³ showed that during two weeks of monitoring in a lightning-prone area, several surges exceeding 2000 volts were recorded, with the maximum recorded being 5600 volts. Experience with field trials of Ground Fault Circuit Interrupters sponsored by NEMA and the Underwriters' Laboratory⁴, when correlated with the known nuisance trip level of the devices and the observed number of trips⁵, would indicate an occurrence frequency of perhaps one surge per 7 years above 2000 volts per household.

Most residential wiring systems are constructed in such a manner that the various wiring boxes will flash over if they are exposed to surges greater than 5 to 10 kV. This means that the amplitude distribution will be chopped at 5 to 10 kV.

Based on these admittedly scattered and tentative numbers, it appears that the typical residential circuit will be exposed to surges of magnitude and frequency of occurrence as illustrated in Figure 2.

The magnitude of the transients produced on 120 volt power lines, however, is not of importance except as it relates to the vulnerability level of the equipment connected to such lines. "Vulnerability" is defined here as the level that causes an irreversible and undesirable change (usually failure) in a device. A corollary term is susceptibility, or that level which causes temporary malfunction of the device. The susceptibility level cannot, by definition, be higher than the vulnerability level. Rectifier diodes and similar semiconductors do not have any particular susceptibility level; they either fail or do not fail when exposed to transients. Active semiconductor devices or a control system operated by a mini-computer system might be a different story. It is quite possible

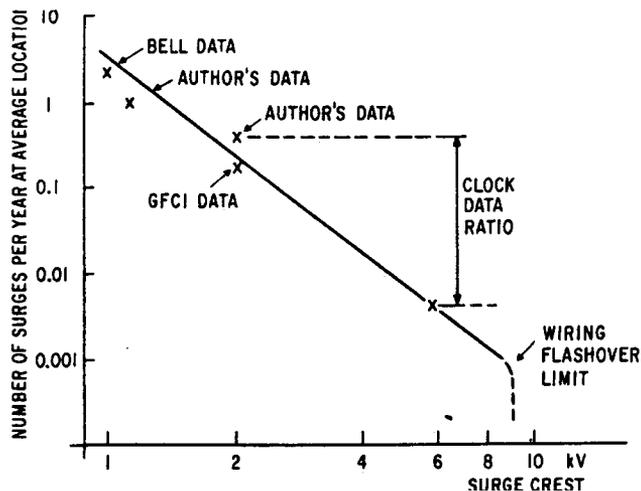


Fig. 2. Exposure of residential circuits to surge (Number of surges vs highest surge at any one location)

that transients of a low level interfere with the operation of the mini-computer, causing it to give incorrect results without causing permanent physical damage. The vulnerability level of such a mini-computer will be higher than the susceptibility level. Both levels must be higher than the normal operating level of the computer logic elements or input/output terminals.

The transient breakdown level or vulnerability of semiconductors is not presently a part of any industry accepted rating system. The vulnerability level is furthermore not inherently related to the normal operating voltage or peak inverse voltage (PIV) level. As examples, consider the data of Table I. During this investigation, power diodes were subjected to unidirectional transient voltages cresting in a few microseconds. The voltages at which failure occurred are seen to have little correlation to the nominal PIV rating.

Similar data have been accumulated for many semiconductors, particularly when semiconductors are exposed to very short transients, characteristic of those produced by nuclear weapons (NEMP). Such information has not been widely reported.

TABLE I
Transient Vulnerability Levels
Typical 1A Silicon Diodes

Diode Number	PIV Rating Volts	Failure Level Under Reverse Impulse* Volts
1	200	1100 - 1500
2	400	1400 - 1500
3	600	1400 - 1600

*Breakdown observed when exposed to a unidirectional surge rising at 1000 volts per microsecond.

Clearly, surges occur with amplitudes greater than the vulnerability of the indicated semiconductors. The frequency of occurrence of such damaging surges, while small on an individual basis, may be unacceptably high on a product line. The transient amplitudes, of course, could be reduced by the use of suitable protective devices. Likewise, the vulnerability levels of the diodes to transients could be raised. Some questions now present themselves, all having to do with the question of who should assume what part of the job of providing transient coordination.

- a) Should it be the responsibility of the user to control transients to levels that do not damage equipment supplied by vendors?
- b) Should it be the responsibility of the manufacturer to provide equipment that will not be damaged by the naturally occurring transients?
- c) If it is the responsibility of the user to control transients, to what level should he control them — the published operating levels (in this case the published PIV levels) or some other level higher than the operating level but below the vulnerability level?
- d) If it is the responsibility of the vendor to provide surge-proof equipment, what level of transient voltage and transient energy must he anticipate?

Similar questions can be asked for all product lines: consumer, industrial, and military, and at all levels of operating voltage.

INSULATION COORDINATION IN THE ELECTRIC POWER INDUSTRY

Similar questions occurred many years ago during the development of the electric power industry at a time when the art of designing equipment to withstand the effects of lightning was in its infancy. The nature of the transients, the level of insulation to be used, or what should be expected of the designers of transmission lines and lightning arresters was not clear.

Those transient problems have largely been eliminated today by proper engineering design on a system-wide basis. The evolution of insulation coordination in the electric power industry, while it can be only very briefly described here, may be of benefit to the electronic industry.

First, the type of transients produced by lightning on transmission lines, their magnitude and wave shape were measured. This was not easy in the days of cold-cathode oscilloscopes employing 50 kV accelerating voltages. Even today with vastly improved instrumentation, such investigations are expensive and time-consuming to make.⁶ Yet, on the basis of very limited testdata, a standard voltage test wave was derived, the familiar $1.5 \times 40 \mu\text{s}$ wave. Similar investigations in other countries led to the establishment in Europe of the $1 \times 50 \mu\text{s}$ impulse wave. International standardizing activities have now produced the $1.2 \times 50 \mu\text{s}$ impulse wave, a test wave used throughout the world for

coordination of insulation protection. It was never pretended, however, that naturally occurring surges were of this type, only that the rise and fall times of the natural surges were in the vicinity of the above values.

The next stage in the process of insulation coordination was the establishment of a series of standard test and design levels, BIL's. For example, equipment designed for operation on 115-kV systems was assigned a BIL of 550 kV. The designer of equipment to be used on 115 kV systems then was required to provide an insulation structure that would withstand 550 kV. The level of 550 kV was derived on the premise that existing lightning arresters could be used to control the transients applied to that apparatus to less than 550 kV. The proper design of the insulation system was next demonstrated by subjecting the apparatus in the laboratory to a surge of $1.5 \times 40 \mu\text{s}$ wave shape and a peak amplitude of 550 kV. Frequently it was part of the purchase agreement that the equipment had to successfully pass the laboratory test. If the equipment failed, it had to be rebuilt or re-designed. Conversely, it became the responsibility of the user to insure that no surge greater than 550 kV was ever applied to the apparatus.

As a result, power equipment achieves its resistance to lightning-induced transients not so much by being designed to the threat that might be posed by lightning, but by the threat that will be posed by an acceptance test. This acceptance test does not subject the equipment to transients having the complex wave shapes produced by lightning, but instead to transients having elementary wave shapes that can be produced by basically simple test apparatus. Neither does the acceptance test subject the equipment to transients of the amplitude produced by lightning. However, it subjects the equipment to transients of amplitude consistent with the capabilities of existing surge-protective devices.

These amplitudes, the BIL's while assigned somewhat arbitrarily, were (and are) kept in touch with reality by the fact that equipment designed in accordance with standards does not fail when exposed to surges produced by lightning, in contrast to equipment designed prior to the development of the philosophy of insulation coordination and the establishment of standard BIL's.

The test and design levels, the BIL's, are not necessarily fixed. As better protective devices are developed, the levels may be lowered so that reliable equipment can be built at lower cost.

Electronic and control equipment, on the other hand, is all too often designed, built, and delivered before the existence of a transient threat is recognized. If transients turn out to endanger the equipment, there may be no adequate surge protective devices. There may, in fact, not be any satisfactory answer to the problem posed by transients.

THE TRANSIENT CONTROL LEVEL CONCEPT

One way in which transient compatibility might be achieved in the electronics industry is to establish a transient coordination system similar in concept to the BIL

system, but of a nature more adapted to the requirements of electronic and control equipment.

In this paper, such a concept is called the Transient Control Level (TCL)* concept. Specifically, it is hereby proposed:

- a) That there be defined for electronic equipment (and other low-voltage equipment) a standard transient voltage similar in concept to, but different in wave shape from the $1.2 \times 50 \mu\text{s}$ wave used in coordination of insulation in high-voltage power apparatus.
- b) That there be defined for electronic (and other low-voltage) equipment a series of TCL's similar in concept to the BIL's.
- c) That a start be made on assigning one of these standard levels to individual electronic components and electronic devices.
- d) That individual protective devices be rated in terms of their ability to control transients to levels no greater than, and preferably lower than, one of the above levels.
- e) That equipment and procedures be developed by which equipment may be tested by vendors to determine which TCL is appropriate to assign to individual components and equipment.
- f) That TCL's begin to be used in purchase specifications.
- g) That such equipment and procedures be used by purchasers to evaluate vendor-supplied equipment to determine its compliance with such purchase specifications.
- h) That such TCL's begin to appear in regulatory specifications for consumer apparatus in which the consumers cannot make the appropriate tests or prepare appropriate specifications.

Suggested TCL Voltage Wave Shape

The wave shape suggested for the TCL concept (with the understanding that discussion and presentation of alternatives is actively encouraged) is shown on Figure 3. Shown are both proposed open-circuit voltage and short-circuit current waveforms, since the question of the impedance of the source from which voltage surges derive must ultimately be considered. These shapes are different from the long-established $1.2 \times 50 \mu\text{s}$ wave employed in the BIL rating system for electric power apparatus because none of the recorded transients exhibited this type of wave shape on 120-volt AC circuits. The type of transient most frequently recorded appeared of an oscillatory nature, very strongly damped, and in a frequency range between 100 and 500 kHz.

Independent work on the resonant frequency of power systems previously indicated a range of 150 to 500 kHz as being the natural frequency of typical residential systems.⁷ Other investigations indicate that a

lower limit of 5 kHz might be more typical.⁸ Thus, it appears that the observed transients are not at all typical of lightning surges propagated directly into the system but are rather the response of the power system to an initial excitation caused by a nearby lightning stroke. The internally generated transients due to switching operations typically are of the same basic type as those produced by the indirect effects of lightning. The observed transients are in each case more nearly the result of the natural oscillatory response of the local wiring system, in this case the wiring system of typical residences. Similar surge wave shapes have been encountered in a wide variety of other systems, ranging from airplanes to space booster rockets.^{9,10} Typical examples of recorded transient wave shapes are given in the Appendix. The great bulk of the recorded transients exhibit a faster front time and shorter decay time than do the transients produced by lightning on high-voltage power lines, the $1.2 \times 50 \mu\text{s}$ type of wave.

Switching transients in air break contacts (internally generated transients) can produce rise times in the order of 10 to 100 ns. Although this steepness attenuates rapidly with distance, the typical front time is still less than $1.2 \mu\text{s}$. For some types of devices (rectifier diodes) the wave shape is of secondary importance, with only the peak magnitude being important. For other types of apparatus (inductive devices such as motors), the front time, or more correctly the rate of change, is of importance equal to that of the peak magnitude. In still other types of devices (surge protective devices), the total energy content of the surge is of most importance.

Current Wave Shapes and Source Impedances

The characteristics of short-circuit current wave shapes are less well known than those of open-circuit voltage. The short-circuit current is of importance both for evaluation of surge protective devices and for equipment of low input impedance such as lower voltage semiconductor devices. In any discussion of test wave shapes and test levels, it is important to recognize the natural response of the device in the test. It is inappropriate to prepare a specification that implies that a specified voltage must be developed across a device of low input impedance, such as a spark gap after it has broken down, or to seemingly require that a specified short-circuit current be produced through a high input impedance, such as the line-to-ground insulation of a relay coil. The characteristics of short-circuit currents are poorly defined because the impedance of the circuits from which transients are produced is poorly defined or unknown.

For purposes of discussion, it is suggested that two different types of impedance be considered, one independent of frequency (resistive source impedance or classical surge impedance, $Z = \sqrt{L/C}$), and one of simple inductive source impedance. The waveform shown on Figure 3b assumes a source impedance of

* The TCL concept was first proposed by one of the authors (F. A. Fisher) in regard to electronic equipment on the Space Shuttle.¹²

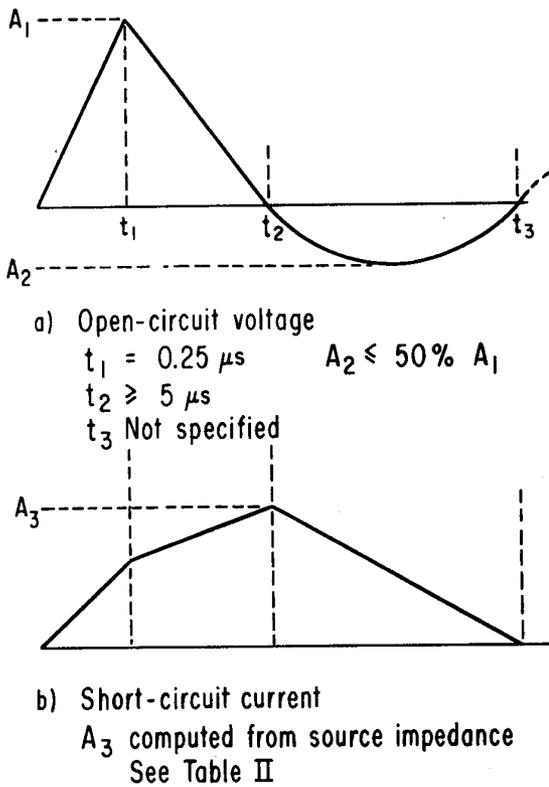


Fig. 3. Proposed TCL wave shapes.

10 μH . Again, for purposes of discussion, it is proposed that a resistive source have an impedance of 50 ohms, and an inductive source have an impedance of 10 mH.

Voltage and Current Levels

Central to the success of the BIL system of insulation coordination is the fact that only a limited number of BIL's were established, arranged in a generally geometric order of progression. For purposes of discussion, we therefore propose that there be established a series of TCL's progressing in the approximate ratio of $\sqrt[3]{10}$ or 3 values per decade. Such possible TCL's, as rounded to convenient voltages, then appear as shown on Table II.

The subject of source impedance and short-circuit current needs to be further discussed since the concept of constant surge impedance, and particularly constant inductive surge impedance, may not be valid. Transients of high voltage and large energy content tend to be produced by physically large systems, whose inductance tends to be larger than that of the systems producing lower voltage or lower energy transients.

Proof Test Techniques

The generation of surge voltages in the laboratory is well known to manufacturers and users of high power equipment. However, producing a test wave of the shape and levels proposed here may present some difficulty for the small equipment manufacturer. To answer this need, a previously developed circuit¹¹, as shown in Figure 4, may be applicable.

TABLE II
Possible Transients Control Levels

Level No.	Open Circuit Voltage (volts)	Short Circuit Current (amperes)	
		Z = 50 Ω	Z = 10 μH
1	10	0.2	2.63
2	25	0.5	6.56
3	50	1	13.1
4	100	2	26.3
5	250	5	65.6
6	500	10	131
7	1000	20	263
8	2500	50	656
9	5000	100	1310
10	10000	200	2630

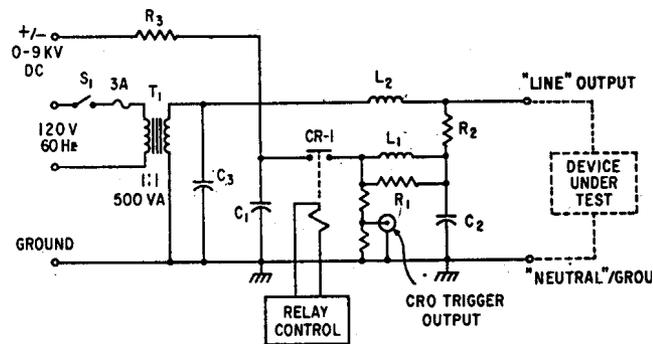


Fig. 4. Test circuit for applying spikes on 120-volt AC lines.

The objective of this design was to super-impose on a 120-volt, 60-Hz power line a transient having a rise time to first peak of 0.5 μs , followed by a damped ringing at 100 kHz in which each successive peak is 60% of the preceding peak amplitude. The amplitude of the first peak is adjustable from 0 to 8000 volts. The source impedance for the high-voltage transient is 50 ohms.

The 0.5 μs rise characteristic is obtained by the series resonance of L1 and the capacitance of C1 and C2 in series. Component values were selected to make $\sqrt{L/C}$ approximately 50 ohms, and R1 was selected to provide heavy damping for a smooth transition to the following wave.

The 100 kHz damped ring results from the parallel resonance of L2 with the parallel capacitance of C1 plus C2. Again, $\sqrt{L/C}$ is about 50 ohms. The series damping resistor R2 was selected to produce the decay to 60% amplitude between successive peaks.

CONCLUSIONS

1. The present lack of transient coordination methods in low-voltage systems does not allow the user of electronic equipment to obtain the best reliability at lowest cost.
2. Manufacturers, vendors, and users could benefit from a systematic approach to transient coordina-

tion similar in concept to the BIL used for many years in high-voltage systems. This is illustrated in Figure 5.

3. A concept of Transient Control Level (TCL) is proposed by the authors. This involves discrete steps of withstand level and proof tests based on the capability of available surge protective devices and reflecting the occurrence of surges in the real world.

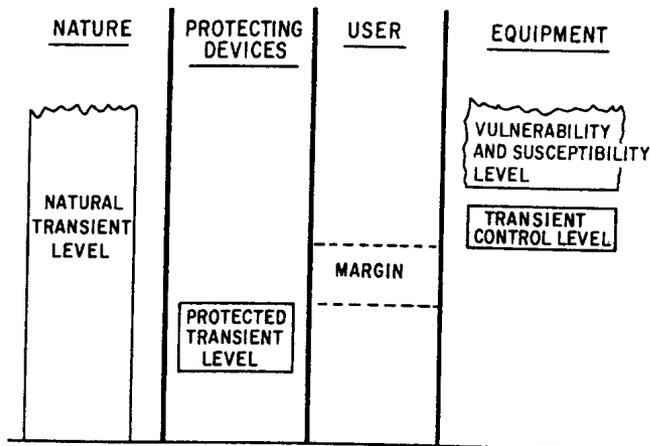


Fig. 5. Well-coordinated low voltage system.

4. Discussion is earnestly invited on the parameters to be considered in defining TCL's such as:

- voltage waveform of the transients
- source impedance of the transients
- current waveform of the transients
- levels to be assigned — current and voltage
- proof-test techniques.

Successful application of the TCL concept will require careful study of these factors, so as to develop a valid consensus among all interested parties.

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APPENDIX
TYPICAL WAVE SHAPESS

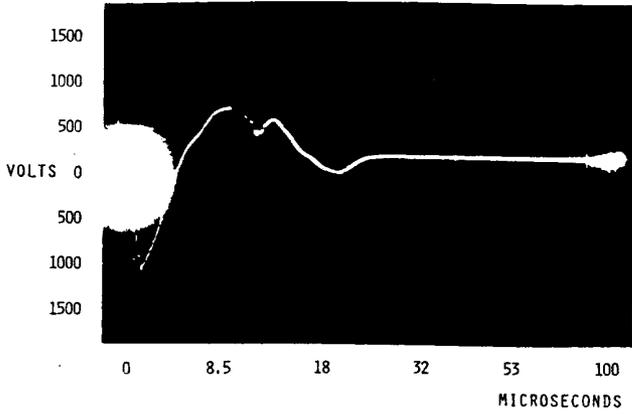


Fig. A1. Transient recorded during starting of a furnaceblower at service box.

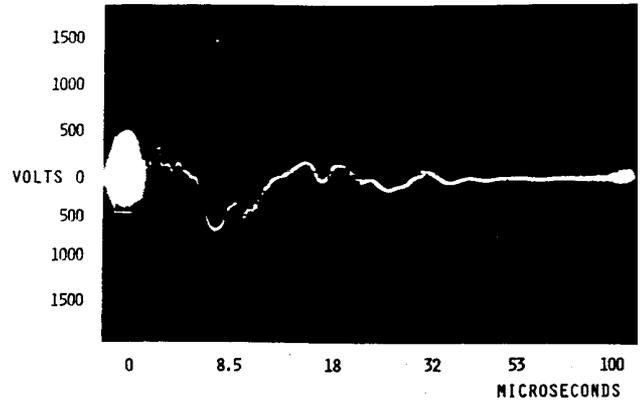


Fig. A3. Transient recorded during unidentified disturbance at service box.

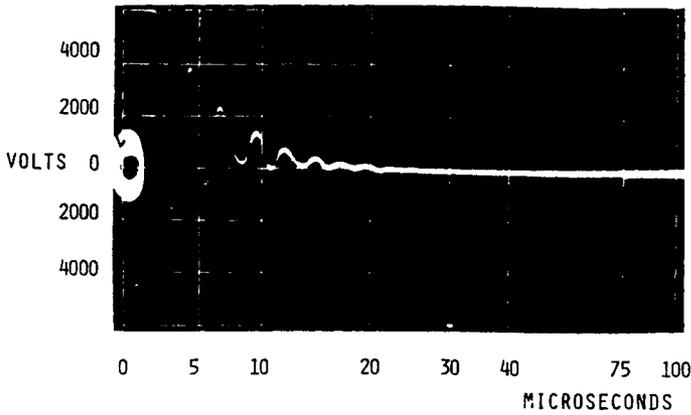


Fig. A2. Transient recorded during lightning storm on street pole.

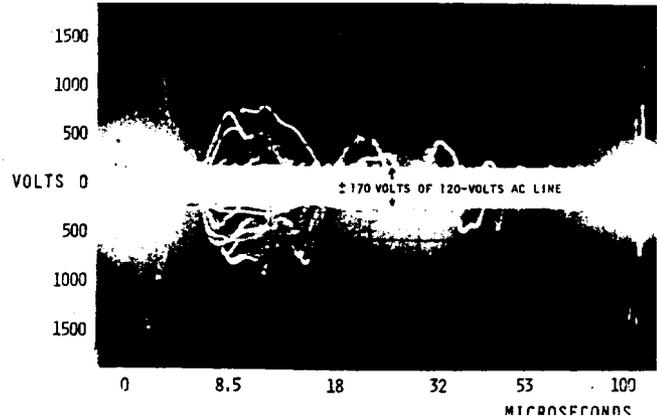


Fig. A4. Composite recording of furnace ignition transformer transients over 24 hours at service box.

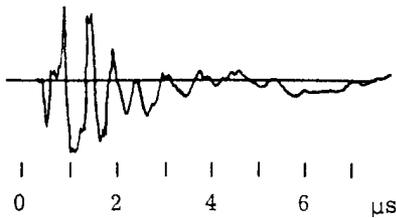


Fig. A5. Typical transients recorded during lightning injection tests on fighter-type aircraft (amplitudes are relative).

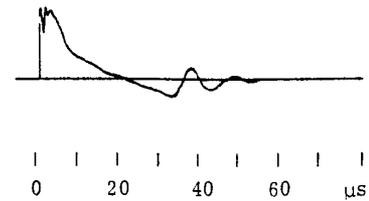


Fig. A6. Typical transients recorded during lightning injection tests on small general aviation aircraft (amplitudes are relative).

Discussion

S.M. Harvey (Ontario Hydro Research Division, Toronto, Canada): This paper provides a clear presentation of the case for a transient interference immunity standard applicable to residential and, presumably, light commercial electronic equipment. Designing transient or surge withstand compatibility into low-voltage equipment is not, of course, a new concept. The telephone companies have been doing it for years. However, the authors have commendably proposed their Transient Control Level concept in the context of a general and down to earth philosophy of testing that should encourage informed discussion.

Following the establishment of Basic Insulation Levels, the electric power industry has not been idle in the area of overvoltage testing of low-voltage equipment. A number of committees, including the Power System Relaying Committee of the IEEE Power Engineering Society and Technical Committee No 41 of the International Electrotechnical Commission have been working for years on the surge testing of static relays used for transmission line protection. The Swedish Electrical Commission has prepared a draft proposal for interference withstand capability testing of apparatus used in power stations and industrial installations. These committees have proposed a range of test waveforms including the familiar 1.2/50 impulse at peak voltages of 1, 3, and 5 kV, a moderately damped 1 MHz oscillatory wave at peak voltages of 0.5, 1, and 2.5-3.0 kV, and a high-frequency spark test at 2 - 4 and 4 - 8 kV.

In 1974, Ontario Hydro introduced a uniform transient immunity test specification for relays and other equipment intended for substation relay or control buildings. The test waveform is a moderately damped oscillatory transient whose frequency can be specified in the range of 100 kHz to 2 MHz. One of four test levels, specified in Table I, can be called for. The test is supervised by our Supply Division and manufacturers are encouraged to supply their own test equipment. However, it is still frequently necessary for Ontario Hydro to make its own test generators available.

Test	Peak Amplitude (Volts)	Source Impedance (ohms)
A	5000	100-500
B	2500	100-150
C	1000	30-50
D	500	30-50

Note that these levels when specified at 100 kHz are very similar to tests 6 and 9 in Table II of the present paper. Level B, incidentally, when specified at 1 MHz is equivalent to the IEEE Relay Test [1].

Our experience with the tests, although limited, suggests that minor circuit deficiencies leading to operational upsets are common but that damage is relatively rare. Probably the marginally greatest value of the tests at this time lies in their potential for creating an awareness of the transient problem.

A number of questions being considered at this stage of our transient test program can be rephrased to apply also to the proposals in this paper. Perhaps the authors could comment on the following:

1. What is the advisability of introducing a new test waveform or test procedure in addition to those already in circulation?
2. Would it be necessary to shield the test circuit of Fig. 4 or to locate it, say, 4-6 meters from the equipment under test? In the latter case, should the voltage and current waveforms be measured at the near end or the far end of the connecting cable?
3. Can the test circuit of Fig. 4 correctly simulate transient disturbances that occur when the white wire neutral and the green wire ground are connected together a quarter wavelength from the device under test?
4. Can a reliable certification procedure, particularly in terms of energy deliverable to a load, be established for test generators differing in design from the one shown?
5. Finally, what is the incidence of damage or significant upset to equipment now used in residential or light commercial environments and does it justify the introduction of transient testing to this class of apparatus? If applied, in view of the data contained in Fig. 2 of the paper, what criterion would be used to select a test level of less than, say 500 volts?

REFERENCES

- [1] ANSIC37.90a-1974(IEEE Std 472-1974) Guide for Surge Withstand Capability (SWC) Tests.

E.J. Cohen (U.S. Dept. of Agriculture, Washington, D.C.): We feel the concept expressed in this paper is long overdue in the field of electrical protection of electronic equipment. Experience within the telephone industry has already demonstrated that, with present trends to ever smaller equipment, protection problems can be severely aggravated. The over voltage and current tolerance of microelectric circuits has decreased to the point where protection should be major consideration in circuit design.

Added to this increased equipment vulnerability, we have found a „communications gap” between the manufacturers of electronic equipment, and the producers of protection devices. When a protection defect is uncovered, we frequently encounter disagreements between the equipment and arrester manufacturers. By establishing “Transient Control Levels,” as proposed by this paper, much of this “finger pointing” could be eliminated. As both equipment and arrester manufacturers should know precisely what the other adequate protection should be minimized.

It is felt that while the concept expressed here is valid, further consideration should be given to the levels and waveshapes involved in the tests. As these parameters may be critical to the workability of this proposal, every effort should be made to generate realistic values.

Manuscript received August 13, 1975.

Richard F. Hess (Sperry Flight Systems, Phoenix, Arizona): I agree that some form of action is needed to properly assess and overcome the adverse effects of power transients on military and commercial equipment. Assuming a consensus is reached concerning the need for transient control and the adoption of Transient Control Levels (TCL), the following comments are intended to complement the proposal for transient control in low voltage systems.

The voltage specification is based upon measurements which are appropriate to present and past equipment designs. For the most part these designs use devices which present a relatively high impedance to a source of transient energy.

Damage occurs during a power transient when the device breaks down and high to medium voltages are developed across the device while large to medium currents are flowing through it. Standard components are not normally tested under transient conditions, therefore it may be difficult to determine whether they would break down or to assign a confidence level that they would survive such a transient. When a device breaks down, either a voltage or a current viewpoint could be assumed when describing the threat of the power transient to the device.

If in order to conform to a specified TCL a device has been designed to withstand a specified voltage level, then the voltage specification is appropriate. However, a manufacturer designing equipment to meet a specific TCL could adopt an approach which calls for the use of transient power suppression devices (transzors, metal oxide varistors, etc). In this case, transient power surges are manifested as large current surges into equipment (through the protection device) rather than a large voltage transient across the equipment. Even when passing large currents, the network impedances (suppression devices, etc.) will probably be significant enough to produce a natural mode current response within the total network. Thus, current measurement of such a network would contain a significant oscillatory component similar to that present in the voltage measurements.

Two types of TCL specifications should be provided:

1. Voltage
2. Current

Like the voltage specification, the waveform and magnitude of the current specification at each TCL would be based upon the measurement of the current response modes of networks containing power suppression devices and excited by a power transient.

With the two types of specifications, equipment could be designed and tested to withstand a power transient by safely withstanding specified voltage levels or by safely passing specified currents levels. The test equipment for, the voltage specification would be calibrated under open circuit conditions and would be designed to deliver current (in the event of device breakdown) at a level at least as large as that specified in the current specification. The test equipment for the current specification would be calibrated under short circuit conditions and would be designed to provide voltage (in the event of a high impedance) at a level at least as large as that specified in the voltage specification.

Manuscript received August 14, 1975.

Tests for semiconductor vulnerability (damage) levels using square pulse waveform are common practice with the military. The damage level of many discrete components has been determined and recorded. However, the damped sinusoid pulse is more appropriate to susceptibility testing (transient upset). Depending upon the type of equipment being tested and the frequency content of expected transients, it may be desirable to test using more than one waveform. Lower frequency, high amplitude sinusoid (100 KHz) would be used to vulnerability testing and a higher frequency sinusoid (500 KHz, 1 MHz or 10 MHz depending upon the bandwidth of the equipment) would be used for susceptibility testing. At each frequency the equipment should be subjected to at least two pulses:

1. Maximum pulse is positive
2. Maximum pulse is negative

As a final observation, testing and test equipment should be kept as simple as possible to avoid adding inordinate costs to the equipment ideally, the degree of confidence obtained by such testing should result in a net reduction in equipment costs (manufacture plus maintenance).

F.A. Fisher and F. D. Martzloff: We appreciate the response of the discussors and will attempt to both respond to their questions and expand somewhat on the protection philosophy we propose. First of all, it should be pointed out that while this paper was written using household appliances as an example and presented before a group largely concerned with utility relaying, the problems of transients pervade the entire field of low voltage electrical and electronic apparatus, including the communication (telephone) industry. One of the areas where the authors have seen a great need for better transient compatibility is in the Aerospace field. Much of the background upon which the TCL concept is based comes from consideration of the transients induced in aerospace vehicles by lightning and other energetic discharges. Designers in the Aerospace community tend not to have had the problem of transients brought as forcibly to their attention as have the designers of relay devices intended to work in the harsh electrical environment of a utility substation. With reference to Mr. Harvey's first question, we feel that it is advisable to introduce new test procedures because the specialized test procedures adapted in the electric utility field may not meet the needs of users in other fields.

Each of the discussors mentions the subject of levels and waveshapes. We suggested the voltage waveshape of Figure 3 of the paper because measurements have indicated that most transients to which electronic equipment is exposed are oscillatory in nature and generally of faster front and tail times than the 1.2×50 microsecond test wave common in the electric power industry. Several other factors influence our choice. One was that the proposed wave is of long enough duration that breakdown of semiconductor junctions would not be greatly influenced by deviations from the specified waveshape. With much shorter waveshapes, the resistance of semiconductor junctions to burn out becomes strongly influenced by waveshape. Another is that transients of this nature can be injected into wires by rather simple transformer-coupled pulse-injection generators, whereas transformer injection of higher frequency oscillatory voltages and currents is more difficult. Transformer injection of transients has not been discussed in this paper but is sometimes an appropriate means of evaluating the resistance of a device to circuit upset. Mr. Hess mentions the need for two types of TCL specifications: voltage and current. We agree. We have seen instances of groups worrying wastefully about specifications that call for a specific voltage transient to be developed at the terminals of a device when that device had properly been fitted with a low-pass filter, a low impedance suppressor, or transient suppression spark gap. Specifications that do not recognize that one can neither develop a voltage across a short circuit nor circulate a current through an open circuit are not only incomplete but mischievous and counterproductive.

With reference to more of Mr. Harvey's questions, we feel that any test circuit should be built in a sufficiently well-shielded cabinet so that there is no need to physically separate the test circuit from any device under test. If a test circuit must be located away from the device under test and an interconnecting cable be used, we would think that the generator open-circuit voltage and short-circuit current should be measured at end of the cable nearest the device under test.

We do not really know what would be the interaction between a white wire neutral and a green wire ground if the two were connected together a quarter wavelength away from the generator. We take refuge in the observation that transient coordination is more likely to be

achieved through the successful passing of even an imperfect test than it is in the avoidance of all but perfect tests.

We hold no special faith in the virtues of the test circuit shown on Figure 4 of the paper and show it only as one example of various test circuits that might be produced. We feel that a reliable certification procedure not only can be, but must be, based on specifications that are not unique to any one test circuit. It is for this reason that we propose specifications be written in terms of open-circuit voltage and short-circuit currents; a concept that implies a fixed generator impedance. Care must be taken that the voltage and current specifications not be incompatible with the generator impedance. Since the writing of this paper another paper discussing the impedance of AC wiring circuits has been published [1]. Based on this paper, we would now propose that the internal impedance of a transient generator be 50 ohms paralleled by 50 microhenries. Figure 1, reproduced from the referenced paper with the permission of the author, shows how the impedance of the line ("the mains") can be closely approximated by the parallel combination of 50 ohms and 50 microhenries. Levels and waveshapes appropriate to such an impedance might then appear as shown in Figure 2 and Table 1.

As Messrs. Cohen, Harvey and Hess emphasize, the choice of appropriate levels is crucial to the successful implementation of a TCL philosophy. While a TCL of 5000 or 6000 volts might be appropriate to high reliability utility relays or a safety-oriented consumer product such as the Ground Fault Circuit Interrupter, it might impose an unnecessary economic hardship on a high volume item intended for routine household use. Likewise, while a TCL of 500 volts might be too low for residential purposes, it might be appropriate for the power inputs of electronic equipment used in aircraft, and excessively high for the signal inputs of data processing equipment intercommunicating through well-shielded signal wires.

Since one of the major purposes of this paper is to promote discussion, it is appropriate to list some of the questions the authors have posed to themselves during the formulation of this proposal:

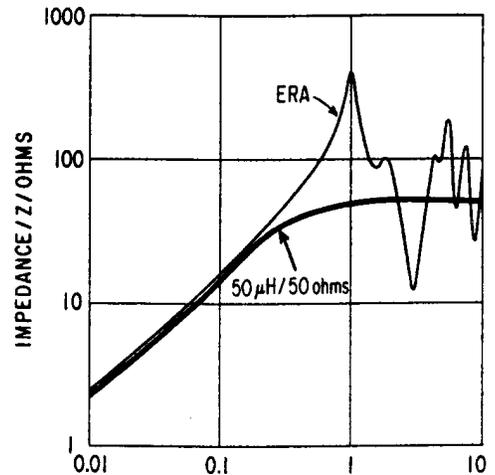


Fig. 1. Comparison of impedance measurements made by the Electrical Research Association (ERA) on the impedance of power systems with a network of 50 ohm & 50 μ H in parallel

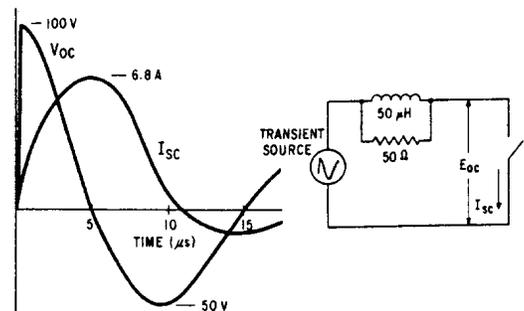


Fig. 2. Short-circuit current (I_{SC}) resulting from a transient source with V_{OC} open-circuit voltage and 50 Ω / 50 μ H source impedance.

TABLE 1

Proposed Transient Control Level Number	Open-Circuit Voltage Level (volts)	Short-Circuit Current Level (amperes)
1	10	0.68
2	25	1.7
3	50	3.4
4	100	6.8
5	250	17
6	500	34
7	1000	68
8	2500	170
9	5000	340

— Are there sufficient problems relating to transient coordination to warrant an effort, likely to be major and long term, to achieve better coordination between the transients to which equipment is exposed, and the ability of equipment to withstand such transients?

- Would transient control level (or some other) specifications and standards help achieve successful transient coordination between equipment manufacturers, utilities and equipment users?
- Should there be a limited number of fixed levels? The authors feel that it is essential that the number of levels be limited, perhaps to 9-15 levels distributed in a geometric progression over the range 10-5000 volts. The assignment of the levels may have -to be done arbitrarily. This need not be cause for alarm. The electronic industry for years has worked successfully with resistor and capacitor values produced according to an arbitrarily selected geometric progression.
- Should these levels reflect the system voltage, the expected reliability of the equipment function, the environment?
- What kind of source impedance is appropriate? As mentioned above, an impedance of 50 ohms paralleled by 50 microhenries may be appropriate.
- Should open-circuit voltage and impedance be stated or, alternatively, should open-circuit voltage and short-circuit current be specified?
- Is one impedance value suitable for the majority of the systems?
- What waveshape is appropriate, for voltage as well as current? For damage, we are mostly concerned with energy and front-ofwave but if upset (interference) is to be included in TCL, then do we need to specify a frequency spectrum?

REFERENCE

[1] "Impedance of the Supply Mains at Radio Frequencies", J. H. Bull, Proceeding of 1st Symposium on EMC, Montreux, May 1975.

**TRANSIENT
CONTROL LEVELS
PHILOSOPHY
AND
IMPLEMENTATION**

**PART 1: THE
REASONING
BEHIND THE
PHILOSOPHY**

Transient Control Levels Philosophy and Implementation

Part 1: The Reasoning behind the philosophy

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Significance:

Part 2 Development of standards – Reality checks
Part 5 Monitoring instruments, laboratory measurements and test methods
Part 6 Textbooks and tutorial reviews

Presentation to the EMC community in a European forum of the Transient Control Level concept being proposed in the US via the IEEE Power Engineering Society (See Fisher and Martzloff in *IEEE Transactions PAS 95*, 1976). A companion paper on implementation is reprinted in Parts 5 and 6 (See Fisher and Martzloff in the same forum).

The proposal also included the concept of establishing **first** a level of surges that will not be exceeded, thanks to the application of appropriate SPDs, and **only then** designing equipment that will withstand level higher than the allowable level of surges. This was nothing new, having been applied successfully in the high-voltage utility environment. However, the proposal was new for the low-voltage community.

Unfortunately, the *fait accompli* of equipment being designed and placed on the market without such coordination prevented application of that proposal. Thus, industry is left with the situation where equipment failures under surge conditions can occur, after which remedies must be found as retrofits.

In 1975, the following statement appeared in the paper and should be kept in mind when questions arise on the selection of “representative waveforms” in IEEE Std C62.41.2:

These BIL amplitudes, while assigned somewhat arbitrarily, were (and are) kept in touch with reality by the fact that equipment designed in accordance with standards do not fail when exposed to surges produced by lightning, in contrast to equipment designed prior to the development of the philosophy of insulation coordination and the establishment of standard BILs.

TRANSIENT CONTROL LEVEL PHILOSOPHY AND IMPLEMENTATION

I. The Reasoning Behind the Philosophy

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Abstract

This is the first of a pair of papers describing how better transient protection might be achieved through the use of a Transient Control Level (TCL) philosophy. The authors have developed and are proposing this TCL philosophy because damage to and upset of electronic and other low-voltage equipment by transients seems to be a never-ending problem, and one that is likely to get worse in the future as electronic controls permeate even more of the products which affect our lives. A number of proposals have been made — some already incorporated into standards — on various test wave shapes and specifications. The authors propose an approach integrating many of these proposals while focusing attention on significant parameters.

Introduction

An area where present standards do not seem to offer sufficient guidance to designers and manufacturers of electronic equipment is in what types of transients to consider and how to prove that equipment works in the presence of transients. This situation is perhaps under better control in the electric power field than it is in the fields of aerospace, general industry, housewares, and the military. For instance, the insulation of high-voltage apparatus is coordinated to the threats that nature provides to that insulation through the philosophy of insulation coordination as expressed in the Basic Insulation Level (BIL) system. The BIL system provides for a standardized series of levels being coordinated with the protective abilities of existing protective devices. On the other hand, electronic and control equipment is all too often designed, built, and delivered before the existence of a transient threat is recognized. If transients turn out to endanger the equipment, there may be no adequate surge protective devices. In fact, there may not be any satisfactory answer to the problem posed by transients.

The authors' TCL philosophy is aimed at achieving better coordination than now exists between the transients to which equipment is exposed and the abilities of equipment to withstand the transients. It is patterned after the BIL approach to insulation coordination so successfully used in the electric power field.

The purposes of this first paper are to explain the reasoning behind the different elements of the BIL system of insulation coordination, and to explain how similar reasoning has led to the formulation of the TCL philosophy. Some observations on how to perform TCL tests are given in a companion paper [1].

Proposal for TCL

This proposal can be summarized by saying that we want to:

1. Establish the concept that equipment shall be rated in terms of its ability to withstand a limited set of transient proof tests, rather than in terms of its ability to withstand unknown "actual" transients.
2. Establish the concept that transient specifications apply to power and signal lines. In the past, only power lines have been considered.
3. Establish a set of levels (limited in number) to which equipment is designed and tested.
4. Establish a set of standard test waves (limited in number) to which low-voltage equipment will be subjected.
5. Establish standardized relationships between voltage and current (source impedance).
6. Differentiate between the task of establishing the family of test levels and wave shapes, and the task of actually selecting a specific level. This means that:
 - We will propose to you a family of levels and wave shapes
 - You will select the specific level and shape, based on your reliability goals, your costs, and your experience.

This proposal is made with awareness that it may be one more of an already confused array of standards. However, if accepted by a large section of industry and users, it could become a unifying link and make the applications more successful.

In the following paragraphs, we will attempt to present the background justifying our proposal, for each of the points listed above.

1. Basis for rating equipment

The concept that equipment be rated in terms of its ability to withstand a standard test rather than "actual" service conditions is not new. This is at the very heart of the system of BIL, which has been so successful in the field of electric utility equipment.

Fortunately for the utilities, few parties were involved in making the decisions, and thus it was possible at an early stage to establish the BIL system and to enforce it because of the near total control of the engineering department of a utility over the system design. In the field of low-voltage systems, however, the selection and purchase of a multiplicity of components and equipment by a multiplicity of buyers from a multiplicity of vendors on behalf of a multiplicity of users have made it very difficult to maintain the organized systems approach which succeeded in the case of the electric utilities.

A basic concept, which needs to be mutually accepted by users and manufacturers of equipment, is that it is impossible to simulate all possible transient overvoltages (and over-currents) that a product line might experience in service. However, by designing the equipment to a certain standard and controlling the level of transients by suitable protection, a much greater chance of successful operation in the cruel real world will be obtained.

The task is then to establish a set of standard tests, acceptable to the vast majority of applications, reflecting the real world but not pretending to duplicate it, simple enough to be practical, conservative enough to ensure reliability, but realistic in terms of economics.

Obtaining complete agreement from all is most unlikely an impossible goal, and thus the unsatisfactory situation endures. This stalemate can be broken by accepting a proposal which might not be perfect, but is better than many isolated standards or no standard at all.

2. All lines subject to transient tests

The existence of transients on power lines is by now a recognized and accepted fact, so that most applications will involve a certain amount of precautions in specifying transient withstand capability. However, in the case of signal lines, this recognition is less frequent, and there have been examples where a total lack of appreciation of the problem has led to the design and deployment of equipment that cannot be protected from transients.

Transients can be introduced into a piece of equipment by the power lines from many sources, such as lightning, switching transients, fault clearing, and coupling from adjacent circuits. Signal lines, especially in the case of extensive systems covering a vast area, can also be subjected to induced transients by lightning, adjacent circuits, ground currents, etc. Since

quite often the signal circuits tend to be at a lower voltage than the power circuits, the discrepancy between the rated level in the circuit and the actual level of transients makes the signal circuits more susceptible to transient problems.

A question related to which lines are to be subjected to transients is that of "common mode" versus "transverse mode." This is not always clear and must be addressed in a comprehensive specification.

3. Test Levels

An important feature of the BIL system was that it involved a limited number of test levels graded to the operating voltage of the system for which apparatus was being designed. A successful TCL system should also be designed around a relatively small number of levels. One who tries to establish levels is pulled in two directions; one to avoid complexity by establishing a minimum number of levels, and in another to provide levels that accommodate existing practices with minimum disruption.

One way to achieve this is through the use of major and minor intervals in the levels. Figure 1 shows several possible level series. The scales show the range 30 to 3000 volts divided into intervals based on $10^{1/3}$, $10^{1/6}$, and $10^{1/5}$. The physical positioning of the numbers on the figure shows how those numbers match the proportionate interval scales. In the past, we have proposed that there be three levels per decade with the spacing between levels being approximately $10^{1/3}$. The factors 1.5, 3, and 6 seem appropriate, particularly since such a set could include the voltage levels 600, 1500, and 3000 volts in some existing specifications. The widely used specification MIL-704 includes the 600-volt level for transients, and it would appear that this number, at least, should appear in any set of TCL levels. Levels based on the above progression appear in the left-hand column.

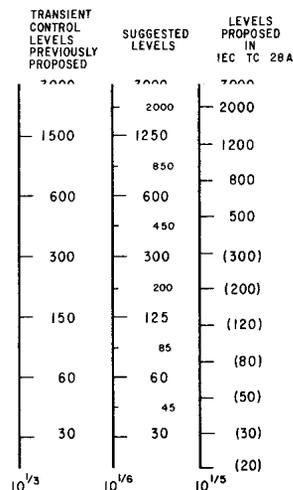


Fig. 1: Proposed levels for TCL voltages compared to existing level systems

A progression proposed in IEC TC 28A, Low Voltage Insulation Coordination, is shown in the right-hand column. The levels that have been proposed range from 500 to 12000 volts. On Fig. 1, the levels in parentheses are inserted only to indicate the sequence. This progression, which seems to be based on the factor $10^{1/5}$, does not include the 600-volt level.

Levels as arranged in the center column might appear to provide an appropriate compromise. We propose that the levels in boldface print be the recommended levels while those in lighter print be used, preferably sparingly, when intermediate levels are needed. Associated with each of these levels would be a short-circuit current level, the magnitude of which is related to the voltage levels through defined source impedances. Source impedance will be discussed further below.

Some of the levels will seem very low, particularly to those accustomed to dealing with transients on power lines. They may not be unrealistic for some low-voltage signal circuits. A more important point, however, is that the establishment of a series of levels, from which a choice may be made, is a task separate and distinct from that of deciding to what level a piece of equipment should be designed. This latter point is discussed in more detail later.

4. Wave shape

Many test waves have been proposed in the past. Table I shows some that have been proposed.

These wave shapes range from the very fast rise, short duration, to the slow-rise, long duration, with oscillatory or unidirectional voltages. Each of these is based on practical considerations for specific applications; but the total picture is then one of confusion and discouraging attempts at standardization.

Observations of oscilloscope recordings and independent work on the resonant frequency of power systems [2] have shown that most transient voltages in low-voltage systems have an oscillatory wave shape, in contrast to the well-known and generally accepted unidirectional wave used in high-voltage insulations standards. Frequencies are typically in the range of 5 kHz to 500 kHz, with the majority of the transients having frequencies above 100 kHz [3].

On the basis of these observations, the authors have proposed the voltage wave shape of Fig. 2, as being most representative of transients in low-voltage systems.

This wave is a composite. One component is aimed at producing the effects associated with fast rise times. Coupled interference and the response of inductive devices are examples. Another component is aimed at producing the effects associated with the more slowly changing, and oscillatory, tail. Voltage summation in capacitive circuits coupled by rectifiers is an example. Energy handling capability of surge protective devices is another.

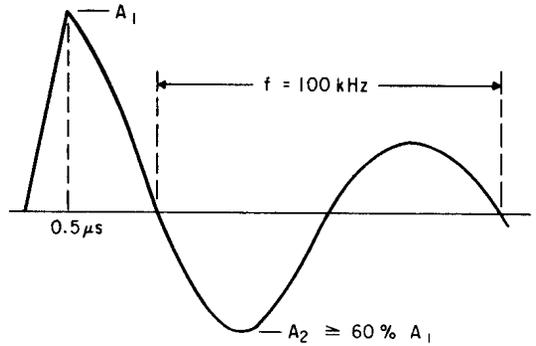


Fig. 2: Proposed TCL voltage wave

While this wave may then appear artificially contrived, it will subject test samples to the two most significant effects of voltage, circuit upset, and circuit damage. Since the wave may be produced by simple laboratory circuits, comparison tests may be easily done by different organizations. [4].

This wave shape was first defined by a consensus at a meeting of the Ground Fault Protection Section of NEMA, in August 1973, and has since received increasing acceptance, notably at the Underwriter's Laboratories. Recently, independent considerations [5] have given further support to a 0.5 μs rise time and 5 μs duration impulse.

However, in all probability this one oscillatory TCL wave will not meet the needs of all users. Therefore, we propose that the wave of Fig. 2 be supplemented by two unidirectional voltage waves: the classic ANSI 1.2 x 50 μs impulse wave and a 10 x 1000 μs wave [6,7].

We believe that most applications can be treated by one of these three wave shapes, once the concept is accepted that a perfect match of "actual" wave shape and "test" wave shape is not essential. The first wave, fast rise and 100 kHz ring, would be more applicable for circuits exposed to "lightning remnants" (the natural oscillation of a power system excited by a lightning discharge or switching transient at some remote point) as well as control circuitry exposed to induced transients. The second wave shape, the familiar 1.2 x 50 unidirectional, would be applicable to circuits where direct exposure to lightning strokes is likely; while the third (long tail) would be applicable to situations involving lightning current discharge on long cables. The second and third wave shapes are also representative of transients produced by the switching of inductive circuits.

Special applications, such as NEMP (Nuclear Electromagnetic Pulse) hardening, or high-voltage substation supervisory equipment, would rather retain their own well-documented standards.

5. Source impedance and energy

In some types of tests, the object is to determine what level of voltage will cause failure (permanent or temporary) of insulation. The

TABLE I - PARTIAL LISTING OF EXISTING OR PROPOSED TEST WAVES

ORIGIN	DESCRIPTION		TYPICAL APPLICATION
	Wave Shape	Amplitude	
ANSI, IEC	- 1.2 x 50 μ s	Specified voltage	Power apparatus
	- 8 x 20 μ s	Specified current	
IEEE Std. 472 Guide for Surge Withstand Capability (SWC)	- 1.25 MHz repetitive at 60 Hz	2.5 kV peak	Low-voltage AC circuits and control lines in substation equipment
	- 6 μ s decay to 50%		
	- 150 Ω source impedance		
Fisher-Martzloff [8]	- 0.25 μ s rise	Specified levels	Low-voltage AC circuits and signal lines
	- 5 μ s to zero		
	- Unspecified ring		
GE Transient Suppression Manual [4]	- 500 kHz rise	0 to 8 kV	Low-voltage AC circuits
	- 100 kHz ring		
	- 40% decay		
Crouch-Fisher-Martzloff [10] U.L. Ground Fault Interrupters	- 0.5 μ s rise	Specified levels	Low-voltage AC circuits
	- 100 kHz ring		
	- 2nd peak \geq 60% first		
	- 50 Ω source impedance		
IEEE Std. 465.1 Test Specifications for Gas Tube Surge Protective Devices	Three requirements:		Telephone protectors
	- 10 x 1000 μ s current	50 to 500 A	
	- 8 x 20 μ s current	5 to 20 kA	
	- Linear voltage ramp of 100, 500, 5000, 10,000 V/ μ s until sparkover		
FCC Docket 19528	- Metallic		Communications equipment
	- 10 x 560 μ s	800 V peak	
	- 100 A short circuit current		
	- Longitudinal		Telephone electronics
	- 10 x 160 μ s	1500 V peak	
	- 200 A short circuit current		
Rural Electrification Administration Spec. PE-60	- 10 x 1000 μ s voltage	3 σ of protector level	Telephone electronics
	- 100 V/ μ s rise		
NEMP Hardening	- Rectangular pulse 3 ns to 10 μ s	0.1 to 1000 A	Evaluation of components
	- Damped sinewave 10 ⁴ to 10 ⁸ Hz	1 to 100 A	
NASA Space Shuttle	- Damped sinewave 125 kHz	E _{oc} = 50 V I _{sc} = 10 A	Space shuttle electronics
	- Unidirectional		
	- 2 x 100 μ s	E _{oc} = 50 V I _{sc} = 10 A	
	- 300 x 600 μ s	E _{oc} = 0.5 V I _{sc} = 5 A	
MIL-STD-704	Envelope specified, max. duration 50 μ s	600 V peak	Military aircraft power

nature of the transient following breakdown is not of much concern. The typical test piece is of high impedance (except after breakdown), and thus does not load the generator. People have tended to overlook the source impedance of the generator, even in applications where that impedance is important.

However, with the development of voltage suppression devices, the source impedance becomes an integral part of the suppression scheme. Some types of devices (spark gaps) function by switching into a low impedance state and reflecting the energy associated with the transient back from whence it came. Other devices (varistors, selenium, and Zener type diodes) clamp the voltage across their terminals while conducting the surge current and thus dissipate the surge energy in the protective device. The ability of the device to handle that energy becomes of importance. In either case, the test generators must be capable of supplying an appropriate amount of current, but should not supply too much current.

Test specifications should reflect the fact that, in some cases, voltage is the appropriate measure of the transient, and in other cases current is the appropriate measure. Above all, they must avoid wording that leads the inexperienced to struggle valiantly, with everlarger surge generators, to develop a specified voltage across a correctly functioning spark gap or varistor. This has occurred.

In the original formulation of the TCL concept, the authors proposed, and still do propose, that the generator impedance associated with the 100 kHz oscillatory test wave be an impedance representative of that measured on a-c supply mains. Such an impedance can be represented as 50 ohms in parallel with 50 microhenrys [10].

The ANSI specifications dealing with the long-established 1.2 x 50 μs unidirectional wave do not treat source impedance directly, but recognize its existence by providing a separate current test wave for surge arresters or other surge protective devices. In the TCL concept as we now visualize it, this same approach would be followed: separate voltage and current levels.

One of the applications where the 10 x 1000 μs unidirectional test wave might be appropriate would be those involving switching of inductive circuits. The impedance associated with such transients can vary over wide limits and may be quite low. We do not feel there is yet a sufficient engineering consensus as to what a suitable standard source impedance might be. Accordingly, we made no recommendations for such impedance, feeling that the evaluation of such impedance must be done on an individual basis for the specific application at hand.

6. Selection of specific levels

The task of selecting the transient control level appropriate for any one piece of equipment, or any one application, is one of engineering and cannot be fully dealt with in this paper. However, some discussion of the task is necessary to show how that task fits into the overall TCL

philosophy. The BIL system provides some guidance. A fundamental tenet of the BIL system is that the insulation structure of apparatus is not designed until after the required insulation level is agreed upon, and that this insulation level is not chosen until one is sure that there are voltage-limiting devices (surge arresters) that can control natural transients to levels lower than those to which the factory proof test will subject the apparatus under design.

On the other hand, low-voltage and electronic equipment is all too often designed without consideration of transients or whether protective devices might even be available if needed. One guideline is then that equipment should not be designed until an appropriate design level has been chosen. This choice should be made after consideration of the distribution of naturally occurring transients.

The occurrence of transients is a statistical process, both in voltage levels and energy content. Low levels are common while high levels occur rarely. Figure 3 shows the relationship between voltage level and frequency of occurrence on 120-volt residential circuits, from observations made in the United States [9]. While this type of information cannot serve to predict the occurrences at individual locations, it is of interest if one is concerned with the overall statistics of transients. For instance, a manufacturer can select a withstand level (or conversely, a failure level) by trading off the tangible and intangible cost of failures for the cost of the added protection required to achieve that level. From the graph of Fig. 3, we can see that decreasing the withstand level from, say, 4 kV to 2 kV is likely to increase the failure rate of a product by a factor of 10.

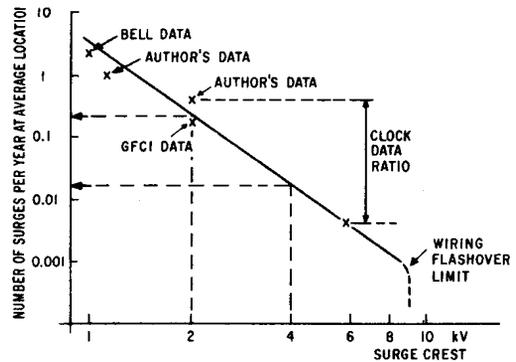


Fig. 3: Exposure of residential circuits to surges (number of surges vs highest surge at any one location)

Selection of the most appropriate level for a specific application should remain the prerogative of the parties directly interested. This choice will be based on a number of factors such as the circuit rated voltage, the exposure of the circuit to induced transients, the presence or absence of a mandatory suppressor in the circuit, the risk analysis (probability of failure, consequence of a failure, cost-trade off), etc.

TABLE II - PROPOSED IMPULSE LEVELS BY IEC-TC 28A

Rated Voltages				Preferred Series of Impulse Withstand Voltages in Volts				
Line-to-Earth	1-phase or d.c. Line-to-line L-M or L-L	3-Phase L-N or L-L	System Voltages According to IEC-Publ. 38	Category				
Up to Volts	Up to Volts	Up to Volts	Volts	I	II	III	IV	V
80	75- 150			500	800	1200	2000	3000
150	150- 300	150/250	120/220/240 1-phase a.c. 110/220 d.c.	800	1200	2000	3000	5000
300	300- 600	300/500	220/380 } 3- 240/415 } phase 277/480 }	1200	2000	3000	5000	8000
600	600-1200	600/1000	660 } 3- 1000 } phase	2000	3000	5000	8000	12000

Note: The values of impulse withstand voltage given in columns I through V are a preferred series of values to be used by the Technical Committees for the purpose of insulation coordination. Products subjected in the field to the same conditions of overvoltages or rated to withstand the same overvoltages are to be assigned values from the same column. While it might be useful to describe products and specify a preferred column for such products, SC 28A has refrained from doing so.

An example of such a selection process is found in current proposals of IEC 28A for low-voltage insulation coordination. This proposal includes a matrix of voltage levels depending on one hand on the system voltage and on the other hand on a level category, which is left to the users to choose but implies some recognition of exposure factors. This proposed table is reproduced here as Table II with the permission of the IEC TC 28A Chairman.

Conclusion and Recommendation

Acceptance of the TCL concept by manufacturers and users of equipment, as well as standardizing and regulatory agencies, would be a great step toward simplification of specifications and toward more reliable system performance.

This paper has incorporated the feedback received after several proposals made at IEEE meetings, and at this point represents the position of the authors, supported and amended by the comments received. Further feedback from the EMC community is earnestly invited and welcome.

To summarize our proposal, we recommend consideration and eventual acceptance of the following:

1. Major voltage levels of 300, 600, and 1250 volts, with intermediate levels of 450, 850, and 2000 volts used if necessary; the levels to be scaled upwards or downwards by the appropriate powers of ten.
2. A voltage wave shape of 0.5 μs rise x 100 kHz ring with current related to voltage by a source impedance of 50Ω and 50 μH. This wave shape would be supplemented by 1.2 x 50 μs and 10 x 1000 μs unidirectional waves.
3. All terminals, power and signal, are to be subjected to TCL tests.
4. For any particular piece of equipment, an appropriate level would be chosen from the above series, by mutual agreement between supplier and user.

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**A GUIDELINE
ON SURGE
VOLTAGES
IN AC POWER
CIRCUITS
RATED UP
TO 600 V**

A Guideline on Surge Voltages in AC Power Circuits Rated up to 600 V

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Significance:

Part 2 Development of standards – Reality checks

Progress report to the European EMC community on the development of what became IEEE Std 587-1980.

Explains the proposition that a Ring Wave should be added to the traditional unidirectional impulses

NOTE: A parallel paper was presented to the 1979 IEEE PES community at the Summer Power Meeting under the title "*The Development of a Guideline on Surge Voltages in Low-Voltage AC Power Circuits*".

A GUIDELINE ON SURGE VOLTAGES IN AC POWER
CIRCUITS RATED UP TO 600 V

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Summary

Surge voltages occurring in ac power circuits can be the cause of misoperation or product failure for residential as well as industrial systems. The problem has received increased attention in recent years because miniaturized solid state devices are more sensitive to voltage surges (spikes and transients) than were their predecessors.

Although surge voltage amplitudes and their frequency of occurrence on unprotected circuits are well known, their waveshapes and energy content are less well known. On the basis of measurements, statistics, and theoretical considerations, a practical guideline for outlining the environment for use in predicting extreme waveshapes and energy content can nevertheless be established. The Surge Protective Device Committee of the Institute of Electrical and Electronics Engineers has been developing such a guideline, the essential elements of which are presented in this paper.

Surge voltages [1] occurring in ac power circuits rated up to 600 V can be represented by various waveshapes in an attempt to duplicate actual surge voltages. Two major types of surges reflecting differences in the environment are described to represent the situation realistically.

Systems located inside a building and separated from the overhead lines by some line impedance experience surge voltages of waveshapes and energy levels that differ from those of the outdoor environment. Outside systems exposed to direct lightning strikes or lightning-induced surges--typically overhead lines--experience levels implied by IEEE standards for secondary arresters. This guideline addresses particularly the hazards to these two types of systems [2].

Scope

The guideline presented here primarily addresses ac power circuits with rated voltages up to 600 V, although some of the conclusions offered could apply to higher voltages and also to some dc power systems. Other standards have been established, such as IEEE 472, *Guide for Surge Withstand Capability (SWC) Tests*, intended for the special case of high-voltage substation environments, and IEEE 28, *Standard for Surge Arresters for ac Power Circuits*, covering primarily the utilities environment. The guideline presented here intends to complement, not conflict with, existing standards, and to present a practical proposal for the selection of voltage and current tests to be applied in evaluating the surge withstand capability of equipment connected to these power circuits, primarily in residential and light industrial applications.

Some guidance is also presented on how to proceed from the environment description to the selection of "standard" test waves.

The surge voltages [1] considered in this guideline are those exceeding two per unit (or twice the peak

operating voltage) and having durations ranging from a fraction of a microsecond to a millisecond. Overvoltages of less than two per unit are not covered here, nor are transients of longer duration resulting from power equipment operation and failure modes. Because these low-amplitude and long-duration surges are generally not amenable to suppression by conventional surge protective devices, they require different protection techniques.

1. The Origin of Surge Voltages

Surge voltages occurring in low-voltage ac power circuits originate from two major sources: load switching transients and direct or indirect lightning effects on the power system. Load switching transients can be further divided into transients associated with (1) major power system switching disturbances, such as capacitor bank switching; (2) minor switching near the point of interest, such as an appliance turnoff in a household or the turnoff of other loads in an individual system; (3) resonating circuits associated with switching devices, such as thyristors; and (4) various system faults, such as short circuits and arcing faults. Measurements and calculations of lightning effects have been made to yield data on what levels can be produced, even if the exact mechanism of any particular surge is unknown. The major mechanisms by which lightning produces surge voltages are the following:

- (a) A direct lightning strike to a primary circuit injects high currents into the primary circuit, producing voltages by either flowing through ground resistance or flowing through the surge impedance of the primary conductors.
- (b) A lightning strike that misses the line but hits a nearby object sets up electromagnetic fields which can induce voltages on the conductors of the primary circuit.
- (c) The rapid collapse of voltage that occurs when a primary arrester operates to limit the primary voltage couples effectively through the capacitance of the transformer and produces surge voltages in addition to those coupled into the secondary circuit by normal transformer action.
- (d) Lightning strikes the secondary circuits directly. Very high currents can be involved, exceeding the capability of conventional devices.
- (e) Lightning ground current flow resulting from nearby direct-to-ground discharges couples onto the common ground impedance paths of the grounding network.

Fast-acting protection devices, such as current-limiting fuses and circuit breakers capable of clearing or beginning to part contacts in less than 2 ms, leave trapped inductive energy in the circuit upstream; upon collapse of the field, very high voltages are generated.

Transient overvoltages [1] associated with the switching of power factor correction capacitors have lower frequencies than the high-frequency spikes with which this document is concerned. Their levels, at least in the case of

restrike-free switching operations, are generally less than twice normal voltage and are therefore not of substantial concern here, but should not be overlooked.

On the other hand, switching operations involving restrikes, such as those produced by air contactors or mercury switches, can produce, through escalation, surge voltages of complex waveshapes and of amplitudes several times greater than the normal system voltage. The severest case is generally found on the load side of the switch and involves only the device that is being switched. While this situation should certainly not be ignored, in such a case the prime responsibility for protection rests with the local user of the device in question. However, switching transients can also appear on the line side across devices connected to the line. The presence and source of transients may be unknown to the users of those devices. This potentially harmful situation occurs often enough to command attention.

2. Occurrence and Voltage Levels in Unprotected Circuits

2.1 Rate of Occurrence Versus Voltage Level

The rate of occurrence of surges varies over wide limits, depending on the particular system. Prediction of the rate for a particular system is always difficult and frequently impossible. Rate is related to the level of the surges; low-level surges are more prevalent than high-level surges [3]. Data collected from many sources (Appendix I) have led to the plot shown in Figure 1. This prediction shows with certainty only a relative frequency of occurrence, while the absolute number of occurrences can be described only for an "average location." The "high exposure" and "low exposure" limits of the band are shown as a guide, not as absolute limits [2], to reflect both the location exposure (lightning activity in the area and the nature of the system) and the exposure to switching surges created by other loads. Such data are useful in that they describe the maximum levels likely to be encountered and give some estimate of the rate of occurrence of such surges. Of equal importance is the observation that surges in the range of 1 to 2 kV are fairly common in residential circuits.

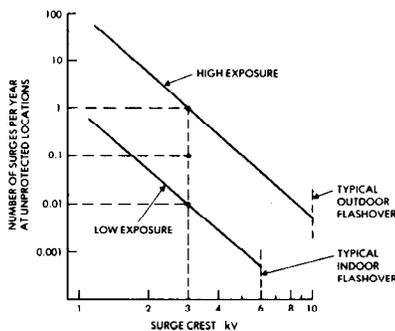


Figure 1. Rate of Surge Occurrence vs Voltage Level

From the relative values of Figure 1, two typical levels can be cited for practical applications. First, the expectation of a 3 kV transient occurrence on a 120 V circuit ranges from 0.01 to 1 per year at a given location - a number sufficiently high to justify the recommendation of a minimum 3 kV withstand capability. Second, the wiring flashover limits indicate that a 6 kV withstand capability may be sufficient to ensure device survival indoors, but a 10 kV withstand capability may be required outdoors.

2.2 Timing of Occurrence

Surges occur at random times with respect to the power frequency, and the failure mode of equipment may be affected by the power frequency follow current. Furthermore, the timing of the surge with respect to the power frequency may affect the level at which failure occurs [4]. Consequently, surge testing must be done with the line voltage applied to the test piece.

3. Waveshape of Representative Surge Voltages

3.1 Waveshapes in Actual Occurrences

Indoor - Measurements in the field, measurements in the laboratory, and theoretical calculations indicate that most surge voltages in indoor low-voltage systems have oscillatory waveshapes, unlike the well-known and generally accepted unidirectional waves specified in high-voltage insulation standards. A surge impinging on the system excites the natural resonant frequencies of the conductor system. As a result, not only are the surges typically oscillatory, but surges may have different amplitudes and waveshapes at different places in the system. These oscillatory frequencies of surges range from 5 kHz to more than 500 kHz. A 30 to 100 kHz frequency is a realistic measure of a "typical" surge for most residential and light industrial ac line networks.

Outdoor - Surges encountered in outdoor locations have also been recorded, some being oscillatory [5], others being unidirectional. Because the overriding concern here is the energy associated with these surges, a conservative but realistic description of the surges can be derived from the long-established specified duty of a secondary arrester, as detailed in Paragraph 3.2. While this specification is arbitrary, it has the strength of experience and successful usage.

3.2 Selection of Representative Waveshapes

The definition of a waveshape to be used as representative of the environment is important for the design of candidate protective devices, since unrealistic requirements, such as excessive duration of the voltage or very low source impedance, place a high energy requirement on the suppressor, with a resulting cost penalty to the end user. The two requirements defined below reflect this trade-off.

Indoor - Based on measurements conducted by several independent organizations in 120 and 240 V systems (Appendix I), the waveshape shown in Figure 2 is reasonably representative of surge voltage in these power circuits. Under the proposed description of a "0.5 μ s x 100 kHz ring wave," this waveshape rises in 0.5 μ s, then decays while oscillating at 100 kHz, each peak being about 60% of the preceding peak.

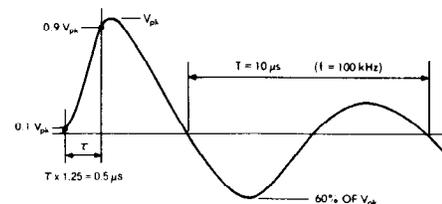


Figure 2. The Proposed 0.5 μ s x 100 kHz Ring Wave (Open-circuit Voltage)

The fast rise can produce the effects associated with nonlinear voltage distribution in windings and the dv/dt effects on semiconductors. Shorter rise times are found in many transients, but, as those transients propagate into the wiring or are reflected from discontinuities in the wiring, the rise time becomes longer.

The oscillating and decaying tail produces the effects of voltage polarity reversals in surge suppressors or other devices that may be sensitive to polarity changes. Some semiconductors are particularly sensitive to damage when being forced into or out of a conducting state, or when the transient is applied during a particular portion of the 60 Hz supply cycle (Appendix II). The response of a surge suppressor can also be affected by reversals in the polarity, as in the case of RC attenuation before a rectifier circuit in a dc power supply.

The pulse withstand capability of many semiconductors tends to improve if the surge duration is much shorter than one microsecond. For this reason, the first half-cycle of the test wave must have a sufficient duration.

Outdoor - In the outdoor and service entrance environment, as well as in locations close to the service entrance, substantial energy, or current, is still available. For these locations, the unidirectional impulses long established for secondary arresters are more appropriate than the oscillatory wave.

Accordingly, the recommended waveshape is 1.2 x 50 μs for open-circuit voltage and 8 x 20 μs for short-circuit current or current in a low-impedance device. The numbers used to describe the impulse, 1.2 x 50 and 8 x 20, are as defined in IEEE Standard 28 - ANSI Standard C62.1; Figure 3 presents the waveshape and a graphic description of the numbers.

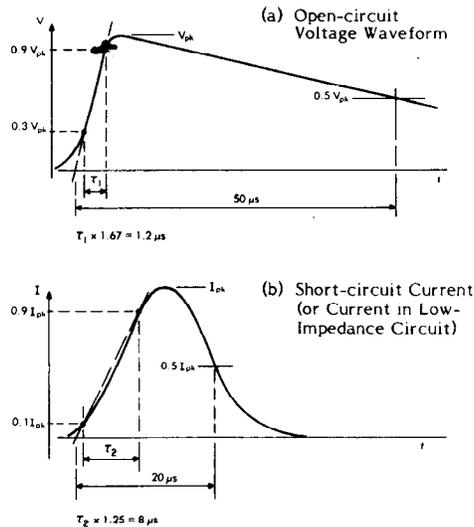


Figure 3. Waveshapes for Outdoor Locations

4. Energy and Source Impedance

4.1 General

The energy involved in the interaction of a power system with a surge source and a surge suppressor will divide between the source and the suppressor in accordance with the characteristics of the two impedances. In a gap-type suppressor, the low impedance of the arc after sparkover forces most of the energy to be dissipated elsewhere: for instance, in a power-follow current-limiting resistor that has been added in series with the gap. In an energy-absorber suppressor, by its very nature, a substantial share of the surge energy is dissipated in the suppressor, but its clamping action does not involve the power-follow energy resulting from the short-circuit action of a gap. It is therefore essential to the effective use of suppression devices that a realistic assumption be made about the source impedance of the surge whose effects are to be duplicated.

The voltage wave shown in Figure 2 is intended to represent the waveshape a surge source would produce across an open circuit. The waveshape will be different when the source is connected to a load having a lower impedance, and the degree to which it is lower is a function of the impedance of the source [6].

The degree to which source impedance is important depends largely on the type of surge suppressors that are used. The surge suppressors must be able to withstand the current passed through them by the surge source. A test generator of too high an impedance may not subject the device under test to sufficient stresses, while a generator of too low an impedance may subject protective devices to unrealistically severe stresses. A test voltage wave

specified without reference to source impedance could imply zero source impedance - one capable of producing that voltage across any impedance, even a short circuit. That would imply an infinite surge current, clearly an unrealistic situation.

4.2 Proposed Approach

Because of the wide range of possible source impedances and the difficulty of selecting a specific value, three broad categories of building locations are proposed to represent the vast majority of locations [7,8], from those near the service entrance to those remote from it. The source impedance of the surge increases from the outside to locations well within the building. Open-circuit voltages, on the other hand, show little variation within a building because the wiring provides little attenuation [9]. Table 1 outlines the three categories of building wiring.

Table 2 shows open-circuit voltages and short-circuit currents for each of the three categories. The energy deposited in a 500 V suppressor has been computed and is shown for each of the categories.

TABLE 1
Location Categories

A. Outside and Service Entrance	
Service drop from pole to building entrance	
Run between meter and distribution panel	
Overhead line to detached buildings	
Underground lines to well pumps	
B. Major Feeders and Short Branch Circuits	
Distribution panel devices	
Bus and feeder systems in industrial plants	
Heavy appliance outlets with "short" connections to the service entrance	
Lighting systems in commercial buildings	
C. Outlets and Long Branch Circuits	
All outlets at more than 10 m (30 ft) from Category B with wires #14-10	
All outlets at more than 20 m (60 ft) from Category A with wires #14-10	

TABLE 2
Ranges of Voltage and Currents

Location	Maximum Impulse	Energy Deposited in a 500 V Suppressor
A. Outdoor and Service Entrance	• 10 kV 1.2 x 50 μs for high-impedance circuits • 10 kA 8 x 20 μs for low-impedance circuits	150
B. Major Feeders and Short Branch Circuits	• 6 kV 1.2 x 50 μs for high-impedance circuits • 3 kA 8 x 20 μs for impedance circuits	40
C. Long Branch Circuits and Outlets	• 6 kV 0.5 μs x 100 kHz for high-impedance circuits • 500 A short circuit for low-impedance circuits	2
	• 6 kV 0.5 μs x 100 kHz for high-impedance circuits • 200 A short circuit for low-impedance circuits	0.8

The values shown in the table represent the maximum range, corresponding to the "High Exposure" situation of Figure 1. For less exposed systems, or when the prospect of a failure is not highly objectionable, one could specify lower values of open-circuit voltages with corresponding reductions in the currents.

5. Conclusion

The broad range of surge voltages occurring in low-voltage ac power circuits can be simulated by a limited set of test waves, for the purpose of evaluating their effects on equipment.

Field measurements, laboratory experiments, and calculations indicate that two basic waves, at various open-circuit voltages and short-circuit current values, can represent the majority of surges occurring in residential, commercial, and light industrial power systems rated up to 600 V rms.

Exceptions will be found to the simplification of a broad guideline; however, these should not detract from the benefits that can be expected from a reasonably valid uniformity in defining the environment. Other test waves of different shapes may be appropriate for other purposes, and the present guideline should not be imposed where it is not applicable.

6. Acknowledgments

The members of the IEEE Working Group contributed the data, shown in Appendix I, that shaped this guideline. Helpful comments, discussions, and reviews by all members of the Surge Protective Devices Committee are also acknowledged. The author is particularly indebted to the contributions of Catharine Fisher and Peter Richman for the presentation and discussion of the concepts and recommendations made in this guideline.

Appendix I - Data Base

Recordings and surge counter data have been contributed from several sources, in addition to the surge counter data obtained by members of the working group. Representative oscillograms and summary statistics are reproduced in this appendix, in support of the voltage levels and oscillatory wave proposals.

1. Recordings by Bell Telephone Laboratories

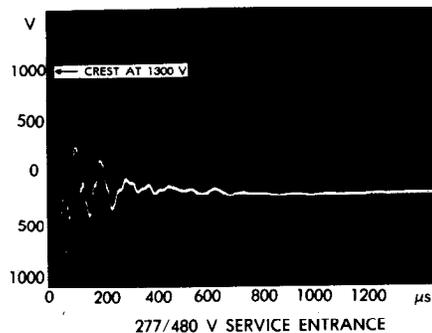
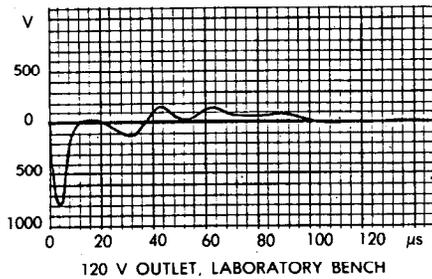
(Data contributed by P. Speranza, internal report, unpublished to date)

1.1 Typical Surge Counter Statistics

120 V line at BTL facility in Chester, New Jersey, during 42 months of monitoring:

- 146 counts at 300 to 500 V
- 14 counts at 500 to 1000 V
- 3 counts at 1000 to 1500 V
- 3 counts above 1500 V

1.2 Typical Automatic Recording Oscilloscopes



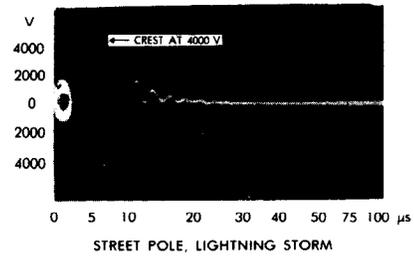
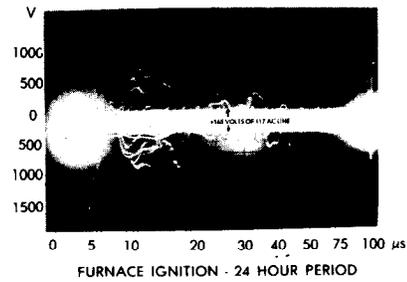
2. Recordings by General Electric Company

(Data contributed by F.D. Martzloff [2])

2.1 Surge Counter Statistics

- a) Three percent of all U.S. residences experience frequent occurrences (one per week or more) above 1200 V
- b) There is a 100:1 reduction in the rate of device failure when the withstand level is raised from 2 kV to 6 kV.

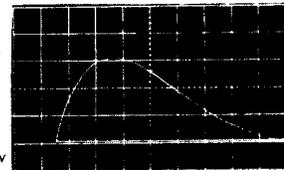
2.2 Typical Automatic Recording Oscilloscopes



2.3 Simulated Lightning Strokes on a Residential Power Circuit (Laboratory Model of System) [9]

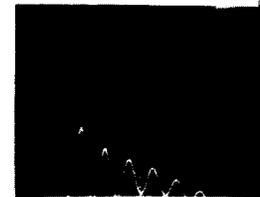
1.5 kA current impulse (8 x 20 μs approx.) is injected in ground wire only of service drop. (Higher currents produce flashover of wiring)

500 A/div
5 μs/div



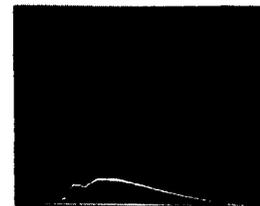
Recording of open-circuit voltage at a branch circuit outlet: 2200 V peak 500 kHz oscillations

500 V/div
2 μs/div



By connecting a 130 Ω load at the same outlet (1A load) the voltage is reduced to 1400 V peak, with more damping.

500 V/div
2 μs/div



Conclusions From This Test Series

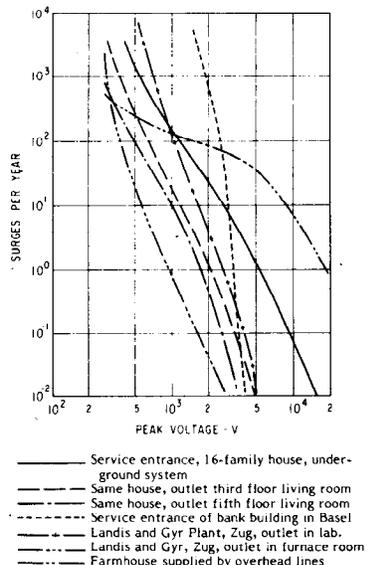
- 1. A current of 1.5 kA (moderate for a lightning discharge injected in the ground system) raises the wiring system of the house 2.2 kV above ground. Four kiloamperes (still a moderate value) will bring this voltage to 6 kV, the typical flashover value of the wiring.
- 2. A natural frequency of 500 kHz is excited by a unidirectional impulse.

- In this example, the source of the transient (from the loading effect of 130Ω) appears as

$$Z = 130 \Omega \left[\frac{2200}{1400} \right] - 1 = 75 \Omega$$

3. Statistics By Landis and Gyr Company

Surge counter data on various locations in Swiss 220 V systems (Data contributed by L. Regez - unpublished to date)



4. Working Group Surge Counter Statistics

Surge counters with four threshold levels (350, 500, 1000, and 1500 V) were used to record surge occurrences at various locations. Members of the Working Group installed these on 120 and 240 V systems of various types, including the following: outlets in urban, suburban, and rural residences; outlets in a hospital; secondary circuits on distribution system poles (recloser controls); secondary of pad-mounted distribution transformers; lighting circuits in an industrial plant; life test racks at an appliance manufacturer; bench power supply in a laboratory.

Summary Statistics of these measurements are as follows:

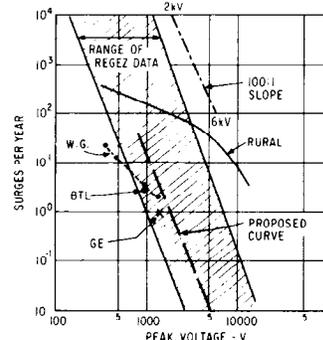
- Data base from 18 locations with a total recording time of 12 years spread over 4 calendar years, using 6 counters.
- Number of occurrences per year (weighted averages) at "average location."
 - 350 V: 22
 - 500 V: 11
 - 1000 V: 7
 - 1500 V: 3
- Significant extremes
 - One home with large number of surges caused by washer operation.
 - Four locations out of 18 never experienced a surge.
 - One home experienced several occurrences above 1500 V, with none below that value.
 - One industrial location (switching of a test rack) produced thousands of surges in the 350-500 V range, and several surges in excess of 1500 V. This location was left out of the average computation, but it exemplifies a significant extreme.

From the data base cited in the preceding pages, one can draw the chart below, including the following information on voltage vs frequency (rate) of occurrence:

- The Bell Laboratories data yield a point of 1000 V at about 2 occurrences per year (-).

- The General Electric counter statistics yield a point of 1200 V at about 1 occurrence per year (x).
- The General Electric clock data indicate a slope of 100:1 from 2 kV to 6 kV (---).
- The Regez data provide a band for the majority of locations (shown cross-hatched), with the exception of the rural location with long overhead line, which has more occurrences.
- Working Group statistics (-.-.-) indicate a less steep slope, perhaps because of the influence of outdoor locations included in the sample (similar to the rural data of Regez).

The proposed curve, which is the center of the ± 10 range of Figure 1, is shown in bold dashed lines (— — —). It has been drawn at the 100:1 slope, passing near the Bell and General Electric points and located within the band of the Regez data.



Appendix II - Effect of Transient Polarity Reversals on Semiconductors

Breakdown of semiconductors under various conditions of load and transient overvoltage applications has been investigated.† Evidence is presented in the two investigations cited that a reverse voltage applied during the conduction period of the power frequency produces lower breakdown voltages than the application of the same transient with no load or during blocking. Examples are given below, taken from these two investigations, showing statistically significant differences in the voltage levels.

		Average Breakdown (V)
IN1190 Diode*	Transient at no load	1973
	Fast wave under load	830
	Slow wave under load	1097
IN2160 Diode*	Transient at no load	2056
	Fast wave under load	894
	Slow wave under load	1106
IN679 Diode †	Transient applied at:	
	- peak of reverse voltage	1766
	-25° after start of conduction	1181
	-90° after start of conduction	906
	-155° after start of conduction	1115

This effect is one of the reasons for selecting an oscillatory waveform to represent the environment: it will be more likely to induce semiconductor failures than a unidirectional wave. Also, it shows the significance of the timing of the transient application with respect to the power frequency cycle.

Appendix III - Notes and References

1. Surge Voltage

Definitions of terms used in this guideline are consistent with IEEE Standard 100-1977, *Dictionary of Electrical and Electronic Terms*, 2nd ed.; however, some differences exist. For instance, IEEE Std 100-1977 defines a surge as a "transient wave of current, potential or power in the electric circuit"--a definition broader than that used

*Chowdhuri, P., "Transient-Voltage Characteristics of Silicon Power Rectifiers," IEEE IA-9, 5, September/October 1973, p. 582.

† F.D. Martzloff, internal report, unpublished.

here. *Transient overvoltage* is defined as "the peak voltage during the transient condition resulting from the operation of a switching device"--a definition more restricted than that of the present guideline.

2. Amplitudes of Strikes, Worst Case

The surge voltages described in this guideline include lightning effects on power systems, mostly strikes in the vicinity of a power line, or at a remote point of the power system. The literature describes the frequency of occurrence vs amplitude of lightning strikes, from the low levels of a few kiloamperes, through the median values of about 20 kA, to the exceptional values in excess of 100 kA. Clearly, a secondary arrester rated for 10 kA can protect adequately in case of a mild direct strike, or of a more severe strike divided among several paths to ground. However, a very high and direct strike will exceed the capability of an ANSI-rated secondary arrester.

References:

Cianos, N. and E.T. Pierce, *A Ground-Lightning Environment for Engineering Usage*, Stanford Research Institute, Menlo Park, CA 94205, August 1972.

Bodle, D.W., A.J. Ghazi, M. Syed, and R.L. Woodside, *Characterization of the Electrical Environment*, Toronto and Buffalo, N.Y.: University of Toronto Press, 1976.

Martzloff, F.D. and G.J. Hahn, "Surge Voltage in Residential and Industrial Power Circuits," *IEEE PAS-89*, 6, July/August 1970, 1049-1056.

3. Level vs Rate of Occurrence

The relationship between the level and the rate of occurrence of surges is partly caused by the attenuation of the surges as they propagate away from the source of the surge and divide among paths beyond branching points. Equipment at a given point will be subjected to a relatively small number of high-level surges from nearby sources, but to a larger number of surges from more remote sources.

4. Timing of Surges with Respect to Power Frequency

Lightning surges are completely random in their timing with respect to the power frequency. Switching surges are likely to occur near or after current zero, but variable load power factors will produce a quasi-random distribution. Some semiconductors, as shown in Appendix II, exhibit failure levels that depend on the timing of the surge with respect to the conduction of power frequency current. Gaps or other devices involving a power-follow current may withstand this power follow with success, depending upon the fraction of the half-cycle remaining after the surge before current zero. Therefore, it is important to consider the timing of the surge with respect to the power frequency. In performing tests, either complete randomization of the timing or controlled timing should be specified, with a sufficient number of timing conditions to reveal the most critical timing.

5. Oscillatory Surges During Lightning

The "classical lightning surge" has been established as $1.2 \times 50 \mu\text{s}$ for a voltage wave and $8 \times 20 \mu\text{s}$ for a current wave. Evidence has been collected, however, to show that oscillations can also occur. Lenz reports 50 lightning surges recorded in two locations, the highest at 5.6 kV, with frequencies ranging from 100 to 500 kHz. Martzloff reports oscillatory lightning surges in a house during a multiple-stroke flash.

References:

Lenz, J.E., "Basic Impulse Insulation Levels of Mercury Lamp Ballast for Outdoor Applications," *Illuminating Engrg.*, February 1964, pp. 133-140.

Martzloff, F.D. and G.J. Hahn, "Surge Voltage in Residential and Industrial Power Circuit," *IEEE PAS-89*, 6, July/August 1970, 1049-1056.

6. Surge Impedance and Source Impedance

To prevent misunderstanding, a distinction between *source impedance* and *surge impedance* needs to be made. Surge impedance, also called *characteristic impedance*, is a concept relating the parameters of a long line to the propagation of traveling waves. For the wiring practices of the ac power circuits discussed here, this characteristic impedance would be in the range of 150 to 300 Ω , but because the durations of the waves being discussed (50 to

20 μs) are much longer than the travel times in the wiring systems being considered, traveling wave analyses are not useful here.

Source impedance, defined as "the impedance presented by a source of energy to the input terminals of a device, or network" (IEEE Standard 100), is a more useful concept here.

7. Power System Source Impedance

The measurements from which Figure 1 was derived were of voltage only. Little was known about the impedance of the circuits upon which the measurements were made. Since then, measurements have been reported on the impedance of power systems. Bull reports that the impedance of a power system, seen from the outlets, exhibits the characteristics of a 50 Ω resistor with 50 μH in parallel. Attempts were made to combine the observed 6 kV open-circuit voltage with the assumption of a 50 Ω /50 μH impedance. This combination resulted in low energy deposition capability, which was contradicted by field experience of suppressor performance. The problem led to the proposed definition of oscillatory waves as well as high-energy unidirectional waves, in order to provide both the effects of an oscillatory wave and the high-energy deposition capability.

Reference:

Bull, J.H., "Impedance of the Supply Mains at Radio Frequencies," *Proceedings of 1st Symposium on EMC*, 75CH1012-4 Mont., Montreux, May 1975.

8. Installation Categories

Subcommittee 28A of the International Electrotechnical Commission has prepared a report, referenced below, in which installation categories are defined. These installation categories divide the power systems according to the location in the building, in a manner similar to the location categories defined in this guideline. However, there are some significant differences between the two concepts. First, the IEC categories are defined for a "Controlled Voltage Situation," a phrase that implies the presence of some surge suppression device or surge attenuation mechanism to reduce the voltage levels from one category to the next. Second, the IEC report is more concerned with insulation coordination than with the application of surge protective devices; therefore it does not address the question of the coordination of the protectors, but rather the coordination of insulation levels - that is, voltages. Source impedances, in contrast to this guideline, have not been defined. Further discussion and work toward the application guidelines of both documents should eventually produce a consistent set of recommendations.

Reference:

Insulation Coordination Within Low-Voltage Systems Including Clearances and Creepage Distances for Equipment, International Electrotechnical Commission, Report SC28A (Central Office) 5, to be published in 1979.

9. Open-Circuit Voltages and Wiring Flashover

Surges propagate with very little attenuation in a power system with no substantial connected loads. Measurements made in an actual residential system as well as in a laboratory simulation have shown that the most significant limitation is produced by wiring flashover, not by attenuation along the wires. Ironically, a carefully insulated installation is likely to experience higher surge voltages than an installation where wiring flashover occurs at low levels. Therefore, the open-circuit voltage specified at the origin of a power system must be assumed to propagate unattenuated far into the system, which is the reason for maintaining the 6 kV surge specification when going from the "B" location to the "C" location.

References:

Martzloff, F.D. and K.E. Crouch "Coordination de la protection contre les surtensions dans les reseaux basse tension residuels," *Proceedings, 1978 IEEE Canadian Conference on Communications and Power*, 78CH1373-O, pp. 451-454.

Martzloff, F.D. *Surge Voltage Suppression in Residential Power Circuits*, Report 76CRD092, Corporate Research and Development, General Electric Company, Schenectady, N.Y., 1976.

**DEVELOPMENT
OF A
GUIDELINE
ON SURGE
VOLTAGES IN
LOW-VOLTAGE
AC POWER
CIRCUITS**

The Development of a Guideline on Surge Voltages in Low-Voltage AC Power Circuits

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Significance:

Part 2 Development of standards – Reality checks

Progress report to the IEEE PES community on the development of what became IEEE Std 587-1980.

Explains the proposition that a Ring Wave should be added to the traditional unidirectional impulses

NOTE Parallel presentation of the subject made to the European EMC community under the title "*A Guideline on Surge Voltages in AC Power circuits rated up to 600 V*"

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Abstract - Surge voltages in ac power circuits become more significant with the increased application of miniaturized electronics in consumer and industrial products. A Working Group of IEEE is preparing a Guideline describing the nature of these surges in ac power circuits up to 600 V.

The paper describes the data base and approach used by the Working Group and the recommendations proposed to represent typical surges, in order to obtain feedback before the final writing of the Guideline.

Two waveforms are proposed, one oscillatory, the other unidirectional, depending on the location within the power system. Recommendations for source impedance or short-circuit current are also included.

INTRODUCTION

Surge voltages occurring in ac power circuits can be the cause of misoperation or product failure for residential as well as industrial systems. The problem has received increased attention in recent years because miniaturized solid state devices are more sensitive to voltage surges (spikes and transients) than were their predecessors.

Although surge voltage amplitudes and their frequency of occurrence on unprotected circuits are well known, their waveshapes and energy content are less well known. On the basis of measurements, statistics, and theoretical considerations, a practical guideline for outlining the environment for use in predicting extreme waveshapes and energy content can nevertheless be established. A Working Group of the Surge Protective Devices Committee is currently developing such a guideline; this paper reports the status of the Guideline, presents the considerations which led to the approach chosen, and provides a possible vehicle for discussion before the final writing and publication of the Guideline.

SCOPE

The Guideline primarily addresses ac power circuits with rated voltages up to 600 V, although some of the conclusions offered could apply to higher voltages and also to some dc power systems. Other standards have been established, such as IEEE 472, *Guide for Surge Withstand Capability (SWC) Tests*, intended for the special case of high-voltage substation environments, and IEEE 28, *Standard for Surge Arresters for ac Power Circuits*, covering primarily the utilities environment. The Guideline intends to complement, not conflict with, existing standards.

The surge voltages considered in the Guideline are those exceeding two per unit (or twice the peak operating voltage) and having durations ranging from a fraction of a microsecond to a millisecond. Overvoltages of less than two per unit are not covered, nor are transients of longer duration resulting from power equipment operation and failure modes. Because these low-amplitude and long-duration surges are generally not amenable to suppression by conventional surge protective devices, they require different protection techniques.

Definitions of terms used in the Guideline are consistent with IEEE Standard 100-1977, *Dictionary of Electrical and Electronic Terms*, 2nd ed.; however, some differences exist. For instance, IEEE Std 100-1977 defines a *surge* as a "transient wave of current, potential or power in the electric circuit"—a definition broader than that used here. *Transient overvoltage* is defined as "the peak voltage during the transient condition resulting from the operation of a switching device"—a definition more restricted than that of the Guideline.

While the major purpose of the Guideline is to describe the environment, a secondary purpose is to lead toward standard tests,

through an application guide that will be prepared in the future. These standard tests will provide a realistic evaluation of the surge withstand capability of equipment connected to these power circuits. Of necessity, the complex real situation must be simplified to produce a manageable set of standards. One must recognize the unavoidably arbitrary character of any standard and be prepared to accept an imperfect approach which can simplify matters, rather than demand a perfect but unattainable match between the actual situation and the standard.

THE ORIGIN OF SURGE VOLTAGES

Surge voltages occurring in low-voltage ac power circuits originate from two major sources: load switching transients and direct or indirect lightning effects on the power system. Load switching transients can be further divided into transients associated with (1) major power system switching disturbances, such as capacitor bank switching; (2) minor switching near the point of interest, such as an appliance turnoff in a household or the turnoff of other loads in an individual system; (3) resonating circuits associated with switching devices, such as thyristors; and (4) various system faults, such as short circuits and arcing faults. Measurements and calculations of lightning effects have been made to yield data on what levels can be produced, even if the exact mechanism of any particular surge is unknown. The major mechanisms by which lightning produces surge voltages are the following:

- A direct lightning strike to a primary circuit injects high currents into the primary circuit, producing voltages by either flowing through ground resistance or flowing through the surge impedance of the primary conductors.
- A lightning strike that misses the line but hits a nearby object sets up electromagnetic fields which can induce voltages on the conductors of the primary circuit.
- The rapid collapse of voltage that occurs when a primary arrester operates to limit the primary voltage couples effectively through the capacitance of the transformer and produces surge voltages in addition to those coupled into the secondary circuit by normal transformer action.
- Lightning strikes the secondary circuits directly. Very high currents can be involved, exceeding the capability of conventional devices.
- Lightning ground current flow resulting from nearby direct-to-ground discharges couples onto the common ground impedance paths of the grounding network.

Fast-acting protection devices, such as current-limiting fuses and circuit breakers capable of clearing or beginning to part contacts in less than 2 ms, leave trapped inductive energy in the circuit upstream; upon collapse of the field, very high voltages are generated.

Transient overvoltages associated with the switching of power factor correction capacitors [1] have lower frequencies than the high-frequency spikes with which this document is concerned. Their levels, at least in the case of restrike-free switching operations, are generally less than twice normal voltage and therefore are not of substantial concern here, but should not be overlooked.

On the other hand, switching operations involving restrikes, such as those produced by air contactors or mercury switches, can produce, through escalation, surge voltages of complex waveshapes and of amplitudes several times greater than the normal system voltage. The severest case is generally found on the load side of the switch and involves only the device that is being switched. While this situation should certainly not be ignored, in such a case the prime responsibility for protection rests with the local user of the device in question. However, switching transients can also appear on the line side across devices connected to the line. The presence and source of transients may be unknown to the users of those devices. This potentially harmful situation occurs often enough to command attention.

While the data have been recorded primarily on 120, 220/380, or 277/480 V systems, the general conclusions should be valid for 600 V systems. To the extent that surge voltages are produced by a discrete amount of energy being dumped into a power system, low-impedance, heavy industrial systems can be expected to experience lower peaks from surge voltages than 120 V residential systems, but comparable, or greater, amounts of energy potentially available for deposition in a surge suppressor.

A 79 428-4 A paper recommended and approved by the IEEE Surge Protective Devices Committee of the IEEE Power Engineering Society for presentation at the IEEE PES Summer Meeting, Vancouver, British Columbia, Canada, July 15-20, 1979. Manuscript submitted February 6, 1979; made available for printing April 3, 1979.

OCCURRENCE AND VOLTAGE LEVELS IN UNPROTECTED CIRCUITS

Rate of Occurrence Versus Voltage Level

The rate of occurrence of surges varies over wide limits, depending on the particular system. Prediction of the rate for a particular system is always difficult and frequently impossible. Rate is related to the level of the surges; low-level surges are more prevalent than high-level surges. The relationship between the level and the rate of occurrence of surges is partly caused by the attenuation of the surges as they propagate away from the source of the surge and divide among paths beyond branching points. Equipment at a given point will be subjected to a relatively small number of high-level surges from nearby sources, but to a larger number of surges from more remote sources.

Data collected from many sources have led to the plot shown in Fig. 1. This prediction shows with certainty only a relative frequency of occurrence, while the absolute number of occurrences can be described only for an "average location." The "high exposure" and "low exposure" limits of the band are shown as a guide, not as absolute limits, to reflect both the location exposure (lightning activity in the area and the nature of the system) and the exposure to switching surges created by other loads.

The literature describes the frequency of occurrence vs amplitude of lightning strikes, from the low levels of a few kiloamperes, through the median values of about 20 kA, to the exceptional values in excess of 100 kA [2]. Clearly, a secondary arrester rated for 10 kA can protect adequately in case of a mild direct strike, or of a more severe strike divided among several paths to ground. However, a very high and direct strike will exceed the capability of an ANSI-rated secondary arrester [3].

The voltage and current amplitudes presented in the Guideline attempt to provide for the vast majority of lightning strikes but should not be considered as "worst case," since this concept cannot be determined realistically. One should think in terms of the statistical distribution of strikes, accepting a reasonable upper limit for most cases. Where the consequences of a failure are not catastrophic but merely represent an annoying economic loss, it is appropriate to make a trade-off of the cost of protection against the likelihood of a failure caused by a high but rare surge. For instance, a manufacturer may be concerned with nation-wide failure rates, those at the upper limits of the distribution curve, while the user of a specific system may be concerned with a single failure occurring at a specific location under "worst-case conditions." Rates can be estimated for average systems, however, and even if imprecise, they provide manufacturers and users with guidance. Of equal importance is the observation that surges in the range of 1 to 2 kV are fairly common in residential circuits.

From the relative values of Fig. 1, two typical levels can be cited for practical applications. First, the expectation of a 3 kV transient occurrence on a 120 V circuit ranges from 0.01 to 1 per year at a given location - a number sufficiently high to justify the recommendation of a minimum 3 kV withstand capability. Second, the wiring flashover limits indicate that a 6 kV withstand capability may be sufficient to ensure device survival indoors, but a 10 kV withstand capability may be required outdoors.

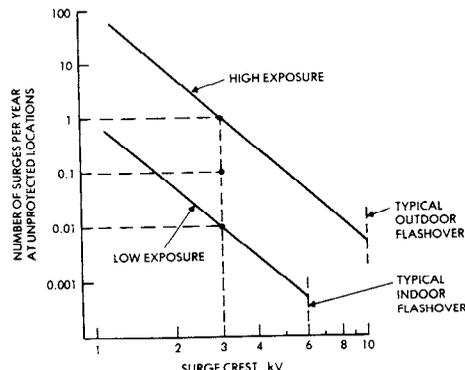


Fig. 1. Rate of surge occurrence vs voltage level.

Timing of Occurrence

Surges occur at random times with respect to the power frequency, and the failure mode of equipment may be affected by the power frequency follow current or by the timing. Consequently, surge testing must be done with the ac voltage applied to the test piece.

Lightning surges are completely random in their timing with respect to the power frequency. Switching surges are likely to occur

near or after current zero, but variable load power factors will produce a quasi-random distribution. Some semiconductors, as shown in Appendix II, exhibit failure levels that depend on the timing of the surge with respect to the conduction of power frequency current. Gaps or other devices involving a power-follow current may withstand this power follow with success, depending upon the fraction of the half-cycle remaining after the surge before current zero. Therefore, it is important to consider the timing of the surge with respect to the power frequency. In performing tests, either complete randomization of the timing or controlled timing should be specified, with a sufficient number of timing conditions to reveal the most critical timing.

WAVESHAPE OF REPRESENTATIVE SURGE VOLTAGES

Waveshapes in Actual Occurrences

Indoor - Measurements in the field, measurements in the laboratory, and theoretical calculations indicate that most surge voltages in indoor low-voltage systems have oscillatory waveshapes, unlike the well-known and generally accepted unidirectional waves specified in high-voltage insulation standards. A surge impinging on the system excites the natural resonant frequencies of the conductor system. As a result, not only are the surges typically oscillatory, but surges may have different amplitudes and waveshapes at different places in the system. These oscillatory frequencies of surges range from 5 kHz to more than 500 kHz. A 30 to 100 kHz frequency is a realistic measure of a "typical" surge for most residential and light industrial ac line networks.

Outdoor - Surges encountered in outdoor locations have also been recorded, some being oscillatory, others being unidirectional. The "classical lightning surge" has been established as $1.2 \times 50 \mu\text{s}$ for a voltage wave and $8 \times 20 \mu\text{s}$ for a current wave. Evidence has been collected, however, to show that oscillations can also occur. Lenz [4] reports 50 lightning surges recorded in two locations, the highest at 5.6 kV, with frequencies ranging from 100 to 500 kHz. Martzloff [5] reports oscillatory lightning surges in a house during a multiple-stroke flash.

Because the overriding concern here is the energy associated with these surges, a conservative but realistic description of the surges can be derived from the long-established specified duty of a secondary arrester, as detailed below. While this specification is arbitrary, it has the strength of experience and successful usage.

Selection of Representative Waveshapes

The definition of a waveshape to be used as representative of the environment is important for the design of candidate protective devices, since unrealistic requirements, such as excessive duration of the voltage or very low source impedance, place a high energy requirement on the suppressor, with a resulting cost penalty to the end user. The two requirements defined below reflect this trade-off.

Indoor - Based on measurements conducted by several independent organizations in 120 and 240 V systems (Appendix I), the waveshape shown in Fig. 2 is reasonably representative of surge voltages in these power circuits. Under the proposed description of a "0.5 μs - 100 kHz ring wave," this waveshape rises in 0.5 μs , then decays while oscillating at 100 kHz, each peak being about 60% of the preceding peak.

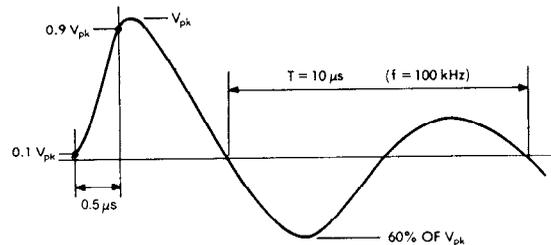


Fig. 2. The proposed 0.5 μs - 100 Hz ring wave (open-circuit voltage).

The fast rise can produce the effects associated with nonlinear voltage distribution in windings and the dv/dt effects on semiconductors. Shorter rise times are found in many transients, but, as those transients propagate into the wiring or are reflected from discontinuities in the wiring, the rise time becomes longer.

The oscillating and decaying tail produces the effects of voltage polarity reversals in surge suppressors or other devices that may be sensitive to polarity changes. Some semiconductors are particularly sensitive to damage when being forced into or out of a conducting state, or when the transient is applied during a particular portion of the 60 Hz supply cycle (Appendix II). The response of a surge

suppressor can also be affected by reversals in the polarity, as in the case of RC attenuation before a rectifier circuit in a dc power supply. The pulse withstand capability of many semiconductors tends to improve if the surge duration is much shorter than one microsecond. For this reason, the first half-cycle of the test wave must have a sufficient duration.

Outdoor - In the outdoor and service entrance environment, as well as in locations close to the service entrance, substantial energy, or current, is still available. For these locations, the unidirectional impulses long established for secondary arresters are more appropriate than the oscillatory wave.

Accordingly, the recommended waveshape is $1.2 \times 50 \mu\text{s}$ for open-circuit voltages and $8 \times 20 \mu\text{s}$ for short-circuit current (impulse discharge current) or current in a low-impedance device. The numbers used to describe the impulse, 1.2×50 and 8×20 , are as defined in IEEE Standard 28 - ANSI Standard C62.1; Fig. 3 presents the waveshape and a graphic description of the numbers.

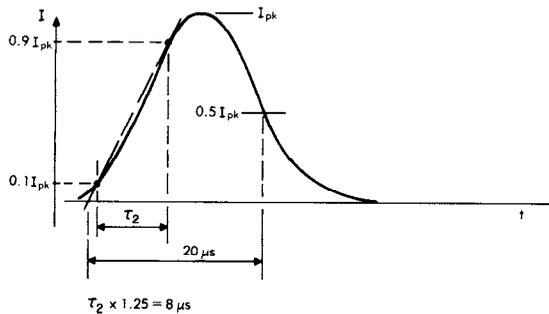
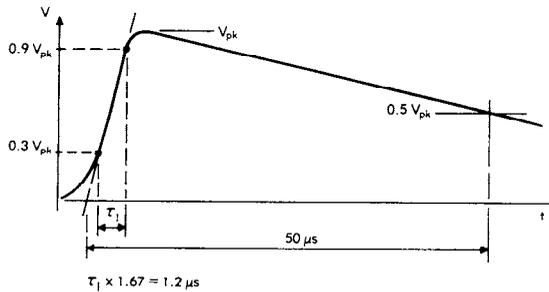


Fig. 3. Waveshapes for outdoor locations.

ENERGY AND SOURCE IMPEDANCE

General

The energy involved in the interaction of a power system with a surge source and a surge suppressor will divide between the source and the suppressor in accordance with the characteristics of the two impedances. In a gap-type suppressor, the low impedance of the arc after sparkover forces most of the energy to be dissipated elsewhere: for instance, in a resistor added in series with the gap for limiting the power-follow current. In an energy-absorber suppressor, by its very nature, a substantial share of the surge energy is dissipated in the suppressor, but its clamping action does not involve the power-follow energy resulting from the short-circuit action of a gap. It is therefore essential to the effective use of suppression devices that a realistic assumption be made about the source impedance of the surge whose effects are to be duplicated.

The voltage wave shown in Fig. 2 is intended to represent the waveshape a surge source would produce across an open circuit. The waveshape will be different when the source is connected to a load having a lower impedance, and the degree to which it is lower is a function of the impedance of the source.

To prevent misunderstanding, a distinction between *source impedance* and *surge impedance* needs to be made. Surge impedance, also called *characteristic impedance*, is a concept relating the parameters of a long line to the propagation of traveling waves. For the wiring practices of the ac power circuits discussed here, this characteristic impedance would be in the range of 150 to 300 Ω , but because the durations of the waves being discussed (50 to 20 μs) are much longer than the travel times in the wiring systems being considered, traveling wave analyses are not useful here.

Source impedance, defined as "the impedance presented by a source of energy to the input terminals of a device, or network" (IEEE Standard 100), is a more useful concept here. In the conventional

Thevenin's description, the open-circuit voltage (at the terminals of the network or test generator) and the source impedance (of the surge source or test generator) are sufficient to calculate the short-circuit current, as well as any current for a specified suppressor impedance.

The measurements from which Fig. 1 was derived were of voltage only. Little was known about the impedance of the circuits upon which the measurements were made. Since then, measurements have been reported on the impedance of power systems. Bull [6] reports that the impedance of a power system, seen from the outlets, exhibits the characteristics of a 50 Ω resistor with 50 μH in parallel. Attempts were made to combine the observed 6 kV open-circuit voltage with the assumption of a 50 Ω /50 μH impedance [7]. This combination resulted in low energy deposition capability, which was contradicted by field experience of suppressor performance. The problem led to the proposed definition of oscillatory waves as well as high-energy unidirectional waves, in order to provide both the effects of an oscillatory wave and the high-energy deposition capability.

The degree to which source impedance is important depends largely on the type of surge suppressors that are used. The surge suppressors must be able to withstand the current passed through them by the surge source. A test generator of too high an impedance may not subject the device under test to sufficient stresses, while a generator of too low an impedance may subject protective devices to unrealistically severe stresses. A test voltage wave specified without reference to source impedance could imply zero source impedance - one capable of producing that voltage across any impedance, even a short circuit. That would imply an infinite surge current, clearly an unrealistic situation.

Because of the wide range of possible source impedances and the difficulty of selecting a specific value, three broad categories of building locations are proposed to represent the vast majority of locations, from those near the service entrance to those remote from it. The source impedance of the surge increases from the outside to locations well within the building. Open-circuit voltages, on the other hand, show little variation within a building because the wiring provides little attenuation. Figure 4 illustrates the application of the three categories to the wiring of a building.

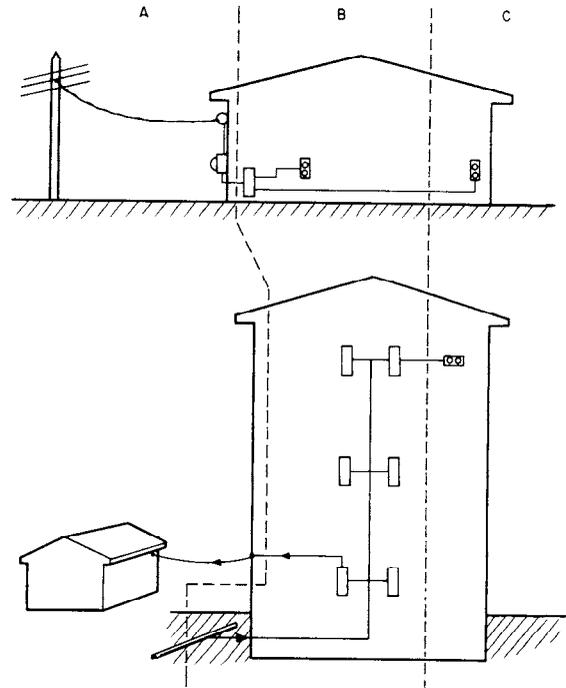


Fig. 4. Location categories.

- | | |
|---|--|
| <p>A. Outside and Service Entrance</p> <ul style="list-style-type: none"> Service drop from pole to building entrance Run between meter and distribution panel Overhead line to detached buildings Underground lines to well pumps | <p>B. Major Feeders and Short Branch Circuits</p> <ul style="list-style-type: none"> Distribution panel devices Bus and feeder systems in industrial plants Heavy appliance outlets with "short" connections to the service entrance Lighting systems in commercial buildings |
| <p>C. Outlets and Long Branch Circuits</p> <ul style="list-style-type: none"> All outlets at more than 10 m (30 ft) from Category B with wires #14-10 | |

Subcommittee 28A of the International Electrotechnical Commission has prepared a Report [8], in which installation categories are defined. These installation categories divide the power systems according to the location in the building, in a manner similar to the location categories defined in the Guideline. However, there are some significant differences between the two concepts. First, the IEC categories are defined for a "controlled voltage situation," a phrase that implies the presence of some surge suppression device or surge attenuation mechanism to reduce the voltage levels from one category to the next. Second, the IEC report is more concerned with insulation coordination than with the application of surge protective devices; therefore it does not address the question of the coordination of the protectors but, rather, the coordination of insulation levels – that is, voltages.

Surges propagate with very little attenuation in a power system with no substantial connected loads. Measurements made in an actual residential system as well as in a laboratory simulation have shown that the most significant limitation is produced by wiring flashover, not by attenuation along the wires. Ironically, a carefully insulated installation is likely to experience higher surge voltages than an installation where wiring flashover occurs at low levels. Therefore, the open-circuit voltage specified at the origin of a power system must be assumed to propagate unattenuated far into the system, which is the reason for maintaining the 6 kV surge specification when going from one category to an adjacent category farther into the building.

Furthermore, source impedances are not defined in the IEC report. The Guideline attempts to fill this need by specifying several levels of source impedance, or of short-circuit current, for the various categories.

PROPOSED REPRESENTATION OF THE ENVIRONMENT

On the basis of the preceding discussions, the Guideline proposes to reduce the infinite variety of actual conditions to three categories, from the outside service drops to the long branch circuits and outlets.

For each category the most appropriate waveshape is indicated, an open-circuit voltage for high-impedance loads, or a short-circuit current for low-impedance loads. The tabulation that follows shows open-circuit voltages and short-circuit currents for each of the three categories. The energy deposited in a 500 V suppressor has been computed and is shown for each of the categories.

The values shown in the table represent the maximum range, corresponding to the "High Exposure" situation of Fig. 1. For less exposed systems, or when the prospect of a failure is not highly objectionable, one could specify lower values of open-circuit voltages with corresponding reductions in the currents. IEC Category I, not represented in the Guideline, would correspond to line cord-connected devices in this context.

CONCLUSIONS

The broad range of surge voltages occurring in low-voltage ac power circuits can be simulated by a limited set of test waves, for the purpose of evaluating their effects on equipment.

Field measurements, laboratory experiments, and calculations indicate that two basic waves, at various open-circuit voltages and short-circuit current values, can represent the majority of surges occurring in residential, commercial, and light industrial power systems rated up to 600 V rms.

Exceptions will be found to the simplification of a broad guideline; however, these should not detract from the benefits that can be expected from a reasonably valid uniformity in defining the environment. Other test waves of different shapes may be appropriate for other purposes, and the present guideline should not be imposed where it is not applicable.

The Working Group is approaching the final phases of preparation of the Guideline document; comments are solicited from the engineering and user communities. However, readers must recognize the unavoidably arbitrary character of any standard and be prepared to accept an imperfect approach, which can simplify matters and clarify the issues as well as provide uniform evaluations of performances, rather than demand a perfect but unattainable match between the actual situation and the standard.

ACKNOWLEDGMENTS

The concepts presented in this paper have greatly benefited from the informed questions and discussions by members of the Working Group on Surge Voltage in AC Power Circuits Rated 600 V or Less, and from interested reviewers; particular appreciation for effective critiques from Catharine Fisher and Peter Richman is acknowledged. The data base presented in Appendix I has been broadened by the contributions of the Bell Telephone Laboratories and Landis & Gyr, Inc.

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Recommended Surge Voltages and Currents Deemed to Represent the Environment

Location Category	Comparable to IEC SC28A Category	Impulse Waveform	Maximum* Amplitude	Type of Specimen or Load Circuit	Energy Deposited in a 500 V Suppressor (joules)
A. Outdoor and Service Entrance	IV	1.2 x 50 μs	10 kV o.c.	High Impedance	--
		8 x 20 μs	10 kA s.c.	Low Impedance	150
B. Major Feeders and Short Branch Circuits	III	1.2 x 50 μs	6 kV o.c.	High Impedance	--
		8 x 20 μs	3 kA s.c.	Low Impedance	40
		0.5 μs - 100 kHz	{ 6 kV o.c. 500 A s.c.	{ High Impedance Low Impedance	{ -- 2
C. Long Branch Circuits and Outlets	II	0.5 μs - 100 kHz	6 kV o.c.	High Impedance	--
			200 A s.c.	Low Impedance	0.8

*o.c.: open-circuit voltage s.c.: short-circuit current

APPENDIX I - DATA BASE

Recordings and surge counter data have been contributed from several sources, in addition to the surge counter data obtained by members of the Working Group. Representative oscillograms and summary statistics are reproduced in this appendix, in support of the voltage levels and oscillatory wave proposals.

1. Recordings by Bell Telephone Laboratories

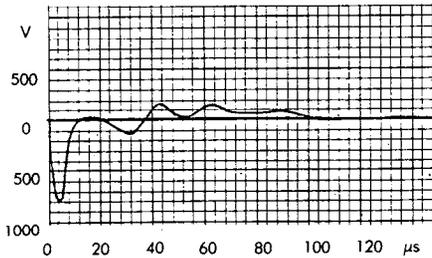
(Data contributed by P. Speranza, internal report, unpublished to date)

1.1 Typical Surge Counter Statistics

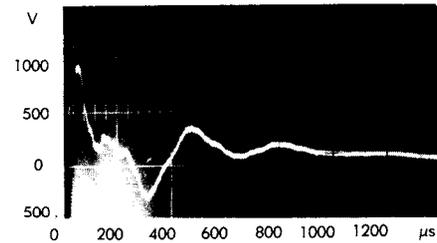
120 V line at BTL facility in Chester, New Jersey, during 42 months of monitoring:

- 146 counts at 300 to 500 V
- 14 counts at 500 to 1000 V
- 3 counts at 100 to 1500 V
- 3 counts above 1500 V

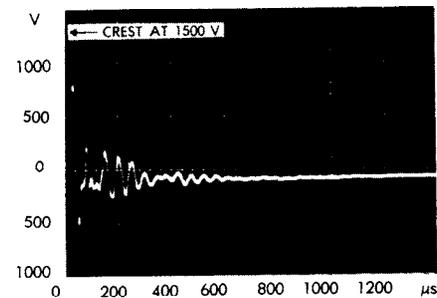
1.2 Typical Automatic Recording Oscilloscopes



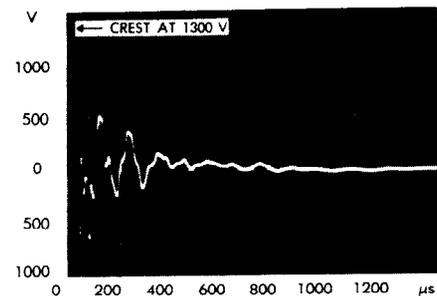
120 V OUTLET, LABORATORY BENCH



277/480 V SERVICE ENTRANCE



277/480 V SERVICE ENTRANCE



277/480 V SERVICE ENTRANCE

2. Recordings by General Electric Company

2.1 Surge Counter Statistics - Martzloff, F.D. and G.J. Hahn, "Surge Voltage in Residential and Industrial Power Circuits, *IEEE Pas-89*, 6, July/August 1970, 1049-1056.

- a) Three percent of all U.S. residences experience frequent occurrences (one per week or more) above 1200 V.
- b) There is a 100:1 reduction in the rate of device failure when the withstand level is raised from 2 kV to 6 kV.

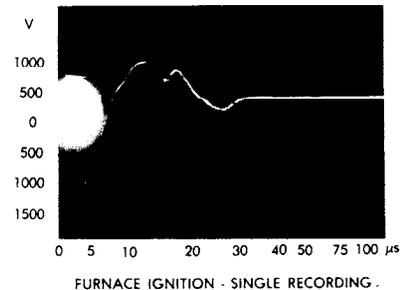
Number of Houses with Repetitive Surge Activity Above 1200 Volts

Location	Number of Homes Surveyed	Recording Period (weeks)	Houses with Repetitive Surges
Providence, R. I.	4	2-6	none
Cleveland, Ohio	28	2-4	none
Auburn, N. Y.	12	2-3	none
Lynchburg, Va.	3	2-3	none
Syracuse, N. Y.	8	1-2	1
Chicago, Ill.	23	1-6	none
Ashland, Mass.	24	1-2	1
Holland, Mich.	6	2-10	none
Louisville, Ky.	10	2-6	none
Somersworth, N. H.	50	1-2	1
Plainville, Conn.	5	10	none
Asheboro, N. C.	24	1-2	none
Fort Wayne, Ind.	38	1-4	3
DeKalb, Ill.	14	3-12	none

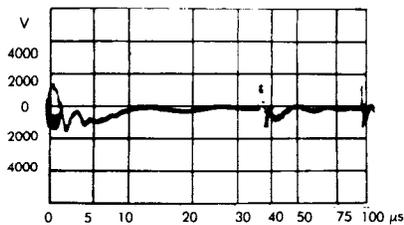
Surge Counter Recordings Above 1200 Volts (Spring, Summer, and Fall)

Location	Number of Homes	Total Homes X Weeks	Number of Surges
Providence, R. I.	6	60	1
Ashboro, N. C.	13	85	none
DeKalb, Ill.	11	60	2
Somersworth, N. H.	3	48	1
Chicago, Ill.	12	58	none
Cleveland, Ohio	8	106	1
Decatur, Ill.	12	72	2
Holland, Mich.	7	56	none
Auburn, N. Y.	3	70	none
Springfield, Pa.	1	24	none
Ashland, Mass.	6	72	none
Pittsfield, Mass.	3	60	1
Plainville, Conn.	3	60	none
Lynchburg, Va.	3	15	none
Total	91	846	8 in 8 homes

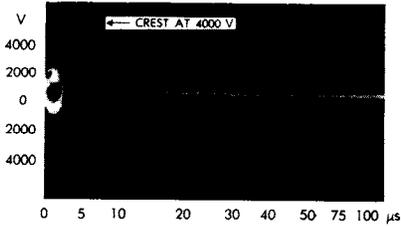
2.2 Typical Automatic Recording Oscilloscopes



FURNACE IGNITION - SINGLE RECORDING.



SERVICE ENTRANCE, LIGHTNING STORM

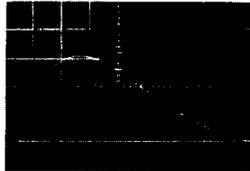


STREET POLE, LIGHTNING STORM

2.3 Simulated Lightning Strokes on a Residential Power Circuit (Laboratory Model of System) - Martzloff, F.D. and K.E. Crouch, "Coordination De La Protection Contre Les Surtensions Dans Les Réseaux Basse Tension Résidentiels," *Proceedings, 1978 IEEE Canadian Conference on Communications and Power, 78CH1373-0*, pp. 451-454.

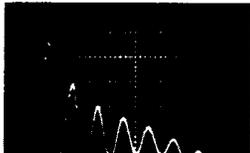
1.5 kA current impulse (8 x 20 μs approx.) is injected in ground wire only of service drop. (Higher currents produce flashover of wiring.)

500 A/div
5 μs/div



Recording of open-circuit voltage at a branch circuit outlet:
2200 V peak 500 kHz oscillations

500 V/div
2 μs/div



By connecting a 130 Ω load at the same outlet (1 A load) the voltage is reduced to 1400 V peak, with more damping.

500 V/div
2 μs/div



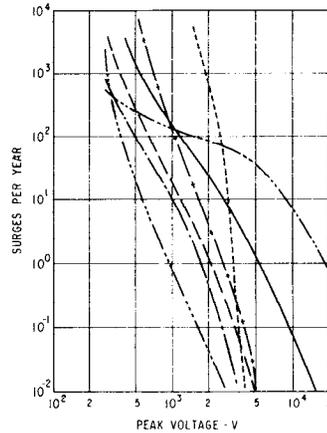
Conclusions from this test series

1. A current of 1.5 kA (moderate for a lightning discharge injected in the ground system) raises the wiring system of the house 2.2 kV above ground. Four kiloamperes (still a moderate value) will bring this voltage to 6 kV, the typical flashover value of the wiring.
2. A natural frequency of 500 kHz is excited by a unidirectional impulse.
3. In this example, the source of the transient (from the loading effect of 130 Ω) appears as

$$Z = 130 \Omega \left[\frac{2200}{1400} - 1 \right] = 75 \Omega$$

3. Statistics by Landis & Gyr, Inc.

Surge counter data on various locations in Swiss 220 V systems (Data contributed by L. Regez - unpublished to date)



- Service entrance, 16-family house, underground system
- - - Same house, outlet third floor living room
- - - Same house, outlet fifth floor living room
- - - Service entrance of bank building in Basel
- + - Landis and Gyr Plant, Zug, outlet in lab.
- - - Landis and Gyr, Zug, outlet in furnace room
- - - Farmhouse supplied by overhead lines

Frequency of Voltage Transients per Year as a Function of the Peak Value of the Voltage Transient for a 220 V, 50 Hz Distribution System with Grounded Neutral

4. Working Group Surge Counter Statistics

Surge counters with four threshold levels (350, 500, 1000, and 1500 V) were made available to the Working Group by Joslyn Electronic Systems, for recording surge occurrences at various locations. Members of the Working Group installed these on 120 and 240 V systems of various types, including the following: outlets in urban, suburban, and rural residences; outlets in a hospital; secondary circuits on distribution system poles (recloser controls); secondary of pad-mounted distribution transformers; lighting circuits in an industrial plant; life test racks at an appliance manufacturer; bench power supply in a laboratory.

Limitations on the availability of personnel and communications made this sampling less than optimum from a statistical point of view. However, by computing weighted averages for each location, one can quote an acceptable overall average; this average has been included in the graph drawn to establish the low and high exposure limits.

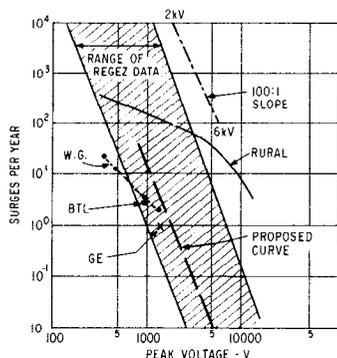
Summary Statistics of these measurements are as follows:

1. Data base from 18 locations with a total recording time of 12 years spread over 4 calendar years, using 6 counters.
2. Number of occurrences per year (weighted averages) at "average location."
 - 350 V: 22
 - 500 V: 11
 - 1000 V: 7
 - 1500 V: 3
3. Significant extremes
 - One home with large number of surges caused by washer operation
 - Four locations out of 18 never experienced a surge.
 - One home experienced several occurrences above 1500 V, with none below that value.
 - One industrial location (switching of a test rack) produced thousands of surges in the 350-500 V range, and several surges in excess of 1500 V. This location was left out of the average computation, but it exemplifies a significant extreme.

From the data base cited in the preceding pages, one can draw the chart below, including the following information on voltage vs frequency (rate) of occurrence:

1. The Bell Laboratories data yield a point of 1000 V at about 2 occurrences per year (●).
2. The General Electric counter statistics yield a point of 1200 V at about 1 occurrence per year (x).
3. The General Electric clock data indicate a slope of 100:1 from 2 kV to 6 kV (— — —).
4. The Regez data provide a band for the majority of locations (shown cross-hatched), with the exception of the rural location with long overhead line, which has more occurrences.
5. Working Group statistics (●—●—) indicate a less steep slope, perhaps because of the influence of outdoor locations included in the sample (similar to the rural data of Regez).

The proposed curve, which is the center of the ± 10 range of Fig. 1 in the Guideline, is shown in bold dashed lines (— — —). It has been drawn at the 100:1 slope, passing near the Bell and General Electric points and located within the band of the Regez data.



APPENDIX II - EFFECT OF TRANSIENT POLARITY REVERSALS ON SEMICONDUCTORS

Breakdown of semiconductors under various conditions of load and transient overvoltage applications has been investigated.*† Evidence is presented in the two investigations cited that a reverse voltage applied during the conduction period of the power frequency produces lower breakdown voltage than the application of the same transient with no load or during blocking. Examples are given below, taken from these two investigations, showing statistically significant differences in the voltage levels.

Average Breakdown (V)

IN1190 Diode*	Transient at no load	1973
	Fast wave under load	830
	Slow wave under load	1097
IN2160 Diode*	Transient at no load	2056
	Fast wave under load	894
	Slow wave under load	1106
IN679 Diode †	Transient applied at:	
	- peak of reverse voltage	1766
	- 25° after start of conduction	1181
	- 90° after start of conduction	906
	- 155° after start of conduction	1115

This effect is one of the reasons for selecting an oscillatory waveform to represent the environment: it will be more likely to induce semiconductor failures than a unidirectional wave. Also, it shows the significance of the timing of the transient application with respect to the power frequency cycle.

*Chowduri, P., "Transient-Voltage Characteristics of Silicon Power Rectifiers." *IEEE IA-9*, 5, September/October 1973, p. 582.

†F.D. Martzloff, internal report, unpublished.



Francois D. Martzloff (M, 1956) was born in France and received his undergraduate degree at the Ecole Spéciale De Mécanique et d'Electricité in 1951; he received the MSEE degree from Georgia Tech in 1952 and the MSIA degree from Union College in 1971.

Since 1956 he has been with the General Electric Company, where he first gained experience in the Transformer and Switchgear Divisions. Upon joining General Electric Corporate Research and Development in 1961, he became involved in power semiconductor circuits and overvoltage protection. He has

participated in the introduction and application of metal oxide varistors since 1971.

In IEEE Mr. Martzloff has been active on the Surge Protective Devices Committee and chairman of the Working Group on Surge Voltages in AC Power Circuits Rated 600 V or Less. He is also a member of the Ad Hoc Advisory Subcommittee of the USA Advisory Committee on IEC S/C 28A. He has been awarded 10 U.S. patents, primarily in the field of varistors and transient protection.

**VARISTOR
VERSUS
ENVIRONMENT:**

**WINNING
THE
REMATCH**

Varistor versus Environment: Winning the Rematch

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Significance:

This paper is listed under four categories of the Annotated Bibliography as it bears on the corresponding topics. The multiple listing reflects the sections in which this paper is cited as supporting material for IEEE Std C62.41.1 and C62.41.2. Therefore, it can be found in the following four parts of the Anthology:

Part 2 Development of standard – Reality checks

Provides an example of the need to recognize capacitor switching transients when characterizing the surge environment

Part 3 Recorded occurrences, surveys and staged tests

Provides an example of monitoring and staged tests motivated by field failure, leading to a better understanding of the environment in which SPDs were expected to perform.

Part 4 Propagation and coupling of surges

Provides an example of how far (3000 meters) the low-frequency transients generated by capacitor switching can propagate, unabated, in a path involving two step-down transformers.

Part 7 Mitigation techniques

Provides an example of improved mitigation design based on field experience

VARISTOR VERSUS ENVIRONMENT: WINNING THE REMATCH

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Abstract — An unusual case of difficult application of surge protective devices was solved by field measurements with retrofit of protective devices suitable for the particular environment. On-site measurements indicated that capacitor switching transients were causing excessive current surges in the varistors and fuses protecting the input to a thyristor motor drive. Knowledge of the environment gained by the measurements allowed understanding of the problem and specification of matching surge protective devices.

SUMMARY

During the initial startup of a solid-state motor drive in a chemical processing plant, difficulties arose with the varistor and its protective fuse at the input of the thyristor circuits. Frequent blowing of the fuse was observed, with occasional failure of the varistor. On-site measurements of the voltages and currents at the input to the drive indicated that switching transients associated with the operation of a remote substation capacitor bank and the relatively low clamping level of the varistor were producing current above the fuse and varistor ratings; hence the short lives of these two components. When the actual conditions at that site were determined by measurements, it became possible to specify surge protective devices capable of withstanding that environment. Immediate relief was secured by the installation of a larger varistor at the same point of the circuit; long-term protection was obtained by the addition of a gapless metal-oxide varistor arrester on the primary side of the step-down transformer feeding the drive. The situation has been changed from failures occurring every few days to no further problems in the 3 years since the larger varistor was installed.

INTRODUCTION

This paper presents a case history illustrating how surge protective devices that are successfully applied for the majority of cases can occasionally suffer failure when exposed to exceptionally severe surge environments. This paper also shows how little attenuation occurs, at the frequencies produced by switching surges, between the distribution level (23 kV) and the utilization level (460 V), even though a long line and two step-down transformers exist between the source of the transient and the point of measurement.

85 SM 365-2 A paper recommended and approved by the IEEE Surge Protective Devices Committee of the IEEE Power Engineering Society for presentation at the IEEE/PES 1985 Summer Meeting, Vancouver, B.C., Canada, July 14 - 19, 1985. Manuscript submitted February 1, 1985; made available for printing April 22, 1985.

The problem involved a 460 V power supply to a thyristor drive circuit in a chemical processing plant extending over several square miles. During the initial startup, difficulties arose with the varistor and its protective fuse at the input of the thyristor circuits. Frequent blowing of the fuse was observed, with occasional failure of the varistor. The plant substation, fed at 23 kV from the local utility, included a large capacitor bank with one-third of the bank switched on and off to provide power factor and system voltage regulation. These frequent switching operations were suspected of generating high-energy transients that might be the cause of the failure of the fuses and varistors, because literally thousands of similar drive systems have been installed in other locations without this difficulty. On-site measurements performed after repeated blowing of fuses and occasional failure of varistors connected at the input to the thyristor drive indicated that indeed the devices were not matched to their environment. From this point on, specifying larger sizes, sizes appropriate to the environment [1], solved the problem.

POWER SYSTEM AND SWITCHING TRANSIENTS

Figure 1 is a simplified one-line diagram of the significant elements of the power system causing the varistor failures. The incoming 115 kV power is stepped down to 23 kV. Three banks of 5400 kVAR capacitors are connected to the 23 kV bus. Typical operating conditions involve two banks connected at all times, with the third bank switched on or off automatically to provide voltage regulation. Power distribution throughout the site is done at the 23 kV level.

The various drive systems which experienced the difficulty are supplied at 460 V by a 2300/460 V transformer in their control house. A substation close to the control house supplies the 2300 V power from the 23 kV distribution system.

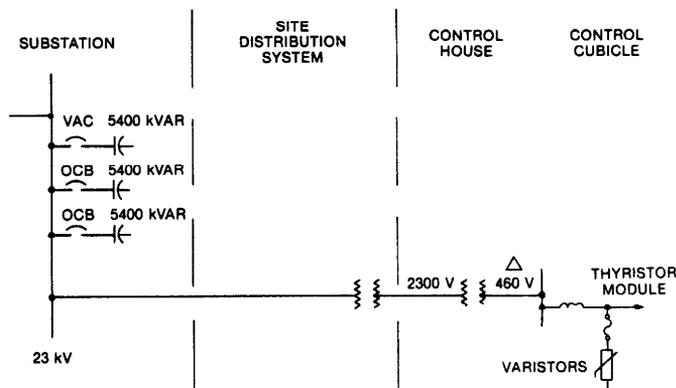


Figure 1. Simplified one-line diagram

Figure 2 is a simplified three-phase schematic of the power input. In the original circuit, the thyristor modules were protected by varistors at the power input of the 1250 hp drive, where the measurements were made. A $6 \mu\text{H}$ line inductance, L_1 , was inserted between the bus and the thyristor modules; 20 mm varistors rated 510 V were connected in a delta configuration, in series with a current-limiting fuse in each line. The varistor connection was about 80 cm long, introducing an estimated $1 \mu\text{H}$ inductance into each lead.

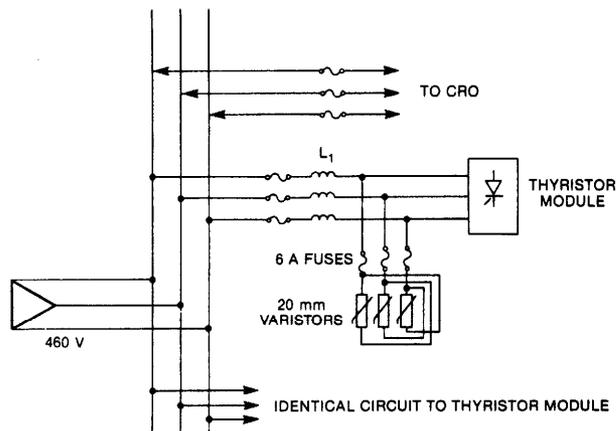


Figure 2. Simplified three-phase schematic

Instrumentation and Measurements

Oscilloscopes were used to measure voltage across one varistor and its connection and currents through all varistors. Voltage measurements were made phase-to-phase on the floating delta 460 V bus bars with Tektronix P6015 1000:1 probes, connected to a Tektronix 7633 storage oscilloscope in differential mode. Current measurements were made with a Tektronix CT5 20:1 current transformer coupled with a P6021 current probe and connected to a second Tektronix 7633 storage oscilloscope.

The trigger modes used during a two-hour monitoring period included positive or negative slopes for both slow ac and high-frequency modes. For the various modes, the level was adjusted to produce a trigger for a voltage exceeding the normal line voltage crest by about 20%, or a varistor current in excess of 2 A. No trigger occurred during the monitoring period. A low-frequency voltage recorder installed by plant personnel produced a recording characterized as representative of an unusually quiet day in the power system operations.

Manual off-on switching of the 5400 kVAR capacitor bank at the 23 kV utility substation was the next step in the measurement procedures because the switching of a capacitor bank is always a prime suspect for producing transients. Measurements were performed with one oscilloscope monitoring the line voltage upstream of the line inductors (Figure 2) and another oscilloscope monitoring the sum of the currents in the three varistors (Figure 3).

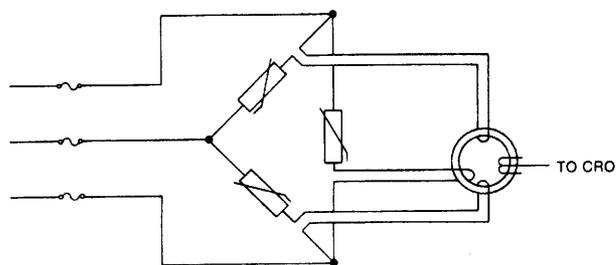


Figure 3. CT connection for recording all three varistor currents

Results

The oscillograms of Figure 4 show typical voltage recordings made during this sequence. The voltages are *not* open-circuit transient voltages. They are instances of the voltage appearing at the bus entrance point. This voltage is the sum of the varistor clamping voltage, the voltage drop in the varistor connections, and the voltage across two L_1 inductances.

A typical total event recorded on one of the phases during a capacitor bank closing is shown in Figure 4A. A low-frequency oscillation with a period of 3 ms (330 Hz) and initial peak-to-peak amplitude of 450 V decayed in about 10 ms. The high-frequency oscillations are resolved in the recording of Figure 4B (recorded during a similar switching sequence). This high frequency has an initial peak-to-peak amplitude of 2000 V, decaying in about 5 ms. The period is $180 \mu\text{s}$ (5.5 kHz). A similar, third event is shown in Figure 4C. For scaling the amplitudes, the steady-state voltage is shown in Figure 4D.

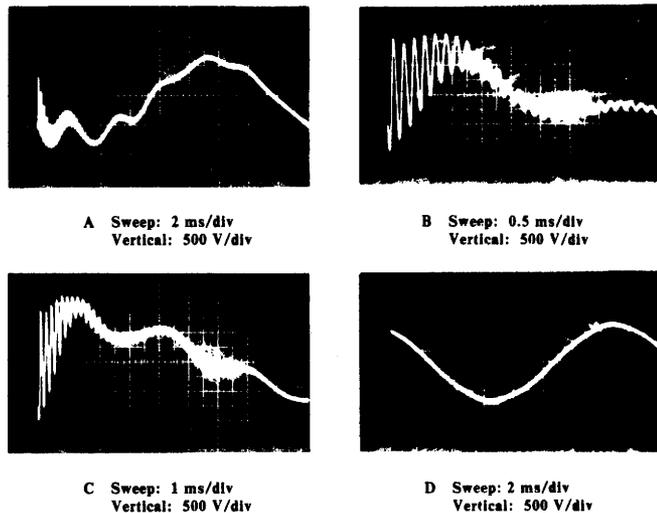


Figure 4. Capacitor switching transients and steady-state voltage

Observe that, depending on the time of closing with respect to the 60 Hz voltage, the 5.5 kHz oscillation varies in amplitude; furthermore, the modulation by the 330 Hz oscillation pushes crests of the 5.5 kHz oscillation above the 1000 V level some time after the beginning of the trace, at a time when the 5.5 kHz amplitude is already lower, producing a burst of pulses above the 1000 V level.

The significance of this finding will be discussed next, with reference to Figure 5, which shows recordings of transient currents in all of the three varistors. The 510 V varistor has a nominal voltage at 1 mA [2] in the range of 735 V to 970 V. For a varistor with a nominal voltage in the middle of this range, a current in the order of tens to hundreds of amperes will flow if a voltage of 1000 V is applied to the varistor. Figure 5A shows a train of current pulses in the range of 10 to 40 A. In the burst of Figure 5B, the recorded current pulses range from 5 A to 200 A. The current and voltage traces are not simultaneous events because each of the two oscilloscopes was triggered by its internal circuit. The nearly symmetrical appearance of this burst can be compared to the symmetry of the voltage peaks exceeding the 1000 V level in Figure 4, the one correlating with the other.

The oscillograms of Figures 4 and 5 were selected as most severe from a series of 20 capacitor switching sequences. Some sequences could not even produce a current or voltage trigger; four sequences produced bursts with the central peak exceeding 120 A, two of these reaching 200 A peaks.

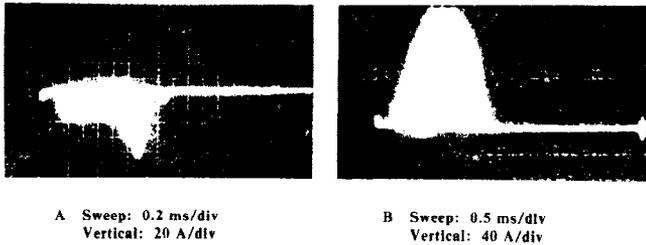


Figure 5. Current surge bursts during capacitor switching

These recordings establish the nature of the current surges that are conducted by the varistors, with an estimate of 10% reaching 200 A maximum crests and another 10% reaching 120 A crests, for all capacitor bank switching.

In Figures 4 and 5, we note that the characteristic appearance of the voltage and current usually observed during a switch restrike is absent [3], indicating a clean switching action of the vacuum interrupters used for switching the capacitor bank. Restrikes are most likely to occur during de-energizing. In all the off-on switching sequences of this test series, no significant transient was observed during de-energizing; all occurred during energizing.

The oscillograms of Figures 4 and 5 establish and explain the pattern of current pulses. The voltages of Figure 4 are not the open-circuit voltages impinging the drive input but, rather, the voltages resulting from the clamping action of the varistors. To better evaluate the magnitude of the switching transients, open-circuit voltages were recorded in a next sequence, with all fuses to the drive open, thus disconnecting both the varistors and all sensitive loads. Figure 6 shows two typical recordings of open-circuit voltages and two of voltages resulting from varistor clamping, recorded during a series of 10 switching sequences for each condition. Table 1 shows the recorded crests of the five highest voltages in each condition; the difference between the two groups, with due allowance for the imperfect statistical basis of the observations, indicates that the 510 V varistors reduced the peaks from a typical high of 1450 V to a typical high of 1100 V.

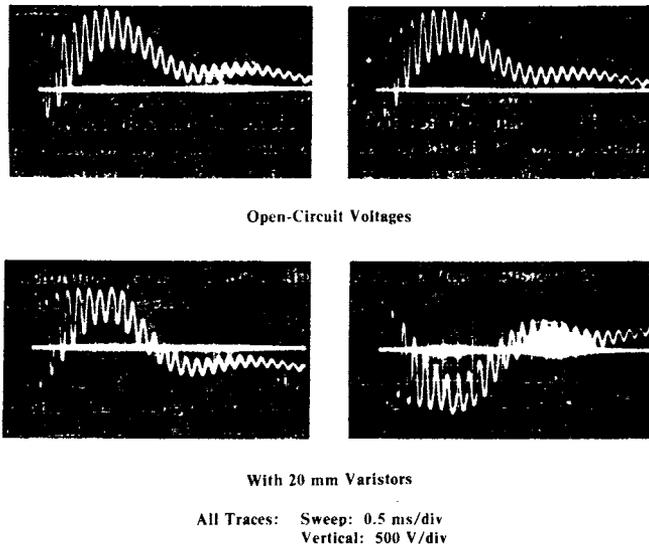


Figure 6. Capacitor switching transients

Table 1
FIVE HIGHEST TRANSIENTS
IN SEQUENCE OF 10 SWITCHINGS

Without Varistors	With Varistors
1450	1100
1400	1100
1300	1050
1300	1050
1300	1050

DISCUSSION

Nature of the Transients

The absence of any transient (over 120% of normal crest) during the 2-hour monitoring period was somewhat surprising, in the context of earlier reports of high counts recorded with Dranetz disturbance analyzers. Frequent checks of threshold levels and variations of the possible trigger modes were made, maximizing the chance of catching an overvoltage, but indeed none occurred. This unusual quiet was also reflected in the chart recording made by the plant personnel, so that the absence of random transients for that period can be accepted at face value.

Therefore, conclusive evidence was obtained that substantial current pulses were absorbed by the varistors during capacitor switching. The magnitude and duration of these pulses were excessive for the capability of a 20 mm disc; many similar drives installed elsewhere do not experience the failures encountered at that particular location.

Another significant finding from these measurements is the fact that the switching transients, generated at the 23 kV level, propagate down to the point of utilization at the 460 V level. Numerical discussion of this finding is given later in this paper.

Effect of Transients on Varistors

Published varistor specifications include the "pulse ratings," a family of curves that define, for each varistor type, the number of isolated pulses that a varistor can absorb until its "rating" is reached [4]. The curves show lines relating amplitude, duration, and total number of pulses. Figure 7 shows this family of curves for the original 20 mm varistor.

Figure 8 shows the same curves for a proposed 32 mm varistor. It should be noted that the pulse rating does not mean catastrophic failure of the varistor at the end of this rating, but only a 10% change in the varistor nominal voltage. Although some change is indicated, the varistor is quite capable of staying on line voltage and of clamping surges.

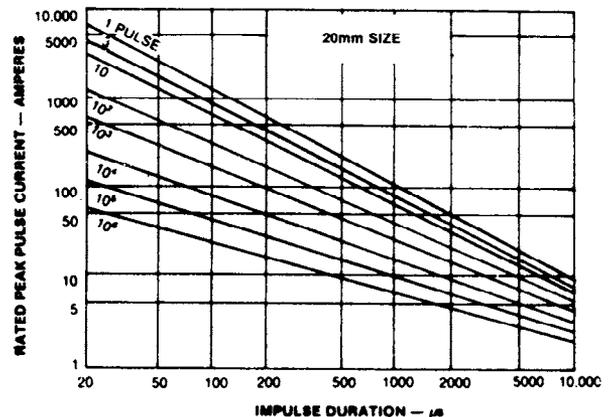


Figure 7. Pulse ratings of 20 mm varistor [4]

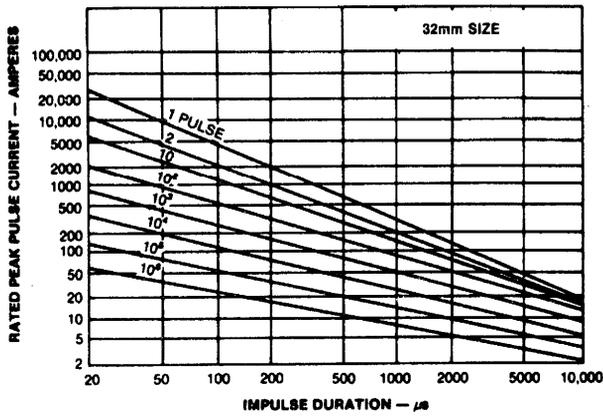
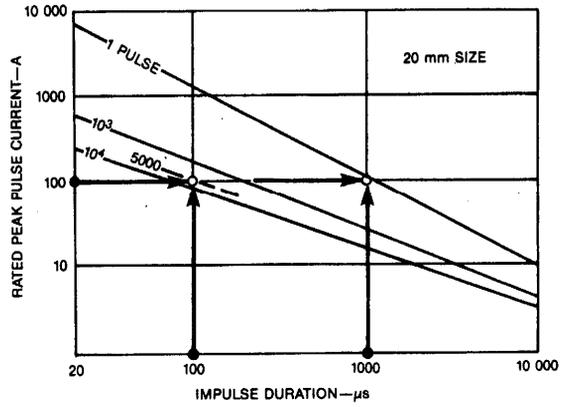


Figure 8. Pulse ratings of 32 mm varistor [5]

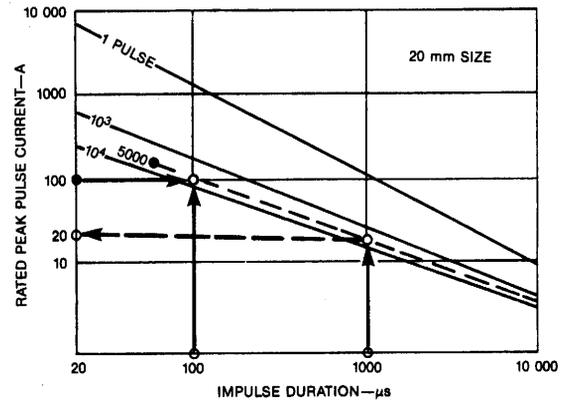
A careful examination of the pulse rating curves will show that the duration of the pulses has a strong influence on the number of permissible pulses. Furthermore, the relationship between the increased duration of the pulses and the decreased number of permissible pulses is not proportional. For instance, consider pulses of 100 A peak and 100 μ s duration (Figure 9A): the curves show 5000 pulses allowed. Now increase the duration of the pulses to 1000 μ s (a ten-fold increase), while keeping the amplitude at 100 A: the curves show the permissible number as one pulse only. Thus, the ten-fold increase in duration does not result in a ten-fold decrease in the number of permissible pulses; the reduction in that number is much greater than the inverse of the increase in duration. Conversely, taking a pulse duration of 1000 μ s, and seeking the amplitude allowable for the same 5000 pulses, Figure 9B shows that the current is 20 A, which is five times less than the original 100 A, not ten times less. Therefore, it would be incorrect to treat the multiple pulses of Figure 5 as five separate short pulses; rather, one equivalent long pulse has to be defined.

The five-pulse burst of Figure 5 has been redrawn in Figure 10 in order to plot an equivalent continuous pulse of approximately equal duration, with a crest such that the $i \cdot t$ integral of the burst and the $i \cdot t$ of the equivalent pulse are approximately the same. The use of $i \cdot t$ rather than the $i^2 \cdot t$ integral typically used for fuses or other linear loads is justified by the fact that heat deposited in the varistors is the significant parameter because the nominal voltage change process is temperature related; this heat is the product of the variable i and the nearly constant voltage across the nonlinear varistor during the burst.

The equivalent pulse of Figure 10 can then be used to evaluate, from the pulse ratings of Figure 7, the number of high-amplitude switching transients that will consume 100% of the varistor pulse rating. Inspection of Figure 6 shows that for a 800 μ s duration and 100 A amplitude, the pulse rating of the 20 mm varistor (6 kA rating at 8/20 μ s) is reached with two such events. With a probability of about 10% that this highest switching transient would occur during random timing of the switching (the effect decreases rapidly for transients other than the highest) and with 2 to 4 switching operations each day, the pulse rating of the varistors could be reached with 20 operations, failure perhaps starting at 40 to 50 operations, or after about 10 days of exposure to that power system environment. This estimate is unavoidably imprecise because the pulse rating curves represent a conservative minimum; actual failures will occur only for amplitudes or numbers of pulses exceeding the rating by a large but imprecise margin to allow for manufacturing variations. However, the order of magnitude of this estimated time to failure is in accord with the observations made at that installation.



A. Same current, increasing duration



B. Same number of pulses, increasing duration

Figure 9. Reading pulse ratings curves

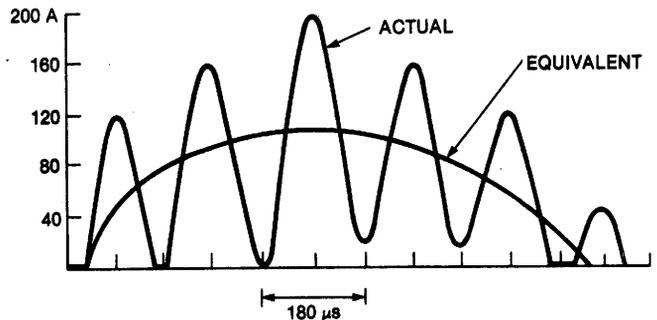


Figure 10. Single-pulse equivalent pulse for multiple pulses

Note that two types of events occur. One is the premature blowing of the fuse, which is not caused by a varistor failure but by the $i^2 \cdot t$ capacity of the fuse being exceeded by the environment [1]. The other is the fuse blowing caused by the varistor end-of-life ultimate failure.

Alternate Varistor Selection

An obvious remedy would be to use a varistor with greater energy-handling capability. The 32 mm size offers such a possibility. Inspection of Figures 7 and 8 shows that the equivalent pulse of Figure 10 (800 μ s and 100 A) corresponds to a permissible occurrence of 100 pulses for the 32 mm varistor, in contrast to the two for the 20 mm varistor. The improvement in the number of pulses is 50 times more pulses until *pulse rating* is reached. The improvement in the number of pulses until *varistor failure* occurs, however, is not necessarily 50 times more pulses. Because of the imprecision mentioned previously in the margin between end of pulse rating and ultimate failure, that margin is not necessarily the same for the two sizes, 20 mm and 32 mm, but it is reasonable to expect the same order of magnitude improvement in the ultimate failure as in the pulse rating. This expectation of a 50 times improvement would change the time between failures from the few days observed with the 20 mm size to perhaps one year with the 32 mm size, providing immediate relief and time to make further changes for the long term. Therefore, the change to a 32 mm size, connected at the same point of the circuit, was immediately implemented for that particular environment.

Further gains could be obtained in the length of time between varistor failures by increasing the clamping voltage of the varistors. This increase would result in lower current pulses for the same open-circuit transient voltage. A 510 V rating had been selected by the designer of the drive as the result of a trade-off between varistor clamping voltage and the withstand voltage of the thyristors protected by the varistors. If thyristors with higher voltage withstand were used, the solution would be easy.

Of course, the standard varistor product line has a certain tolerance band, reflecting normal production lot variations. In principle, a selection could be requested from the manufacturer that varistors with a narrower band be supplied for this application. The maximum clamping voltage allowed by the drive specifications would be retained, but those varistors in the lower half of the distribution, which draw larger current pulses for a given open-circuit transient voltage, would have been removed from the population of varistors. For instance, the range of nominal voltages for a 575 V, 32 mm varistor (the next higher voltage offered) is 805 to 1005 V for 1 mA dc, while the maximum nominal voltage of the same diameter but rated 510 V is 910 V for 1 mA dc. Thus, for a normal distribution of nominal voltages of the 575 V varistor, 50% of the devices could theoretically be used without exceeding the upper limit of the 510 V varistor that is consistent with the drive specifications. To achieve this end, it would be necessary for the supplier or user to make a careful determination of the nominal voltage on a population of 575 V varistors in order to retain only the lower half of the distribution (Figure 11).

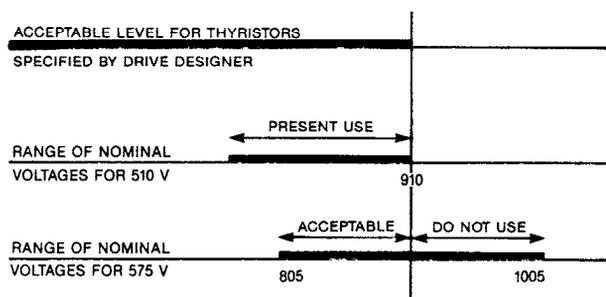


Figure 11. Tolerances bands of 510 V and 575 V varistors

Other Remedies

In addition to the proposed upgrading of protection at the 460 V level, three other remedies could be considered: installation of surge arresters at the 2300 V level, installation of surge arresters at the 23 kV level, or a change in the circuits involved in the capacitor switching, designed to reduce the severity of the transients at their origin.

In general, the protection available from surge arresters tends to improve when the arresters are installed at higher circuit voltages. Thus, it is quite possible that arresters installed at the 2300 V primary of the 2300/460 transformer could provide a more effective clamping (and at the same time relieve some of the energy stress) than the varistors at the 460 V level. (It is of course implied that these would be the zinc-oxide type, gapless arresters.) The full benefit of these arresters depends on the configuration of the 2300 V system and its grounding (solidly grounded neutral in a wye system, resistance-grounded wye, or floating delta) when the arresters are connected in the conventional line-to-ground mode. In a second phase of the retrofit described here, 2300 V arresters were installed at the transformer primary. A discussion of their expected performance, validated by the success of the retrofit, is given later on.

Likewise, arresters on the 23 kV side could be installed at the 23 kV substation to mitigate the capacitor switching transients at their origin, or at the primary of the 23 kV/2300 V substation near the control house, where they would also serve as lightning protection for the overhead 23 kV incoming power line. These arresters, again, must be of the gapless type to obtain the most effective protection.

The final remedy in the list of alternatives, but perhaps the first in effectiveness when the opportunity exists, would be to attempt reducing the severity of the capacitor switching transients at their origin. Series inductors or damping resistors may be considered, the effectiveness of which would be predictable if a simulation of the power system behavior were performed by computer modeling. While that remedy could not be applied to this particular location, it is a remedy that should be considered for a similar case of exceptionally severe environment.

EXPECTED PERFORMANCE OF THE 2300 V ARRESTERS

The measurements made first with open-circuit, then with the 20 mm, 510 V varistors on the 460 V side have shown a reduction of maximum voltage from 1450 V to 1100 V (Table 1) when a current of approximately 200 A is flowing in the line and varistors (Figure 5).

We can assume that the voltage drop in the line from the substation and two step-down transformers is mostly inductive at 5.5 kHz, and that the voltage in the varistors can be treated as the voltage across a resistor at the time of the crest of the current wave. The diagram of Figure 12 shows the relationship between the three voltages V_{OC} , V_L , and V_V , respectively, the open-circuit voltage generated by the capacitor switching action, the voltage drop in the line and two transformers, and the varistor voltage at the current peak. Treating this highly nonlinear circuit as a linear circuit is an approximation that will provide at each point of the full range of voltage and current conditions a valid order of magnitude for the purposes of this discussion. Numerical methods are available for rigorous treatment at any instant over the full range of conditions [6]. With this simplifying assumption, we can determine the order of magnitude of the 5.5 kHz current that would flow in an arrester installed at the primary terminals of the 2300 V/460 V transformer as follows.

1. From the actual measured voltages shown in Figure 12, we derive the voltage drop, V_L , in the 23 kV line and two step-down transformers:

$$V_L = (1450^2 - 1100^2)^{1/2}$$

$$= 940$$

Thus, at 5.5 kHz, the impedance between the source and the varistor is

$$Z_L = \frac{V_L}{I}$$

$$= \frac{940 \text{ V}}{200 \text{ A}}$$

$$= 4.7 \Omega$$

Note that this 4.7Ω impedance means that the 5.5 kHz switching transient, generated at a distance of about 3000 m (2 miles) from the point of measurement, and at the 23 kV level, can travel a long distance and pass through two step-down transformers with less attenuation than might be expected from the unsound but popular view that "surges cannot travel that far without substantial attenuation."

2. We now arbitrarily assign equal values to the three elements of this impedance, Z_L : (1) the 23 kV line impedance; (2) the 23 kV/2300 V transformer; and (3) the 2300/460 V transformer. The impedance between the source and the primary of the 2300/460 V transformer is then two-thirds of the total impedance, Z_L , or about 3Ω for the 460 V side of the transformer.
3. On the 2300 V side, the impedance of 3Ω , calculated above, becomes $3 \Omega \times (2300/460)^2 = 75 \Omega$ and the open-circuit voltage of 1450 V which was measured on the 460 V side becomes $1450 \text{ V} \times (2300/460) = 7250 \text{ V}$.
4. Knowing the open-circuit voltage and the impedance between the source and the 2300 V arrester, we can compute the current in the arrester by iteration if we assume some current value and read the corresponding clamping voltage on the I-V characteristic of the arrester:
- Assume a current crest of 50 A, producing a drop of $50 \times 75 = 3750 \text{ V}$ in the line and 23,000/2300 V transformer. Adding this voltage to the varistor voltage, corresponding to 50 A, which is read as about 5700 V on the arrester characteristic curve for minimum discharge voltage (Figure 13), we have $(3750^2 + 5700^2)^{1/2} = 6780 \text{ V}$, or somewhat below the expected 7250 V open-circuit voltage, which is to equal the quadratic sum of the two voltages V_L and V_V .
 - Assume, for a new iteration, a crest of 60 A, producing a drop of $60 \times 75 = 4500 \text{ V}$, while the varistor voltage remains essentially the same, i.e., 5700 V. The quadratic addition becomes $(4500^2 + 5700^2)^{1/2} = 7210 \text{ V}$, or a value close to the goal of 7250.
5. Thus, we can expect that the 2300 V arrester will experience current pulses occurring in bursts not exceeding 60 to 70 A, with durations similar to those found on the 460 V varistors, i.e., 5 to 7 pulses per train, or a total duration in the order of 1 ms. Information on arrester duty available from the manufacturer indicates that, for a pulse train of that duration and a crest of less than 100 A, no limitation of the number of pulses need be imposed on the arrester as long as enough time is allowed between pulses to permit cooling of the arrester.

Furthermore, the 5700 V clamping level predicted for the 2300 V surge arresters at 60 A would be reflected as a crest of $5700 \text{ V} \times 460/2300 = 1140 \text{ V}$ on the 460 V side. The 510 V, 32 mm varistors, connected in series with the impedance of the 2300/460 V transformer, would then be exposed to this maximum

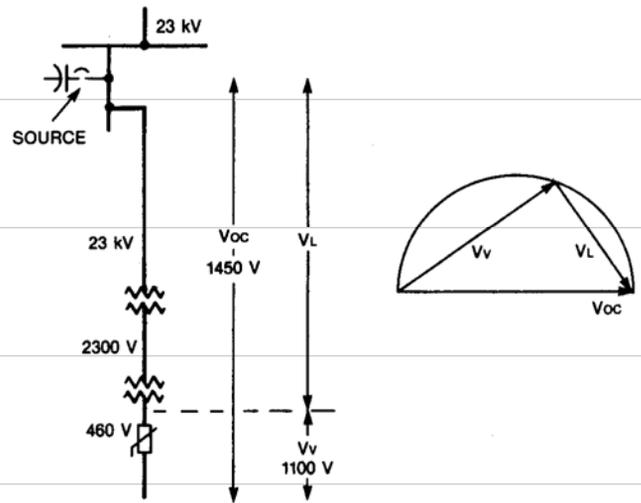


Figure 12. Open-circuit voltage and voltage drops in the system

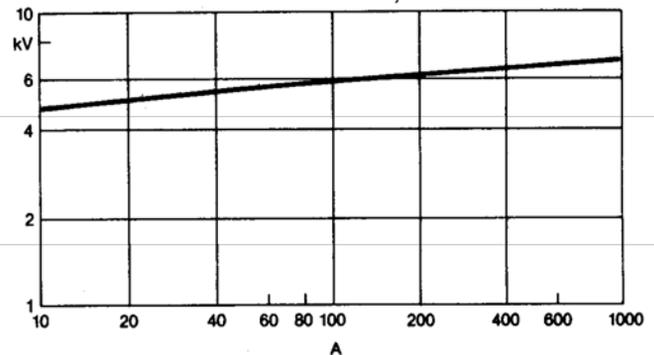


Figure 13. Minimum clamping voltage characteristic for 2300 V arrester

open-circuit voltage of 1140 V, a value much lower than the 1450 V open-circuit voltage that was applied to them in the absence of the 2300 V arresters. For that applied voltage, the current drawn by the varistor would be in the range of 10 to 20 A, values much lower than the 200 A measured without the 2300 V arrester. Computing the equivalent pulse, as was done in Figure 10 for the 200 A crests, would yield an equivalent crest of about 10 A, for which the pulse rating curves of the 32 mm series show more than 100,000 pulses before its rating is reached.

To place these large differences of performance and withstand capability into perspective, Table 2 shows the relative sizes and volumes of varistor material applied to the taming of the capacitor switching transient. In other words, the environment has now been matched by the capability of the varistors.

Table 2
VARISTOR AND ARRESTER DIMENSIONS

Type	Diameter	Thickness	Volume
20 mm	1.8 cm	0.35 cm	0.9 cm ³
32 mm	3.0 cm	0.35 cm	2.5 cm ³
2300 V Arr.	6.1 cm	2.4 cm	70 cm ³

CONCLUSIONS

Voltage and current measurements made on the 460 V input to a thyristor motor drive, during staged capacitor switching operations, showed current surges in the varistors originally used in the system that could consume the pulse rating life of these varistors in a few days of typical operation. Short- and long-term remedies were achieved.

For the short term, the change to a larger varistor connected on the 460 V side of the system was readily implemented to maintain the originally specified protective level, while the fuse-blowing nuisances were eliminated by use of a larger fuse. Available devices for this 460 V circuit may still have a relatively short life (a few hundred days) in the prevailing environment of the site, but they offered immediate relief and therefore allowed successful startup of the system.

For the long term, further protection was obtained by the installation of conventional station-class surge arresters, of the zinc-oxide, gapless type, at the 2300 V level. The system has now operated for 3 years without problems.

This case history also illustrates the low attenuation of the switching transient between the distant source at 23 kV (about 3000 m, or 2 miles) and the point of utilization at 460 V.

ACKNOWLEDGMENTS

J.S. Kresge and B.I. Wolff provided information and guidance on the surge arrester and the varistor characteristics; C.L. Fisher contributed advice in clarifying and unifying the presentation of the concepts. Their contributions are gratefully acknowledged.

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François D. Martzloff (M'56, F'83) was born in France, and received his undergraduate degree at the Ecole Spéciale de Mécanique et d'Electricité in 1951; he received the MS in Electrical Engineering degree from Georgia Tech in 1952 and the MS in Industrial Administration degree from Union College in 1971.

Since 1956 he has been with the General Electric Company, where he gained experience in the Transformer and Switchgear Division. Upon joining General Electric Corporate Research and Development in 1961, he became involved in power semiconductor circuits and overvoltage protection. He has participated in the introduction and application of metal oxide varistors since 1971.

In IEEE, Mr. Martzloff is active on the Surge Protective Devices Committee. He is chairman of the Working Group on Surge Characterization in Low-Voltage Circuits. He is also a member of the Ad Hoc Advisory Subcommittee of the USA Advisory Committee on IEC S/C 28A, ANSI C.62 Subcommittee on Low-Voltage Surge Protective Devices, and Chairman of the NEMA Low-Voltage Arresters Technical Committee. He has been awarded 13 U.S. patents, primarily in the field of varistors and transient protection.

Discussion

J. L. Koepfinger (Duquesne Light Company, Pittsburgh, PA): The author has addressed one of many mechanisms for producing repetitive over-voltages on low-voltage circuits. In this particular instance, it was possible to obtain controlled conditions so that a measurement could be made of the voltage and currents resulting from the capacitor switching. It would be useful if there was an analytical method presented that correlated the generation of the 5.5-kHz pulses with those measured. Did the author attempt to make such a correlation?

This paper points out the need to know the characteristic of the surge so that proper sizing of the protection can be achieved. Therefore it would be desirable to be able to have some analytical tool to permit calculation of the frequency of the surge due to remote capacitor switching.

Manuscript received July 24, 1985.

Francois D. Martzloff: The paper reported a case history from which useful information may be derived on retrofitting corrections of similar problems or, better, on avoiding the problem by foresight. The situations confronting the author was the need for immediate corrective action rather than complete investigation and mutual validation of analytical methods and field measurements.

The literature is fairly rich in both theoretical and practical papers on the problems associated with capacitor switching, both for energizing and for de-energizing, the latter involving the risk of restrikes. Because of this availability and the limited space available in the TRANSACTIONS on one hand, and because of the limitations in scope of the field retrofit mission on the other hand, no attempt was made to correlate the measurements with the power system parameters (which were not readily available to the author). In response to Mr. Koepfinger's suggestion, abstracts are cited below to provide references to both analytical tools and practical results published by other workers.

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Protection requirements and surge arrester duties are analyzed for both digital and transient network analyzer (TNA) simulations. Simple analytical expressions are developed for evaluating arrester duty as a function of capacitor bank size. Guidelines and limitations for applying arresters at grounded- and ungrounded- wye capacitor banks are developed based on overvoltage characteristics and arrester capabilities.

- [2] J. H. Brunke and G. G. Schoeckelt "Synchronous Energization of Shunt Capacitors at 230 kV," *IEEE PES (Power Eng. Soc.) Winter Meeting*, New York, NY; Jan. 20-Feb. 3, 1978; Publ. by IEEE 1977, Paper A78-148-9, p. 4.

This paper reports on the application of synchronous switching to reduce inrush transients when switching a 230-kV shunt capacitor bank. Computer studies determined the required switch performance.

- [3] J. D. Cuffman, John Linders, M. A. Zucker, and S. Willima, "Power Factor Correction Capacitors and Their Side Effects," *IEEE Conf. Rec.*, 28th Ann. Conf. Electr. Ens. Probl. Rubber Plast. Ind., Akron, OH, April 5-6, 1976, pp. 37-49.

The major reason for applying capacitors to an electrical distribution system is to correct poor power factor. In the greater majority of installations it is a routine procedure. In some instances capacitors may cause problems with other in-plant equipment and in other instances they may suffer undesirable side effects that originate in other equipment. Among these problems are switching surges, voltage unbalance due to fuses blowing, and harmonics generated by SCR-controlled equipment.

- [4] H. J. Yelland, and C. P. Yellna, "Vacuum Contactors: Latest Development in Their Design and Application," *Certif. Ens. V 54*, No. 1, Jan. 1981, pp. 824-841.

The paper includes the following topics: Design of vacuum bottles (glass and ceramic); design of the complete vacuum contactor and their panels; panels with on-load isolation, and panels with off-

load isolation; vacuum contactor applications (motor control), capacitor switching, arc and other (furnace-switching, transformer switching, mine-winder-reversers). Surge generation by vacuum contactors is considered under the following heads: basic energization and de-energization transients; transients generated when switching inductive loads; assessment of a vacuum contactor from a surge generation point of view; types of surge suppression devices; switching of capacitive loads. An extensive discussion of the paper is appended.

- [5] Jack R. Harbaugh and John E. Harder, "Important Considerations for Capacitor Applications in the Petroleum and Chemical Process Industries," *IEEE Pet. Chem. Ind. Conf.*, 27th Ann. Rec. of Conf. Pap., Houston, TX, Sept. 8-10, 1980; *IEEE*, #80CH1549-5 IA), Piscataway, NJ, pp. 157-167.

The location of capacitors may have a significant effect of the (I^2R losses within the plant transformers and conductors, which is wasted energy. Transients generated by capacitor switching may require attention in the selection of arresters, system insulation, or other equipment. The presence of capacitors may require some special attention to large motors during system reclosing or load transfer. This study addresses each of these considerations, providing some guidelines for effective, reliable capacitor application. In addition, a checklist is provided for general industrial capacitor applications.

- [6] J. F. Burser, R. J. Santoro, J. W. Stolle, R. E. Owen and C. R. Clinkenbeard, "Comparative Evaluation of Field Test Data and Computer Results on Capacitor Switching Transients," *Meeting Minutes PA Electr. Assoc., Eng. Sect., Trans. and Distrib. Comm. West Middlesec, PA, May 15-16, 1979; PA Electr. Assoc., Eng. Sect., Harrisburg, PA 1979.*

An arrester failure case was analyzed occurring at the time of substation capacitor bank switching with the use of a transient network analyzer (TNA) and digital computer techniques. The case involved a 12.5-kV ground-wye distribution system. Results of the TNA study, which were validated by field tests, showed the effects of system configuration magnitude of transient over voltages.

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Back-to-back switching of grounded wye shunt capacitors cause high frequency, high-magnitude current flow in overhead bus and ground mat conductors. Measurements of induced voltages on control cables and receptacles, transverse voltages on fuse blown PT secondaries and personnel intercept voltages are reported. BPA methods of confining transients to capacitor areas and shielding techniques are reviewed.

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A simplified voltage-reactance equivalent of the one-machine infinite bus system was used. The material presented in this paper shows that this simplified model does not predict the performance of a one-machine infinite bus system with the accuracy necessary to determine the capacitor switching times so as to achieve the control objectives. However, it is shown that computation accuracy may be improved by including system losses. It appears that in order to apply optimal control techniques it will be necessary to develop more accurate models of the power system components. Also, faster than real time iterative, on-line computation techniques as simulated in this paper should be implemented and used to calculate the switching times. Until these obstacles are overcome, optimal control theory will have little impact upon the power industry.

- [9] D. O. Wiitanen, J. D. Morgan, and G. L. Gaibrois, "Station Capacitor Switching Transients, Analytical and Experimental Results," *IEEE Trans. Power App. Syst.* PAS-90, No. 4, July-Aug. 1971, pp. 1639-1645.

Station capacitor bank energization transients predicted by a circuit model are compared to field-test results. Selection of a suitable model is discussed. A computer solution of the model is presented.

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**REAL,
REALISTIC
RING WAVES
FOR
SURGE
TESTING**

Real, Realistic Ring Waves for Surge Testing

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Significance:

Part 2 Development of standards - Reality checks

Part 5 Monitoring instruments, laboratory measurements, test methods

Standards for surge testing have a long tradition of using unidirectional waves ("impulses"), in particular a 1.2/50 voltage impulse and an 8/20 current impulse. Many surveys of surge activity in low-voltage AC power circuits have shown that a large number of recordings actually show oscillatory surges rather than unidirectional surges.

This paper provides examples of such waveforms, independently recorded by two organizations, one in the US and one in Italy. These examples draw on field measurements as well as laboratory experiments and are offered to make the case that Ring Waves should be included in the regimen of electromagnetic compatibility tests.

REAL, REALISTIC RING WAVES FOR SURGE TESTING

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ABSTRACT – Five independent investigations on the coupling of surges into low-voltage circuits (data or power lines), and of their effects, show that a damped oscillatory transient is a real, realistic stress for equipment connected to these lines.

INTRODUCTION

Traditional surge testing performed on electromechanical equipment has been based on the unidirectional 1.2/50 μ s impulse deemed to represent the threat of lightning in power transmission networks. The purpose of these tests was to demonstrate the ability of high-impedance insulation to withstand a voltage stress. As a complement to these traditional tests, a current waveform was defined to demonstrate the ability of low-impedance components, such as surge arresters, to carry the currents associated with simulated lightning discharges. Application of systematic tests based on these two waveforms, as part of the Basic Insulation Level concept, was a turning point in ensuring greater reliability of power systems.

These tests, however, were primarily aimed at demonstrating the ability of equipment to survive in the presence of the lightning phenomenon or transients in the low-voltage power cables. The reliability of systems in the presence of other electromagnetic disturbances requires considering other tests, dealing not only with equipment withstand capability, but also with its immunity.

With the development and increasing deployment of improved instrumentation, it became apparent that typical waveforms of surges in low-voltage circuits are not only the traditional unidirectional wave, but also a decaying oscillatory wave. The results of measurements performed over the years in widely different environments, however, [1], [2] have demonstrated the prevailing pattern of oscillatory transients.

SPECIFIC INVESTIGATIONS

This paper briefly cites the results of five investigations performed by the authors, which point out the need to consider damped oscillations as a necessary complement to the traditional unidirectional waveforms. Included are two unpublished investigations of coupling from a high-voltage line into low-voltage signal and control lines that were performed at facilities of the Italian Electricity Board, ENEL, in 1969 and 1975, as reported in this paper by G. Pellegrini. One investigation of surge coupling between a grounding conductor and other low-voltage conductors was performed in 1978 in support of IEEE Std C62.41 [3], [4]. Another investigation of the propagation surges was performed in 1987 by NIST at an industrial building in California [5]. Preliminary tests have been conducted in 1990 at the NIST facilities on semiconductor failure modes, illustrating the implications of oscillatory stress on semiconductor failure modes. These last three investigations are reported in this paper by F.D. Martzloff. From these five investigations, the conclusion is reached that oscillatory surges – Ring Waves – need to be included in a comprehensive test program for electromagnetic compatibility [6]

1. Surge voltages induced in low-voltage control and signal cables located near transmission lines impacted by lightning

To establish realistic surge immunity specification for equipment installed in high-voltage (HV) substations, it was imperative to identify the parameters of transient voltages induced in associated signal and control lines. To that end, investigations were carried out in 1969 at the ENEL "Verderio" HV Substation [7], [8].

Primary phenomena (lightning, switching, and faults) were simulated on the HV line. The resultant surges induced in control and signal cables running parallel to the line were identified and measured.

1.1 Simulation of primary phenomena – The phenomena considered in the investigation are lightning surges and switching surges (energizing and initial transient of ground faults). Rise time and duration were the two most significant parameters. The 1.2 μ s rise time of the standard 1.2/50 μ s impulse was selected as representative of the phenomena considered. The duration of the standard 1.2/50 μ s impulse is representative of lightning, but too short for switching surges.

Therefore, simulation of the primary phenomena was obtained by a 1/500 μ s Marx generator (600 kV, 18 kJ, 75 ohms internal impedance, output capacitance 3 nF), by applying this unidirectional pulse to a 31-km long line (line-to-ground, single phase). The resultant voltage pulse applied to the line, unloaded and not terminated on its characteristic impedance, was 106 kV peak and the line current 264 A peak.

The waveform of the generator open-circuit voltage is shown in Figure 1 and the resulting voltage applied to the line in Figure 2 (front of wave and complete waveform). The oscillograms show how the unidirectional impulse produces a wave characterized by a unidirectional component plus oscillations caused by the impedance mismatching at the end of the line and also along it, the latter associated with line towers having different heights.

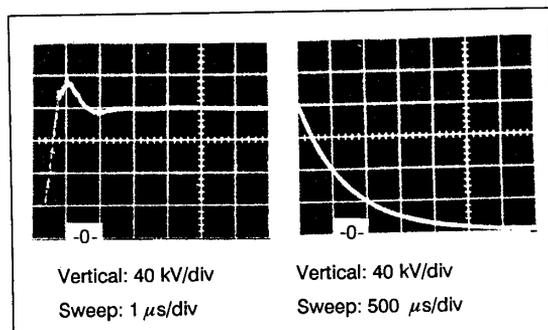


Figure 1. Open-circuit Voltage of Surge Generator Simulating Lightning on the Transmission Line

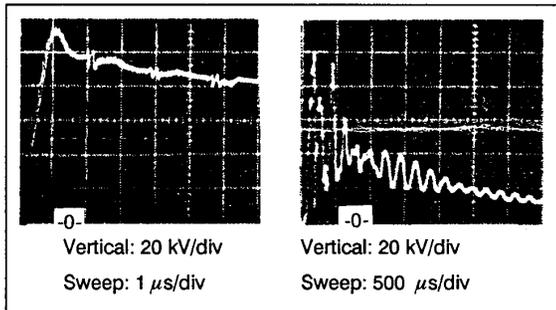


Figure 2. Surge Voltage Applied to the High-Voltage Line

1.2 Measurements on cables – Twelve types of cables were included in the test program. In the present summary, results are cited for typical unshielded and shielded cables, including coaxial and triaxial cables. For each cable, several measurements were made, including common and differential modes, with various combinations of earth connection for the shields and terminating impedances. Space limitation in the present paper prevent presentation of detailed test configurations and results; these can be supplied to interested parties by G. Pellegrini. Figure 3 illustrates one of the types of connections and combinations of grounding for one example of cable. The major point of this paper is to call attention to the occurrence of oscillatory waveforms in the cables, rather than detailed numerical values. For each cable, the characteristics of the voltages are summarized below, and selected oscillograms (Figures 4-7) illustrate the waveforms.

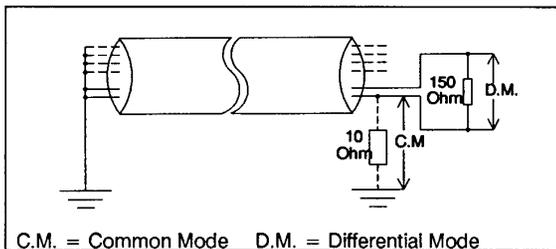


Figure 3. Typical Connections for Cable Measurements

Control cable, unshielded (Figure 4)

Common mode voltage: 200 kHz damped oscillation.
 Differential mode voltage: 250 kHz damped oscillation.

Telephone cable, 20 pairs, aluminum tape shield (Figure 5)

Shield-to-earth voltage: 200 kHz damped oscillation.
 Common mode voltage of the pairs (shield earthed at both ends): unidirectional component with a few microseconds duration and superimposed 400 kHz oscillation.
 Differential mode voltage: not shown, but negligible value (less than 1 V).

Coaxial cable, RG 58/U (Figure 6)

Shield-to-earth voltage: 300 kHz damped oscillation.
 Conductor-to-shield voltage (shield earthed at both ends): unidirectional component with about 40 μs duration

Triaxial cable, RG 58A/U (Figure 7)

The outer shield-to-earth voltage presents the same general waveform as the one observed for the coaxial cable. The inner shield-to-earth voltage, with the outer shield earthed at the ends is similar, but the unidirectional component has 400 kHz oscillations superimposed. The same situation occurs for the conductor-to-earth voltage, with an amplitude of about 15% of the first case, due to the higher shielding efficiency.

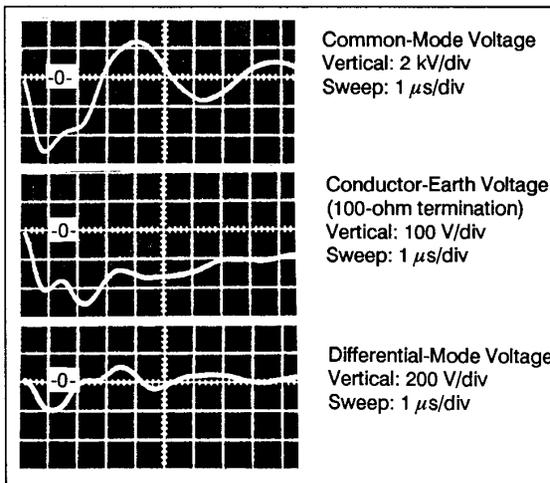


Figure 4. Transients Induced in Unshielded Control Cable

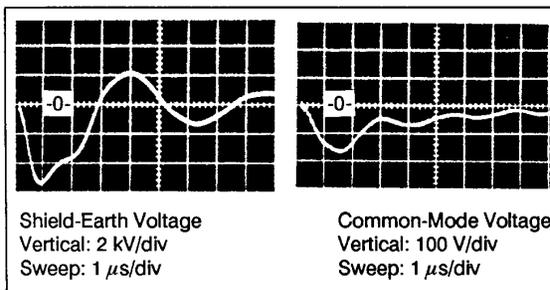


Figure 5. Transients Induced in Shielded Telephone Pair

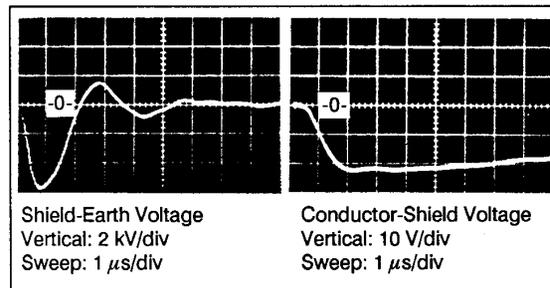


Figure 6. Transients Induced in Coaxial Cable

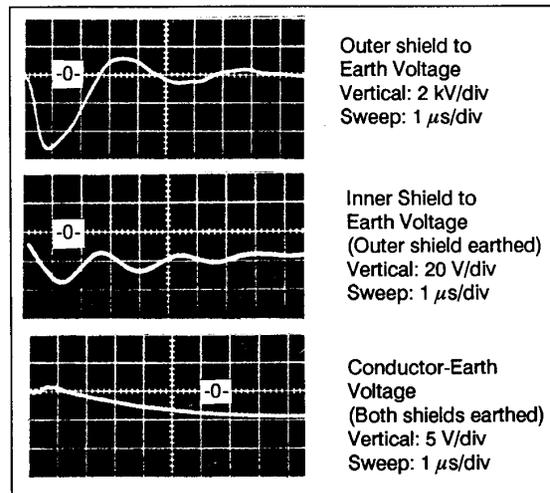


Figure 7. Transients Induced in Triaxial Cable

1.3 Discussion of results – The shape of a unidirectional pulse impressed onto a line though a generator is subjected to modifications due to the practical impossibility of terminating the line on its characteristic impedance. The actual phenomenon occurring on the line and impacting the secondary low-voltage cabling is quite different from the theoretical double exponential pulse. This interaction between the surge generator and its load occurs whenever a similar pulse is applied to other networks or structures.

Whenever the predominant coupling is inductive, the surges in the victim cables have a damped oscillatory waveform at a frequency that may range from 100 to 300 kHz, with a damping dependent on the type of cable shield and the propagation of the induced voltage. When there is a common impedance coupling of the cables with the primary phenomenon, as in the case of the shield earthed at the ends, with consequent transient current flowing in it, the induced voltage shows a unidirectional component with superimposed damped oscillations.

2. Surges induced in control and signal cables near 6 kV and 380 V cables

This investigation, complementing the Verderio measurements, was carried out in 1975 at the ENEL “Turbigo” Power Plant. The purpose was to identify the surges induced in control and low-level (mA, mV) measurement cables installed along power cables in power plants, as well as in industrial plants [8], [9].

The causes of disturbances considered were the switching of power circuits at 6 kV and 380 V, occurring under worst case conditions of switching at the crest of the power-frequency voltage.

2.1 Primary Phenomena – The power circuits used for the investigation were a 6 kV cable feeding a 700 kW load in an operating power plant; the 380 V cables were connected to an artificial load of 2.5 Ω (120 A peak). The waveform of the 6 kV surges occurring at the closing of a circuit breaker is given in Figures 8 and 9. The waveform related to the 380 V cable is given in Figure 10. The waveform of the surge in the 6 kV cable (difficult to read in the reproduced oscillograms) has a rise time less than 1 μs, with a step after 4 μs due to the reflection in the proximity of the load (it has been verified that the waveform is independent on the presence of a load). Because the shield of the 6 kV cable was earthed only at the switchgear cell, the voltage between shield and earth at the other end of the shield was also measured (Figure 9), as this voltage may be the most likely to couple disturbances into adjacent control lines. The surge in the 380 V circuit has a rise time of about 50 ns.

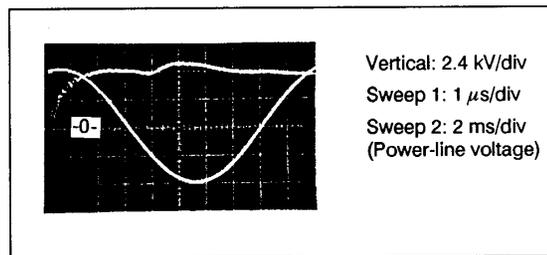


Figure 8. Surge Occurring on 6 kV Cable, between Conductor and Shield

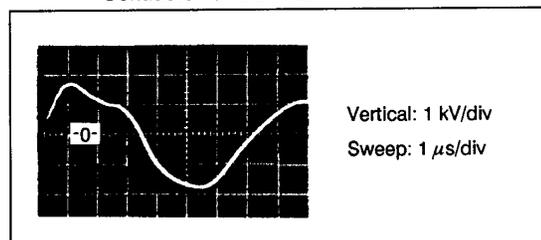


Figure 9. Surge Occurring on 6 kV Cable, between Shield and Earth, at Floating End of Shield

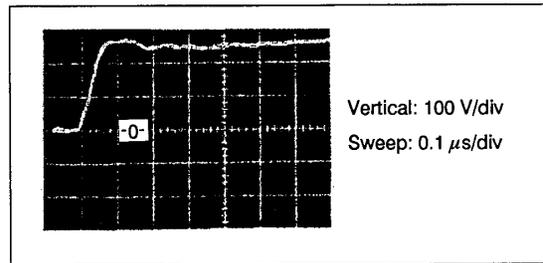


Figure 10. Surge Occurring on 380 V Cable

2.2 Measurements on cables – Two different sets of control and low-level cables, of the same type used in the operating plant, were included in the measurements of induced surges: one set laid down on the same tray as the 6 kV cables and near them, and another set in a PVC tube. For both the cable sets, two values of separation were used, a few centimeters and 0.3 m from the power cables. The length of the parallel runs of power cables and control cables varied between 100 and 300 m.

In this procedure the actual control and low-level cables were not used so that measurements could be carried out under reference condition independently of the service operating condition at the time of measurement. Using separate cables permitted changing the earthing condition of the shields at the process instrumentation in the field or at the supervisory system side.

The cables for which the measurement results are cited as representative examples (the complete test schedule included other types) were a 1-pair twisted thermocouple cable, and a 1-pair twisted, low-level signal cable.

The measurements were made with earthing of the signal source and of the cable shield at the field end (process instrumentation) or at the supervisory system end (according to some manufacturers specifications). At the signal source (process instrumentation side) the cable circuits were short-circuited. At the measurement side, the pairs were left in open-circuit condition, in order to simulate the real operating condition (normally corresponding to the multiplexer input).

For each cable, the characteristics of the voltages are summarized below, and selected oscillograms (Figures 11-14) illustrate the waveforms. For the sake of brevity, the oscillograms are given only for the common mode (CM) and differential mode (DM) measurements with the earthing of the signal circuit and of the cable shields at the process instrumentation in the case of the 0.3 m cable separation.

The results depend on the condition of earthing of the signal circuit and cable shield at the side of the computer and for a few cm separation from power cables present values (differential mode only) that are generally higher, up to one order of magnitude. The waveforms, however, present the same characteristics (frequency of oscillation, damping). Once again, the major object of citing these results in the context of this paper is to show waveforms, not detailed data.

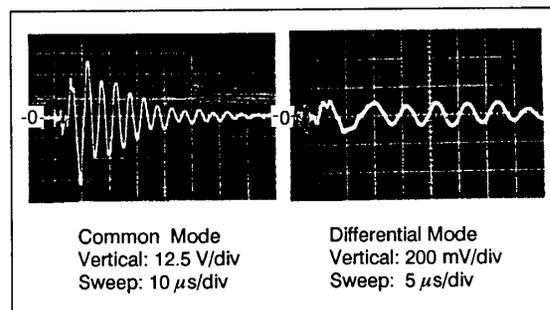


Figure 11. Transients Induced in Thermocouple Pair by Surge Occurring in 6 kV Cable

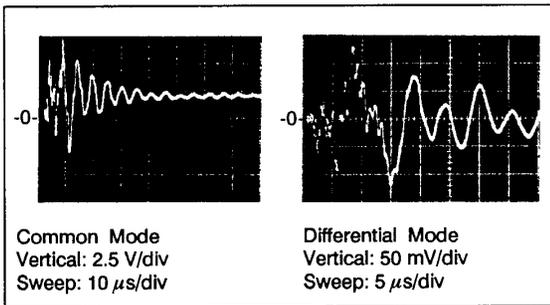


Figure 12. Transients Induced in Thermocouple Pair by Surge Occurring in 380 V Cable

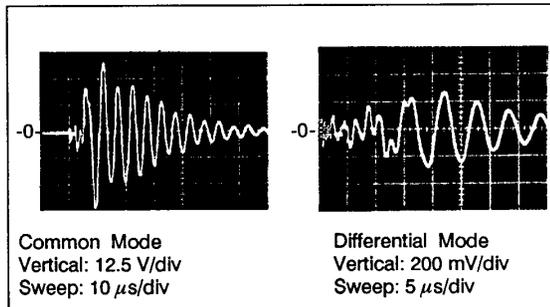


Figure 13. Transients Induced in Signal Pair by Surge Occurring in 6 kV Cable

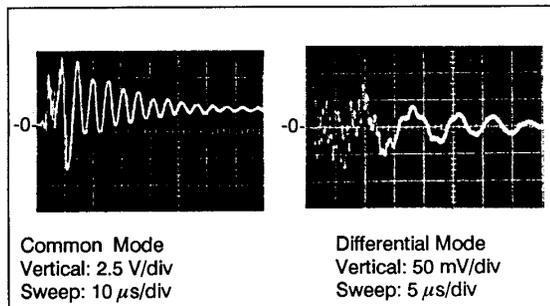


Figure 14. Transients Induced in Signal Pair by Surge Occurring in 380 V Cable

2.3 Discussion of the results – The surges induced in the measurement cables have common-mode levels of less than 100 V peak and differential-mode levels of less than 1 V peak mode on the signal lines (pairs).

The waveforms are substantially damped oscillatory waves (single shot), affected by a damping dependent on the propagation characteristics of the 6 kV and 380 V cables. The frequency of the oscillations ranges from about 100 to 200 kHz; higher values are observed for the common mode and lower for the differential mode voltages. The propagations affect each other; it is important to note that, in practical cases, the final waveform parameters of induced surges cannot be predicted or precisely defined due to the variability of the installation parameters (length of cables, dielectric constant of the insulation, separation from the ground of reference, etc.).

It is evident that, in particular for the differential mode, the induced surges do not include a unidirectional component. Such a component appears only in limited amount for the common-mode on the pairs within a cable, whenever the surges are induced by the transients on the 380 V power cable. In this case, because these 380 V cables are unshielded, the capacitive coupling occurs in the low frequency range.

3. Conversion of unidirectional lightning current in ground conductors into oscillatory surges

Tests were performed in the General Electric High Voltage Laboratory [3], aimed at simulating the passage of current in the grounding (protective earth) conductor of the service drop to a building. The motivation for the test was part of a general investigation on propagation of surges in low-voltage wiring without a preconceived notion on the waveform of the surges. As it turned out, the injection of a unidirectional current produced an oscillatory voltage.

The building wiring system was simulated by erecting a service entrance panel and several branch circuits, in a geometry representative of the normal wiring practice applied in the U.S. The service drop was simulated by using a pole-type distribution transformer including its connection to the earth reference (the ground plane of the laboratory in this test), a three-conductor service drop, and the prescribed grounding of the neutral at the service entrance (Figure 15). The service drop conductor configuration was the conventional, three-conductor "messenger wire" strung between the pole and the building. This messenger wire serves as a mechanical support as well as the multiple-grounded neutral conductor, with the two other conductors wrapped in a long pitch around the messenger.

Unidirectional 8/20 μs current impulses were injected between the neutral terminal of the distribution transformer and the ground plane. For the initial scenario of 30 kA in the messenger wire, resulting from an assumed 100 kA stroke (Figure 15), several flashovers were observed in the branch circuit wiring. The current had to be reduced to 1.5 kA for flashovers to stop. The resulting voltages were then measured between the phase and neutral conductors at various points of the branch circuits. In all cases, a large oscillatory component at 500 kHz was present in the measured voltage, in addition to a unidirectional component. Figure 16 shows the injected unidirectional current and Figure 17 shows the induced voltage appearing at one of the receptacles at the end of a branch circuit.

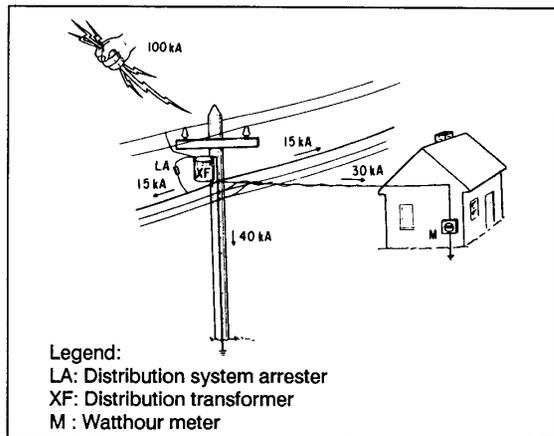


Figure 15. Configuration Simulating Lightning Current in Messenger Wire of a Service Drop to a House

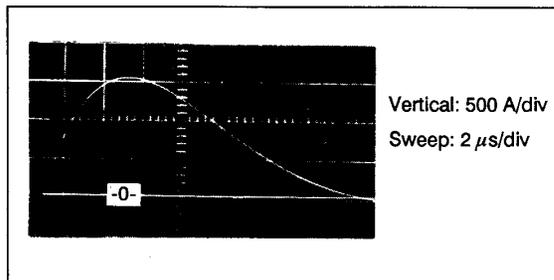


Figure 16. Injected Current in Messenger of Service Drop

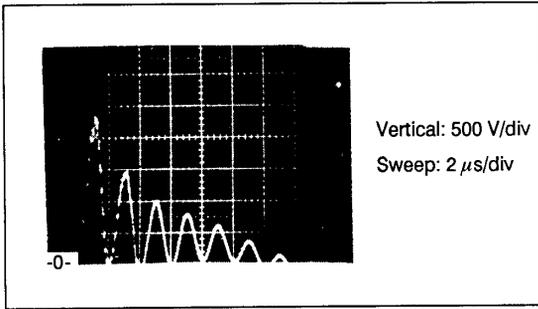


Figure 17. Transient Induced as 2.2 kV Differential Mode by a 1.5 kV 8/20 us Impulse in Messenger Wire

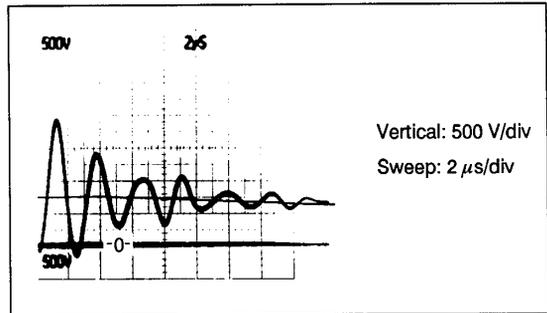


Figure 19. Oscillatory Transient Appearing in Building as Result of Application of the Figure 18 Surge

4. Response of a building wiring system to a unidirectional surge applied at the service entrance of the building

During a series of measurements aimed at defining the surge propagation characteristics of an actual building wiring [5], it was found that applying a unidirectional surge at the service entrance of the building, on the primary side of the transformer installed within the building, results in oscillatory transients. In contrast with the previous test where *current surges* were injected in the *grounding conductors*, in this test series *voltage surges* were injected *phase-to-phase* on the transformer primary. Figure 18 shows the voltage waveform at the transformer primary resulting from a standard 1.2/50 surge voltage. Note the occurrence of a small oscillation (12%) at the crest, but the predominant waveform of the surge is unidirectional, a situation somewhat different from the interaction between the Marx generator and the mismatched high-voltage line seen in Figure 2.

The resulting transient inside the building is a superposition of a unidirectional component and an oscillation (Figure 19), that is, the same situation of common impedance discussed in paragraph 1.3. This transient then propagates throughout the wiring inside the building.

In Figure 19, a line has been drawn on the oscillogram to show the unidirectional component in the resulting oscillatory transient. Note that the first cycle of the oscillation has an amplitude of three times the unidirectional component, compared to the 12% ring of the applied unidirectional surge. Details of the propagation characteristics are presented in Ref [5]. A significant finding was that for the dimensions of the building (length of the conductors), the faster front (0.5 μs) of the 100 kHz Ring Wave produced reflections which would not occur with a slower 1.2/50 μs impulse. On the other hand, much faster waves, such as the 5/50 ns burst [10] were found to be quickly attenuated. Thus, this ring wave produces a unique stress on equipment connected to the end of a branch circuit, such as an electronic control circuit in standby mode, the subject of the next investigation.

5. Effects of oscillatory surges on semiconductors

Various published and unpublished test results [11], [12] have reported that the reversal of bias on a semiconductor junction produced by applying an oscillatory surge can have a strong effect on the surge withstand capability of the device. The failure is also more likely when polarity reversals are applied during conduction, forcing the junction from a forward bias to a reverse bias.

This uncontested but not widely acknowledged finding was recently illustrated again by a series of measurements performed at NIST in preparation of qualification tests for an equipment using triacs for power control. The test schedule called for both the unidirectional wave, described as "Combination Wave" [13], and the 100 kHz Ring Wave defined in several U.S. standards and under consideration in IEC standards in progress [14].

Figure 20 shows one example of failure of the semiconductor occurring, not at the crest of the first part of the oscillation — which would be the expectation under a unidirectional stress — but upon reversal of the polarity during a Ring Wave test. This observation has rekindled interest in the issue and further tests are planned to better characterize the behavior of power semiconductors under ring wave stress.

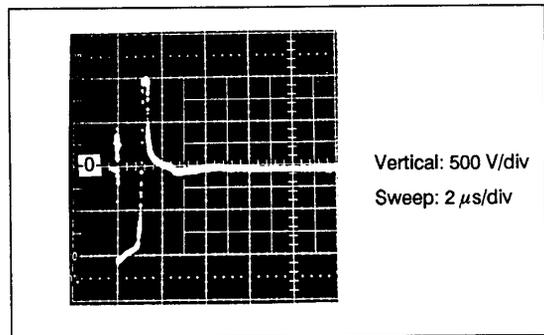


Figure 20. Failure of Triac During 100 kHz Ring Wave Test

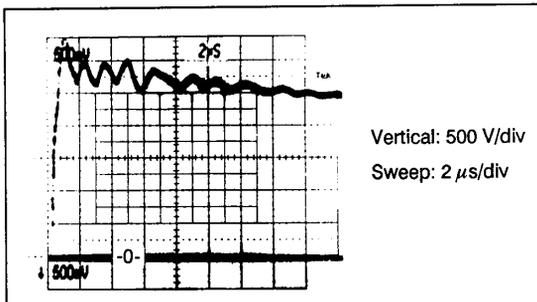


Figure 18. Applied Unidirectional 1.2/50 us Voltage at Primary of Service Transformer of Building

SURGE IMMUNITY SPECIFICATIONS AND TESTS

The four independent experiments cited in this paper include induced transients and injected transients, carried out at different times, countries, and installations. The results show that the surge environment of low-voltage circuits, for ac power systems as well as for control systems, is dominated by damped oscillations with a frequency range of 100-500 kHz.

This observation, especially for surges of large amplitude, should not be misconstrued as denying the significance of other surge test stresses, such as the Electrical Fast Transient [10]

where the interference aspects of the test are its major objective. Furthermore, in the high-voltage power apparatus domain, the traditional, "slow-rising" 1.2/50 μ s impulse has often been complemented by a chopped wave, introducing transition times much shorter than 1.2 μ s. Low-voltage equipment, however, is generally not required to pass a chopped-wave test.

On the other hand, for a device sensitive to total energy deposited in the device, unidirectional waveforms provide a suitable stress level. This three-part stress range – fast, low-energy; medium fast, medium-energy; slow, high-energy – has recently been emphasized in a revision of Ref [4], now in the final stages of approval by the IEEE, where the range of recommended surge tests includes the EFT, the 100 kHz Ring wave, the 1.2/50-8/20 μ s Combination Wave, a new 5 kHz Ring Wave, and a new unidirectional, high-energy 10/1000 μ s wave. The latter is similar to the 100/1300 μ s test under consideration by IEC TC77 [6], although the latter may involve extremely high energy levels encountered only under special circumstances [15], [16].

Discussions in IEC standard-writing groups produced the argument that the amplitude density spectrum of the 1.2/50 μ s impulse is so wide that it would cover the spectrum of the damped oscillatory ring wave. In fact, the ring wave shows a peak that extends almost one order of magnitude higher in frequency, as shown in Figure 21 [17]. Furthermore, the reversal of polarity effects discussed above are not produced by the 1.2/50 μ s impulse. In the IEC, both waveforms are considered in the general overview of immunity tests [6] and in dedicated basic standards [13], [14].

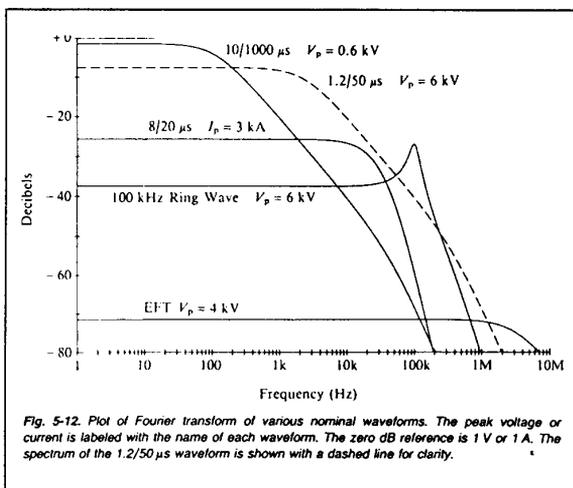


Figure 21. Frequency Spectra of Various Test Surges
Source: Standler [17]

CONCLUSIONS

1. The five independent examples cited in the paper provide converging evidence that there is a difference in the environment and its effect between power transmission systems, where the unidirectional test impulse reigns, and the low-voltage utilization circuits, where ring waves are a more realistic representation of the stresses encountered by modern electronic equipment.
2. To evaluate withstand capability of bulk solid insulation, it may be stressed adequately by unidirectional impulses. However, more complex devices such as windings or semiconductors may exhibit failure modes that will be more prevalent under ring wave test conditions.

3. The two basic surge waveforms, the Combination as well as the Ring Wave, should be taken into consideration for damaging as well as upsetting disturbances. Depending on the nature of the equipment, its characteristics, and installation type, one waveform may be preferred to the other. The choice is the responsibility of relevant product committees, best qualified for assessing the exposure of their equipment, on the basis of a comprehensive menu of real, realistic waveforms.

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**INCOMPATIBILITY
BETWEEN
THE 100/1300
SURGE TEST
AND
VARISTOR
FAILURE RATES**

Incompatibility Between the 100/1300 Surge Test and Varistor Failure Rates

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Significance:

Part 2 Development of standards – Reality checks

Demonstration ad absurdum:

Accepting the premise of prevalent 100/1300 high-energy surges and modeling the response of typical metal-oxide varistors leads to the conclusion that most of the billions of varistors in service should fail at alarming rates – but we know they do not. Ergo, the premise is not valid.

(See also paper "VDE 0160" in this Part 2 for an experimental demonstration.)

INCOMPATIBILITY BETWEEN THE 100/1300 SURGE TEST AND VARISTOR FAILURE RATES

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Abstract – A proposed high-energy surge test featuring a 100/1300 μs waveform and a peak voltage of 2.3 times the peak voltage of the low-frequency mains is under consideration by the IEC. The energy storage capacitor suggested for the surge generator, originally specified as high as 25 000 μF , has been scaled down but is still at a level of several thousand microfarads. To determine the energy dissipated in various surge tests, numerical integration is applied to a simple but realistic mathematical model of a test circuit. The energy that would be deposited into a varistor of the voltage rating commonly used in protecting load equipment, if subjected to this test, far exceeds the capability of the varistor, but reported varistor failure rates do not reflect such a situation. Thus, a re-examination of the premises that led to the 100/1300 μs test specifications appears necessary.

INTRODUCTION

The IEC Technical Committee TC77 is considering a surge test requirement based on the scenario of current-limiting fuses clearing a fault at the end of a cable, where the energy trapped in the system inductance causes a large transient at the time the fuse interrupts the current [1]. That scenario was first described and quantified by Meissen [2], and incorporated in German Standard VDE 0160 [3]. However, there seems to be an inconsistency between the predictable failure of varistors that would be subjected to this test, and the reported failure rate of varistors, considering that several hundred million of these varistors are currently connected across the mains in a wide variety of load equipment.

Accepting the premises that led to the specification of this test, the authors developed a simple circuit model that produces the specified waveform, with an energy storage capacitor having the value specified in the current amendment to VDE 0160 [4]. Applying the surge available at the output of the circuit model to varistors of the ratings commonly used in load equipment results in an amount of energy deposited in the varistor that exceeds by far the capability of the varistor.

If the scenario of fault-clearing by fuses occurs at a frequency such that a universal test should be required to simulate its effect on all equipment, then one would expect a substantial failure rate among the varistors incorporated in equipment in actual service. This expectation follows from the computations which show that typical varistors used in mains-connected equipment cannot survive such a test. While equipment failure rates are not widely published, anecdotal information and the sharing of field experience in the engineering community do not support the existence of a large failure rate attributable to that scenario. Therefore, the authors suggest that the premises that led to the specification of the test, the consequences of the test on in-service varistors, and the actual failure rates of these varistors should be examined to resolve the apparent inconsistency.

THREE FORMS OF THE 100/1300 TEST SPECIFICATION AND VARISTOR RATINGS

Figure 1 shows the parameters of the 100/1300 μs surge described in Ref [1]. The voltage level is specified as $2.3 \times U_{pk}$, the peak of the mains voltage. However, under the clause addressing the test generator specification, one finds the interim statement 'Under Consideration'. The VDE 0160 documents do not include specifications for the test circuit, but leave the circuit design to private industry [5]. Referring to working documents and the original and later amendments of VDE 0160, the test circuit essentially consists of an energy storage capacitor up to 25 000 μF discharged into the equipment under test.

A subsequent VDE amendment shows a table of capacitance values ranging from 700 μF for 660 V rms mains to 6000 μF for 220 V rms mains. According to the amendment, the capacitor charging voltage may be set at one of two levels, respectively $2.3 \times$ and $2 \times U_{pk}$, the peak of the mains voltage. Furthermore, two durations are also stated, the original 1300 μs and one reduced to 400 μs .

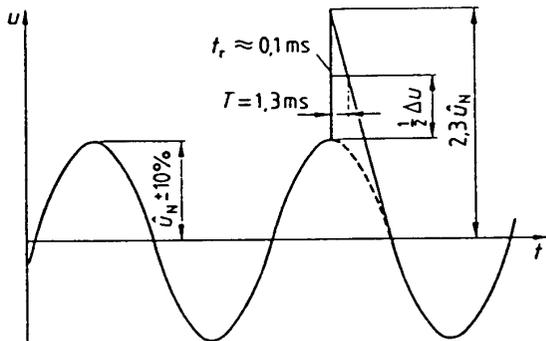


Figure 1. Parameters of the 100/1300 μ s surge test waveform from VDE 0160 [3].

For the sake of exploring the implications, the authors accept the 100/1300 μ s at $2.3 \times U_{pk}$ proposal and will develop conclusions on varistor performance under this proposed test, the more severe of the two levels and two durations. However, an ambiguity exists on the test procedure. Two different interpretations of the VDE 0160 text can lead to different test procedures: providing a fixed charging voltage for the energy storage capacitor or re-adjusting the voltage after connecting the test specimen.

These two different interpretations result in two approaches with two sets of different values for the model parameters. A first method, based on using fixed charging voltage, is the approach generally used in surge testing [6], [7], often described as “let it rip.” However, the VDE 0160 standard contains a sentence that reads: “The test apparatus is used to generate the test voltage impulse between the terminals of the test specimen while it is operating.” This statement would suggest a second method, that is, adjust the charging voltage with the test specimen connected. The implications of the two interpretations will be shown below.

A further statement reads “... under certain conditions, the required half-peak duration of the pulse of 1.3 ms cannot then be reached. In this case it shall be ensured that not less than 80% of the energy stored in the test device is supplied to the sample.” A simple capacitor discharge circuit, as implied by Figure 14 of VDE 0160, reproduced here as Figure 2, will require a parallel resistance to pull the voltage of the 6000 μ F capacitor down to half-value in 1.3 ms. The surge generator shown as “Test Equipment” in Figure 2 does not explicitly include such a resistance. Such a resistor will drain enough energy from the capacitor that it is not clear how 80% of the capacitor energy will be left for the sample. Thus, the computations presented here include three approaches:

1. Fixed initial charging voltage (“let it rip”)
2. Fixed peak surge voltage (readjust initial voltage)
3. Expend 80% of the capacitor energy into the test specimen.

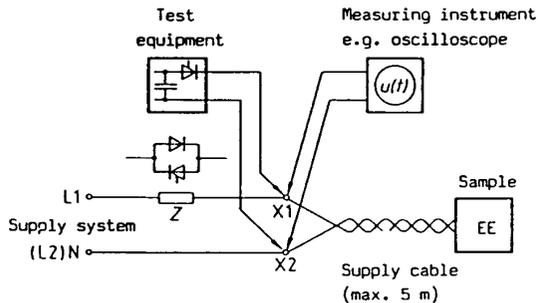


Figure 2. Schematic test circuit for the 100/1300 μ s surge test waveform from VDE 0160 [3].

Metal-oxide varistors offered by manufacturers include ratings of 130 V rms for applications in 120 V systems and 250 V rms for application in 220 V systems. The motivation for using these varistor ratings in electronic equipment is, of course, the desire to provide the lowest possible clamping voltage to protect sensitive equipment [8]. Therefore, the model developed in this paper is applied to a 220 V system and a 250 V varistor. For these values, the peak surge voltage is $220 \times 1.41 \times 2.3 = 715$ V, and the varistor voltage (at 1 A) is 485 V.

ENERGY DEPOSITION IN VARISTORS FOR THREE TEST CRITERIA

A simple, but realistic model of a capacitor discharge through a wave-shaping circuit can produce the specified surge rise and duration of 100/1300 μ s. Referring to Figure 1, this 1300 μ s half-maximum is that of the surge, which is superimposed on the mains sine wave. Thus, the surge duration of 1300 μ s corresponds to the level of $U_{pk} + \frac{1}{2}(1.3 \times U_{pk})$, that is $1.65 \times U_{pk}$.

Figure 3 shows a simple test circuit which is the basis for the mathematical model. Without the metal oxide varistor (MOV) in place, the circuit is configured for the open-circuit discharge test of a model test generator. Using circuit component values of $C = 6000 \mu$ F, $L = 25 \mu$ H, and $R_s = R_p = 0.27 \Omega$, the open-circuit voltage response is generated numerically and is shown in Figure 4.

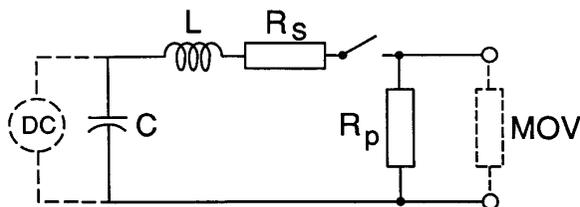


Figure 3. Schematic test circuit for the 100/1300 μ s surge test waveform used for the mathematical model. (The open-circuit test is performed without the MOV.)

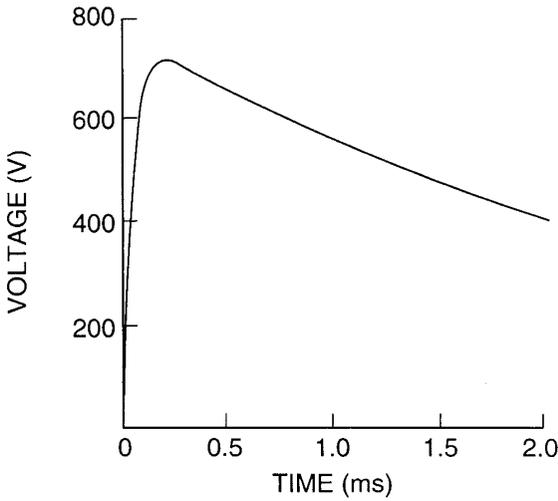


Figure 4. Plot of the open circuit voltage waveform obtained by discharging the capacitor in Figure 3 without the varistor in place.

The basic model is then modified by placing the varistor across the open-circuit terminals of the circuit in Figure 3. The non-linear I-V character of the MOV is expressed by the ‘equation of state’:

$$v_m = \lambda \left(\frac{i_m}{i_1} \right)^{1/p} + R_m i_m \quad (1)$$

in which i_m is the current through and v_m is the voltage across the MOV. This equation is a two-term reduction of the full five-term model for a varistor [9]. It is appropriate to the slow waveforms in this surge testing environment. The unit current, i_1 , should be chosen to be characteristic of the problem. In the present application, $i_1 = 1$ A. Where doing so causes no ambiguity, we have suppressed the current unit, i_1 , in the analysis. R_m is the series resistance of the MOV, in the examples used here, 0.12Ω . The voltage threshold for the MOV, λ , has a nominal value of 485 V. The exponent, p , is nondimensional and in the present calculation has been given the typical value of 31 [9].

The circuit model is formulated to produce a system of differential equations which are solved for q , the stored charge on the capacitor, i_m , and e_m , the energy deposited in the varistor. The voltage drops around the circuit must satisfy

$$v_L + v_{R_s} + v_m + v_C = 0 \quad (2)$$

which yields

$$L \frac{di}{dt} + R_s i + v_m + \frac{q}{C} = 0 \quad (3)$$

It is possible to express (2) as a differential equation in i_m because v_m is a function of i_m and Kirchoff’s Law implies that i is a function of i_m , $i = i_m + v_m/R_p$. The evolution equations for q , i_m , and e_m are thus:

$$\frac{dq}{dt} = -i \quad (4)$$

$$\left[L \frac{di}{dt} \right] \frac{di_m}{dt} = -R_s i - v_m - \frac{q}{C} \quad (5)$$

$$\frac{de_m}{dt} = i_m v_m, \quad (6)$$

where

$$\frac{di}{di_m} = 1 + \frac{R_m}{R_p} + \frac{\lambda}{p R_p} i_m^{1/p-1}. \quad (7)$$

The non-linear system (4-5) is equivalent to a second order equation for i_m . The initial conditions for i_m and e_m are zero, while $q(0)$ is chosen to generate the maximum voltage required by the test as specified below. The solution of the system is computed using a general purpose ordinary differential equation solver, PLOD, which permits a variable time-step size and handles stiff systems (those with widely differing time constants) [10].

1. Results with fixed initial charging voltage

Figure 5 shows the voltage, v_m , and current, i_m , waveforms at the varistor under the ‘let-it-rip’ mode, a procedure under which the open-circuit voltage of the generator is preset and no adjustment is made after connecting the test specimen. The desired waveform is generated with $q(0) = 9$ C. Note that the additional path in the circuit with the varistor in place reduces the peak voltage below that for the open-circuit test, from 715 V to 680 V. Figure 6 displays the time-resolved deposition of energy in the varistor, and compares it to the allowable energy deposition.

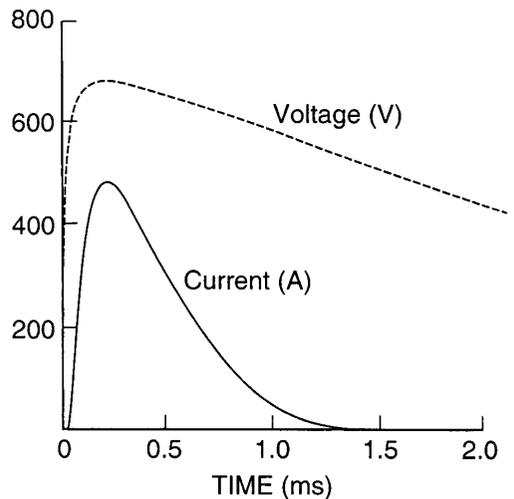


Figure 5. Plot of the current and voltage waveforms at the varistor with charge $q(0) = 9$ C and with $\lambda = 485$ V.

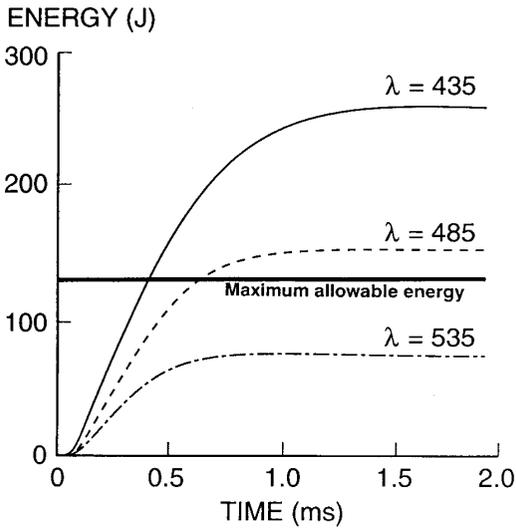


Figure 6. Energy waveforms at a fixed charge, $q(0) = 9C$, for dissipation in the MOV at three ratings of the varistor, $\lambda = 435V, 485V,$ and $535V$.

The three energy curves correspond to three values of the parameter λ , the voltage rating of the MOV. The values are $\lambda = 435 V, 485 V, 535 V$, which are the lowest, mid, and highest values for acceptable ratings from the manufacturer (a $\pm 10\%$ band about the nominal value of a varistor rated at 250 V). The typical energy rating for a 20-mm, 250-V varistor is 130 joules. Denoting by $e_m(t, \lambda)$ the energy as a function of time and of voltage rating, the total amount of energy deposited by the pulses, $e_m(\infty, \lambda)$, is displayed in Table 1. Only the highest voltage rating survives. This fundamental model suggests a gross inconsistency between the failure rate that the test would produce and the available information on actual failures of in-service varistors. The Appendix confirms these conclusions based on an analytical, inductance-free model for the test circuit.

imum with the MOV in the circuit remain $2.3 \times U_{pk}$, rather than accept whatever value will occur under the 'let-it-rip' mode. Under this interpretation, the charging voltage of the generator must be increased to obtain the required level; additional energy is stored in the capacitor and destruction of the MOV is assured at all permissible tolerances, Figure 7. Thus, for the three values of the voltage tolerances given above the initial charges on the capacitor, $q(t, \lambda)$ to reach the voltage maximum, 715 V, and the energy deposited are displayed in Table 2. In each case, the energy rating of the MOV, 130 J, is exceeded substantially.

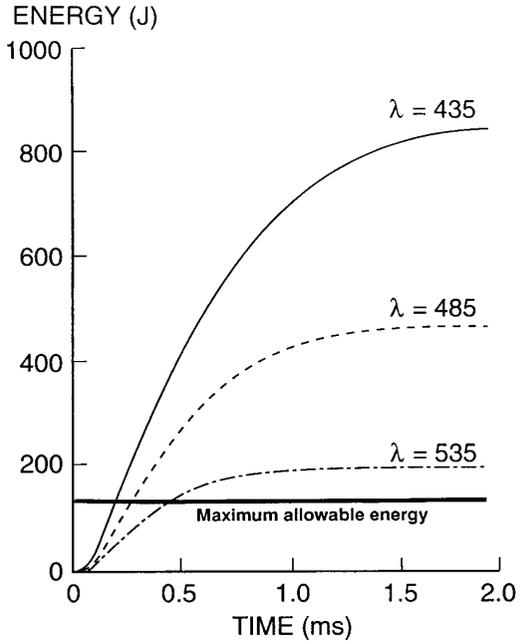


Figure 7. Energy waveforms with the peak voltage fixed, $V_{max} = 715V$. The energy dissipated in the MOV is shown at three ratings of the varistor, $\lambda = 435V, 485V,$ and $535V$.

TABLE 1
Energy deposited in a 20-mm dia, 250-V MOV as a function of tolerance on voltage rating, at fixed initial voltage ("let it rip")

Voltage tolerance (%)	-10	0	+10
Varistor voltage λ (V)	435	485	535
Energy $e_m(\infty, \lambda)$ (J)	257	152	74
Peak v_m (V)	615	645	673

TABLE 2
Energy deposited in a 20-mm dia, 250-V MOV as a function of tolerance on voltage rating, at fixed (readjusted) peak voltage

Voltage tolerance (%)	-10	0	+10
Varistor voltage λ (V)	435	485	535
Energy $e_m(\infty, \lambda)$ (J)	839	459	192
Initial charge $q(0)$ (C)	11	10.75	10

2. Results with readjusted charging voltage

The authors note that VDE-0160 is not unambiguous on the character of the test procedure. The standard may be construed to require that the voltage maxi-

3. Expend 80% of capacitor energy in specimen

An alternate criterion suggested by VDE 0160 is that 80% of the capacitor energy be dissipated in the MOV. For the simple circuit on which the present model

is based, a ready calculation using the capacitively stored energy, $q^2/2C$ (of order 5000 J), shows that no more than 10% of the stored energy is spent in the MOV in the simulations according to the two approaches discussed above. Yet, these two tests are already destructive of the device. It seems likely that a test that would meet the 80% criterion would provide an even more severe stress to the equipment, and provide a greater disparity between the model results and field experience.

Thus, the authors suggest that a reexamination of the premises that led to the VDE 0160 Standard should be considered before incorporating a blanket requirement for such a test into new IEC surge immunity standards. The authors plan to perform actual tests on typical varistors to further support the computations presented in this paper.

CONCLUSIONS

1. A mathematical and derived computational model has been presented which permits the evaluation of many aspects of varistor performance over a range of conditions which are characteristic of the actual operating environment and also of the test environment contemplated by VDE 0160 and other surge standards.

2. Computer model predictions of the impact of the proposed 100/1300 μ s surge test on the millions of varistors in service shows that these varistors should experience a greater failure rate than indicated by available information on actual failures. The simplified inductance-free model provides analytical confirmation of this result. This inconsistency raises serious questions on the proposed requirement of such a severe test to a wide range of equipment.

3. The lingering ambiguity on setting a constant open-circuit voltage or adjusting the voltage while the specimen is connected needs to be clarified. A constant open-circuit voltage is the generally accepted practice in surge testing. The premises that led to this new surge test may justify adjusting the charging voltage after the test specimen has been connected to the surge generator; that adjustment, however, results in larger amounts of energy being dissipated in surge protective devices, making the apparent incompatibility identified above even greater.

4. The criterion that 80% of the capacitive energy must be transferred to the test specimen may be difficult to satisfy and needs clarification. The authors have been unable to identify a simple circuit which satisfies the criterion while maintaining the required rate of decay under open circuit conditions.

5. While the authors do not question the validity of the fuse-blowing scenario, they recommend a critical review of the statistics of the occurrence of fuse blowing, of the use of varistors with low clamping voltage, and of the distribution of actual clamping voltage within manufacturing tolerances. The sensitivity model developed in this paper may be a useful tool in evaluating the effect of these tolerances. The authors also urge all users to share information on the observed failure rates, as well as to perform validating tests, in order to provide a broader perspective on these issues.

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¹Certain software is identified in this paper in order to adequately specify the procedures used to produce the numerical results. Such identification does not imply that the software is necessarily the best available for the purpose.

APPENDIX - SENSITIVITY OF $e_m(\lambda)$

The sensitivity of the energy deposited in the varistor, $e_m(\infty, \lambda)$, to changes in the voltage rating of the MOV is given by

$$\frac{de_m(\infty, \lambda)}{d\lambda}$$

This quantity can be determined by numerically integrating the model (4-7). However, this sensitivity can also be analyzed in closed form by considering the inductance-free, ($L = 0$), version of the model (Eq. 2 or 3). In this case, an algebraic expression for the sensitivity can be derived. It is shown to be accurate to within 15% over a significant range of values of λ .

The sensitivity of the energy dissipated in the MOV to changes in λ is most easily expressed in terms of the initial current through the varistor and the sensitivity of that current to changes in λ . In the inductance-free case, the initial conditions must be reformulated so that $i_m(0)$ is non-zero. When the initial data is the capacitor charge, $q(0)$, as was the case with the inductive model, it is possible to find $i_m(0)$ by finding the root of the non-linear expression given by:

$$F(i_m, \lambda) = (R_m(1 + \frac{R_s}{R_p}) + R_s)i_m + (1 + \frac{R_s}{R_p})\lambda i_m^{1/p} + \frac{q(0)}{C} \quad (8)$$

$$= 0 \quad (9)$$

which is a direct translation of (3) in the case that $L = 0$.

This equation can be solved for $i_m(0)$ using a few iterations of Newton's (gradient) method. Furthermore, the sensitivity of i_m to changes in λ is given by:

$$\frac{di_m}{d\lambda} = -\frac{\partial F}{\partial \lambda} / \frac{\partial F}{\partial i_m} \quad (10)$$

$$= -i_m^{\frac{1}{p}} \left(R_m + \frac{R_s R_p}{R_s + R_p} + \frac{\lambda}{p} i_m^{1/p-1} \right)^{-1} \quad (11)$$

To find the energy deposited in the MOV, first consider the evolution of the varistor current, i_m . This time evolution is given by differentiating equation (3) and applying (1) and (6):

As a result of separability, the energy may be written as a time-independent integral in which λ appears as a parameter:

$$e_m(\infty, \lambda) = \int_0^\infty v_m i_m dt \quad (14)$$

$$= \int_{i_m(0)}^0 v_m i_m G(i_m) di_m \quad (15)$$

$$= C\Pi \int_{i_m(0)}^0 \left(\Sigma i_m + \frac{\lambda}{p R_m} \left[\frac{1}{R_s} + \frac{1}{R_p} \right] i_m^{1/p} \right) \times \frac{\lambda i_m^{1/p} + R_m i_m}{\lambda i_m^{1/p} + (R_m + R_p) i_m} di_m, \quad (16)$$

where

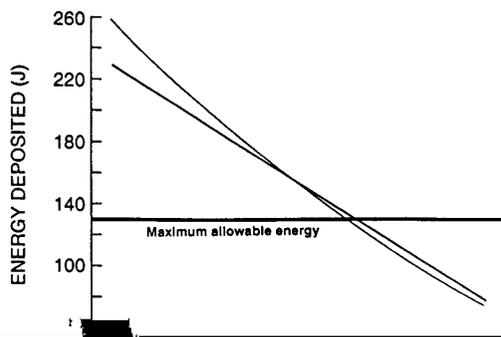
$$\Pi = R_s R_p R_m \quad \text{and}$$

$$\Sigma = \frac{1}{R_s} + \frac{1}{R_p} + \frac{1}{R_m}$$

Expansion in λ about the nominal voltage threshold for the varistor, that is in powers of $\Delta\lambda$ where $\lambda = \lambda_{nom} + \Delta\lambda$, gives the leading term for the sensitivity of e_m to changes in λ :

$$\frac{de_m(\infty, \lambda)}{d\lambda} = C R_s R_p R_m \left(\frac{1}{R_s} + \frac{1}{R_p} + \frac{1}{R_m} \right) i_m \frac{di_m}{d\lambda} \quad (17)$$

The sensitivity (17) may be evaluated algebraically once i_m is known from solving (8-9). A comparison with the inductive model is displayed in Figure 8. The curve shows numerically determined values of $e_m(\lambda)$ for the inductive model in 'let-it-rip' mode. The straight line has a slope determined by evaluating expression (17). The same expression may be used without recomputing the initial current in the case that the voltage, and hence the current, is adjusted to a fixed value.



**TESTING
VARISTORS
AGAINST
THE VDE 0160
STANDARD**

Testing Varistors Against the VDE 0160 Standard

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Significance:

Part 2 Development of standard – Reality checks

Demonstration ad absurdum:

Accepting the premise of prevalent 100/1300 high-energy surges and subjecting typical metal-oxide varistors to the stress from a test performed with a prototype generator leads to the conclusion that most of the billions of varistors in service should fail at alarming rates – but we know they do not. Ergo, the premise is not valid.
(See also paper “Validating Surge Tests ...” in this Part 2 for a demonstration by numerical modeling.)

Testing Varistors Against the VDE 0160 Standard

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Abstract — *High-energy surge tests have been performed on metal-oxide varistors of a type in common use, according to a proposed IEC standard derived from German Standard VDE 0160. The surge generator used for the test was a prototype commercial device developed especially to deliver the 100/1300 μ s waveform specified by VDE Standard 0160. Depending on the position of the varistor within its manufacturing tolerance band, failure or degradation can occur, validating the concern that this test requirement may be too severe for universal application.*

INTRODUCTION

Concerns over the occurrence of high-energy surges associated with current-limiting fuse operation (Meissen, 1983 [1]) have led the German standards organization (VDE) to specify a high-energy surge test to be applied to electronic equipment installed in industrial environments (VDE 0160, 1988 [2]). Essentially, the test requires discharging into the ac line interface of the equipment under test (EUT) a capacitor of such capacity that the specified waveform is generated, initially charged at a voltage suitable for producing a peak of 2.3 times the power-system sine-wave peak (Figure 1). Technical Committee 77 of the IEC has included this test in its menu of surge immunity tests (TC77B/WG3, 1990 [3]), without limiting the scope of application to industrial environments intended by the Meissen paper. Thus, this test is likely to become a general requirement imposed on commercial and consumer equipment, unless its implications are recognized. In the absence of a readily available surge generator, computer modeling of the test had previously been performed (Fenimore & Martzloff, 1990 [4], 1991 [5]). The findings of these simulations have shown that typical varistors, of which many millions have been installed and continue to operate satisfactorily, cannot survive the proposed IEC/VDE test because excessive energy would be deposited in these varistors during the surge. The recent availability of a prototype surge generator made it possible to subject typical varistors to the VDE/IEC surge, as reported in this paper.

TESTING VARISTORS WITH HIGH-ENERGY SURGES

Schaffner*, a manufacturer of surge generators, has now developed a prototype that can produce the VDE 0160 surge; in response to an invitation to try out this prototype, an informal work session was conducted at the Schaffner facility to subject typical varistors to the VDE 0160 surge. The generator includes the specified capacitor, up to 6000 μ F, the necessary dc supply to charge the capacitor, a 220-V ac supply (for European environments), and suitable means to decouple the test specimen circuit from the laboratory ac system. Details of the circuits are still proprietary, and only the output of the generator is described in this paper. A chronological recitation of the work session would require first a discussion of the various considerations and conditions of the test. Recognizing the natural curiosity of the readers, let it be stated here that one varistor was destroyed during the test, and the other (barely) survived, consistent with the predictions of the computer modeling. Having thus given away the outcome, let us now proceed with the detailed recitation of these considerations and conditions.

* As a policy, the National Institute of Standards and Technology disclaims any implied endorsement of a commercial product when identifying such products for the sole purpose of adequately describing the equipment used in the experiment. In this particular case, the prototype generator used in the tests was the only one known to be available. Furthermore, there is no certainty that Schaffner will offer a commercial product based on this prototype.

Voltage across the test specimen and current delivered by the surge generator were recorded with the instrumentation available at the Schaffner engineering demonstration facility. The software package included in the digital storage oscilloscope did not have the capability of computing the power ($i \times v$) dissipated in the varistor and integrating it into total energy deposited. Manual integration of the recorded traces was performed after the tests. This computation yields results of sufficient magnitude (that is, large overstress of the varistor) to make precise computing unnecessary in evaluating the outcome of the test.

The test specimens (EUT) were 20-mm diameter varistors, consisting of two 130-V rms rated devices connected in series, a good approximation of the practice of applying 250-V rated varistors in the 220-V equipment used in Europe (Martzloff and Leedy, 1989 [6]). The nominal voltage, V_{nom} , of each varistor (voltage measured with 0.5 mA or 1 mA dc injected in the varistor) was determined before the test for each device. One varistor pair (referred to as EUT #1) had a nominal voltage of 392.6 V, the other pair (EUT #2), 399.5 V. The nominal voltage for a 250-V rms varistor is 390 V, the minimum 354 V, and the maximum 429 V (Harris Manual, 1990 [7]). Thus, EUT #1 is situated at 1% above the nominal value of a 250-V rated varistor, while EUT #2 is at 2.5% above the nominal value.

To test the varistors under the worst case condition (that is, the varistor at 10% below nominal, thus drawing energy from the generator for a longer portion of the surge waveform), the test voltage should be raised above the voltage specified for nominal test conditions. To place the varistor under conditions equivalent to those prevailing for a -10% specimen, a varistor at some tolerance level must be subjected to the same current as that occurring for a -10% varistor at the nominal test voltage. With the nominal VDE 0160 test voltage of 2.3 times the 220-V peak (714 V), the available EUT varistor specimen can be tested in a manner equivalent to a -10% tolerance varistor by raising the test voltage.

For EUT #1 which is 1% above the V_{nom} of a 250-V rated varistor, the test voltage should be 10% higher than the nominal 714-V peak, plus 1%, that is, 792 V. For EUT #2, 2.5% above the V_{nom} , the test voltage should be 12.5% higher, 803 V. This increased test voltage will place the varistor at the correct value of current on its I-V characteristic, but raises the power dissipated in the varistor by the same percentage. Thus, the energy deposition in varistors other than -10% tested under the artificially raised test voltage received 11% or 12.5% more energy than what a varistor at -10% would have received. However, considering the energy levels observed in the tests reported below (about 200% of rated levels, this 11-12.5% excess does not affect the conclusions. The significant parameter to be observed is the current level, and that correct level was indeed achieved by raising the test voltage.

The VDE 0160 document states that the *specified surge test voltage should be maintained* across the terminals of the EUT, rather than the usual method of having a preset open-circuit voltage, and then connect the EUT *without changing the generator setting* (the so-called 'let-it-rip' mode [5], and (ANSI/IEEE C62.41-1987, [8]). Meissen confirmed this interpretation of the document [9], so that the charging voltage of the generator capacitor was increased toward obtaining the specified voltage with the EUT connected, using an expendable EUT varistor during preliminary tests. However, the prototype generator output voltage, with maximum charging voltage and with varistor connected, could only be raised to 774 volts (Figure 2) instead of the 792 V or 803 V necessary to place the #1 and #2 varistors in the -10% tolerance situation. Thus, EUT #1 was actually tested in a condition corresponding to $774/792 = 98\%$ of the worst case level, and EUT #2 at $774/803 = 96\%$ of the worst case level. In other words, EUT #1 was tested as if it were at a -8% tolerance level, and EUT #2 at a -6% tolerance level with respect to a 0% tolerance on their V_{nom} .

The manufacturer's specifications [7] show a 70-J single-pulse energy rating for the 130-V varistor, or 140 J for two in series. Figure 3, from Ref [5], shows the predicted energy deposition as a function of the varistor position in its tolerance band, for the test condition where the voltage is maintained across the EUT by readjusting the surge generator charging voltage.

The VDE 0160 document shows an elementary circuit diagram (Figure 4) with a maximum of 5 m of leads between the input port of the test specimen and the point of injection of the surge. Accordingly, the test circuit set up by Schaffner included approximately 5 m of leads "suitable for a 16 A load" between the varistor and the output of the generator. Thus, the impedance presented by the test specimen to the applied VDE 0160 surge includes a resistance that will reduce the stress of the varistor; however, this reduction is not readily recognized by the simple mention in the figure of a 5-m maximum lead length, and the cross-section of the conductors is not specified. Operators can interpret the test procedure in a way producing maximum stress (a short lead of large cross section) or a minimum stress (maximum of 5 m of leads with small cross section).

In accordance with the interpretation of the Figure 4 diagram, the voltage measured and shown in Figure 2 is the total of the voltage developed across the varistor and the lead drop. To evaluate the implications of this interpretation, the next test was performed, without changing the generator setting (at its maximum available voltage), with the voltage measurement made at the varistor terminals (Figure 5). Note the 700-V peak in this test, or a 74-V difference (10%) from the value recorded in Figure 2. In the modeling of References [4] and [5], the effect of this 5-m test lead had not been included, so that the conclusions of the modeling are more pessimistic than the consequences of a test condition with a lead length included. Thus, the varistor would be under 10% less voltage stress (keep in mind the nonlinear relationship between voltage and current) than the model prediction, and possibly could survive.

THE DEATH OF A VARISTOR

According to a subsequent amendment to the VDE 0160 test specification, the maximum* capacitor value and the duration of the surges may be reduced to 300 μs for equipment installed in circuits protected by fuses of less than 35 A continuous rating. This reduction will provide significant relief to varistors included in non-industrial environments. However, the IEC document [3] does not include that reduction. The test sequence for EUT #1 included two surges with this reduced stress (Figure 6), followed by surges with the full 6000 μF capacitance and full 1300 μs duration, at the maximum available generator voltage, as shown in Figure 2. Before and after each surge, the varistor V_{nom} was recorded to track any shift in characteristics, comparing it to the maximum shift of 10% allowed in the manufacturer's specifications.

The test sequence and results for EUT #1 (a specimen in the -8% tolerance position), starting with no prior surges applied, were the following:

- Shot 1: 718 V crest, 400 μs duration, V_{nom} shift of 1% (Figure 6)
- Shot 2: 768 V crest, 1100 μs duration, V_{nom} shift of 1%
- Shot 3: 774 V crest, 1400 μs duration, V_{nom} shift of 1% (Figure 2)
- Shot 4: Repeat, same settings as shot 3 (voltage measured at varistor, Figure 5),
 Varistor (a) of pair punctured
 Varistor (b) of pair externally intact, but $V_{\text{nom}} = 0$ (short circuit)
 Energy deposited in the varistor; approximately 300 J (215% of rating)

The same test sequence was then applied to EUT #2, that is, first two shots at reduced stress, and then full stress for shot 3 and four additional shots. The V_{nom} shift grew from 1% after the first shot to 6% after the last shot, as measured after cooling down following the test. By the time the author had returned to the United States (20 days later), the shift in V_{nom} , determined by more systematic measurement at NIST, was reduced to 4%. The difference between the 6% immediately after the test and the 4% after 20 days may be the effect of a slow recovery of the material, or a difference in the precision of the measurements, or both.

* The surge duration is the specified parameter in the VDE 0160 document, therefore the required value of the capacitor is dependent upon the impedance of the EUT.

Notwithstanding the shift in V_{nom} , no apparent external damage was visible, except for some darkening of the red epoxy coating. Thus, while EUT #2 did survive a test corresponding to a -6% tolerance position, the onset of permanent change leading to failure was observed.

DISCUSSION OF THE RESULTS

From the simulation predictions, it was expected that the varistors would be destroyed by the test, even though the (late) realization of the stress reduction provided by the lead length does somewhat change the situation. In other words, the 10% loss of voltage caused by the leads places the varistors used in these tests at respectively +2% and +4% in the tolerance band, a condition that the prediction describes as marginal survival. The joule rating specified by the manufacturer tends to be conservative, so that it may take more than 140 J to destroy a varistor. Furthermore, a larger population of test specimens may produce a distribution of more failure as well as more survivals as only two test points can only provide an indication, not a certainty. However, the conclusion is clear, that varistors of common use in commercial and consumer equipment would be in severe jeopardy if the full 100/1300 μ s surge were applied, even with the mitigating effect of the 5-m lead length. Discussing the test results with Meissen, we agreed on the following conclusions:

1. There is no disagreement that the basic phenomenon of fuse blowing can lead to the high-energy surges described by Meissen in the heavy industrial environment (circuits with fuses above 35 A).
2. The prediction of varistor failure through modeling is consistent with the tests; the mitigating effect of the allowable EUT lead reduces the forecast of widespread failures, but varistors in the lower tolerance bands are still at risk.
3. The amendments to VDE 0160 providing for reduced maximum capacitance values (see the footnote on page 3) and reduced duration make the test more realistic. Further evaluation of these reduced stress levels would show appropriate limits of application.
4. However, this stress reduction has not yet been acknowledged by the IEC proposals (Figure 1, showing only one value of 1.3 ms is excerpted from the IEC document, not the amended VDE 0160 where the alternate duration of 0.3 ms is shown). This paper is therefore submitted to the engineering community at large as a recommendation of limiting the full duration of a 1300 μ s surge and its high energy to the industrial environment for which it was first proposed.
5. The concept of readjusting the surge generator charging voltage to maintain a specified *test voltage* across the specimen is different from the usual practice of maintaining a fixed open-circuit voltage for the generator. However, it may be compared to the practice of readjusting the surge generator used for surge arrester tests at a specified *test current* level. As long as the implications of the procedure are recognized, either method may be suitable, if uniformly interpreted.
6. In its present form, the VDE 0160 document leaves open the possibility of different interpretations by different operators. Should the principle of a high-energy test be adopted by the IEC, more detailed specifications need to be developed and agreed upon by interested parties.

ACKNOWLEDGEMENTS

This successful work session was made possible through the cooperation of T. Hilger and M. Ryser of Schaffner. W. Meissen made the long journey from Erlangen, Germany to Luterbach, Switzerland to participate in the test, and reviewed the manuscript of this paper. The simulation predictions of References [4] and [5] were developed by C. Fenimore. All these contributions toward a better understanding of the VDE 0160 test implications are gratefully acknowledged.

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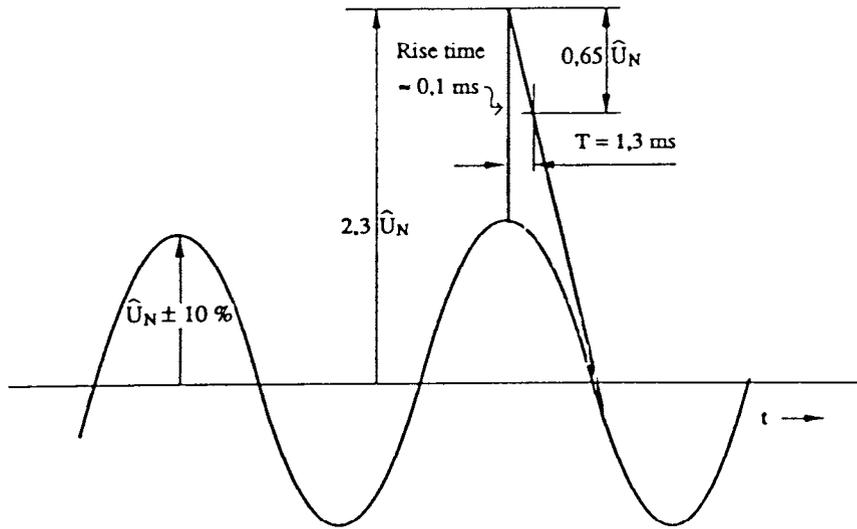


Figure 1. High-energy waveform specification (From Ref. [3])

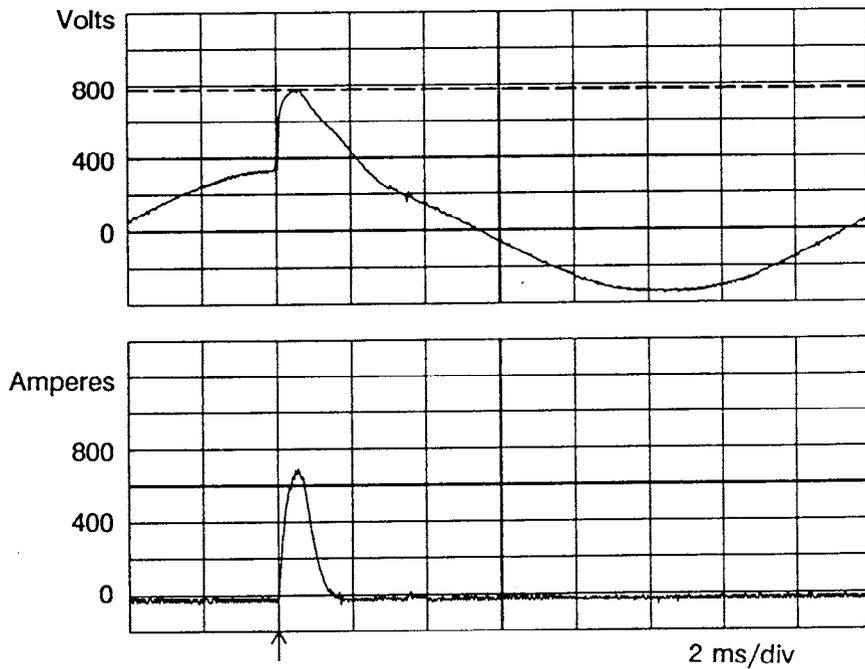


Figure 2. Voltage across and current through EUT #1

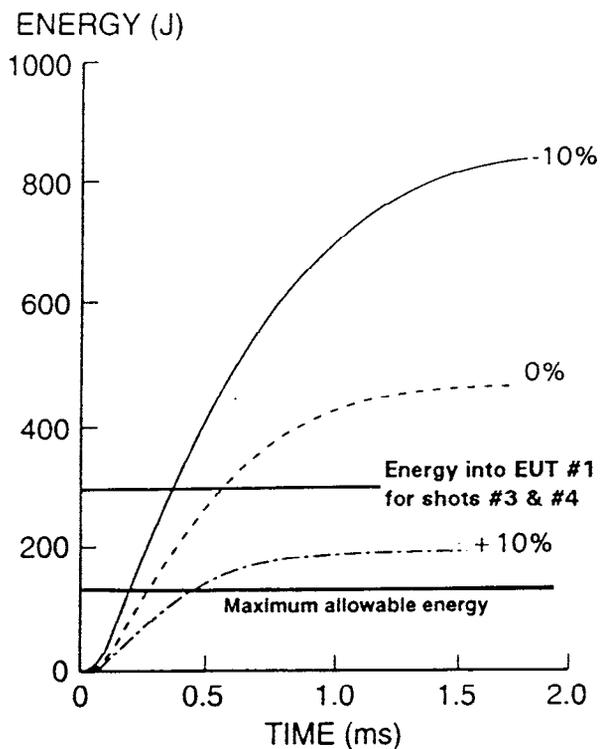


Figure 3.

Energy deposited in varistor as a function of tolerance of device compared to nominal value
(From Ref. [5])

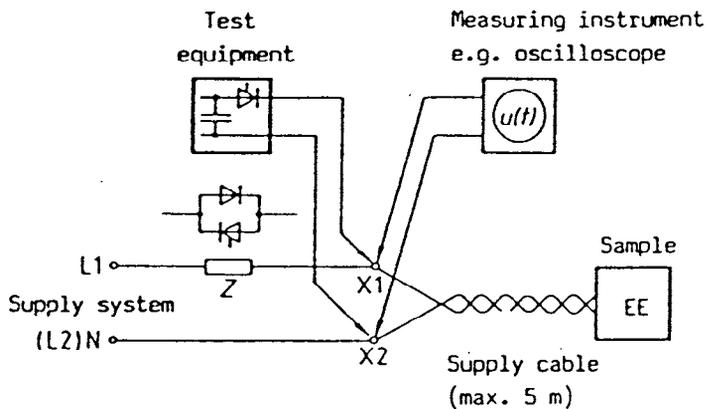


Figure 4. Elementary test circuit diagram (From Ref. [2])

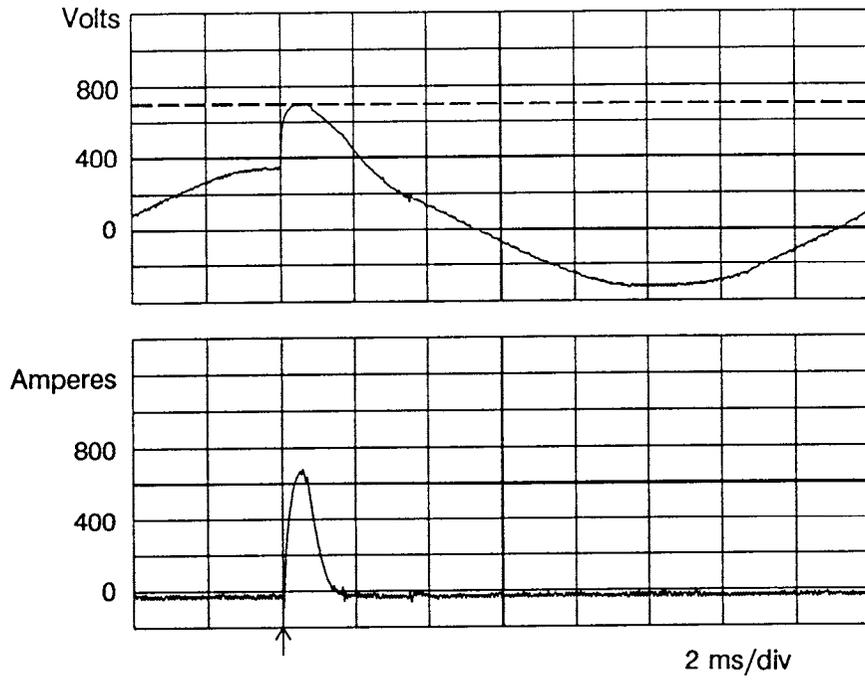


Figure 5. Voltage across and current through varistor only

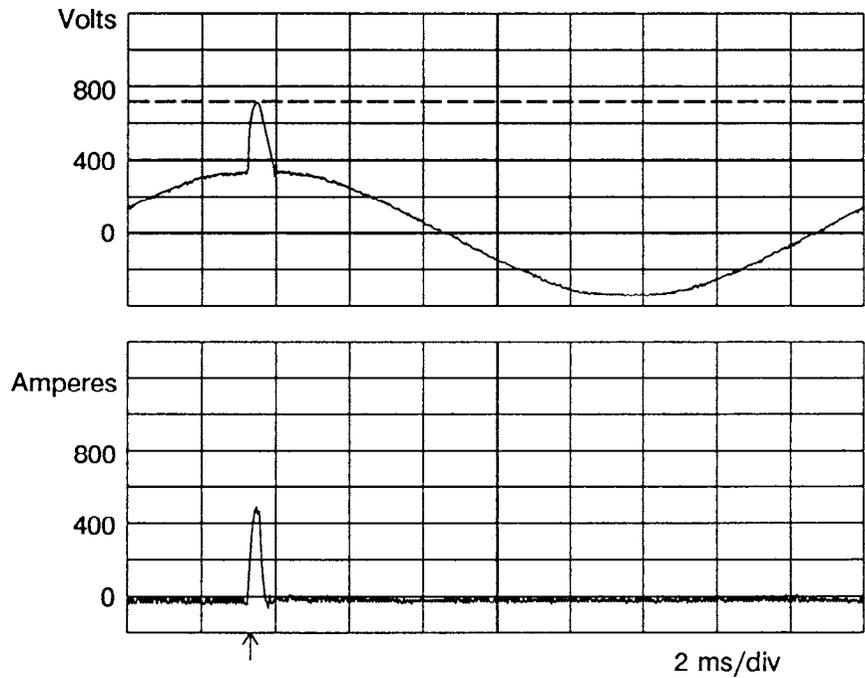


Figure 6. Voltage across and current through varistor with reduced stress

**VALIDATING
SURGE TEST
STANDARDS
BY FIELD
EXPERIENCE:**

**HIGH-ENERGY
TESTS AND
VARISTOR
PERFORMANCE**

Validating Surge Test Standards by Field Experience: High-Energy Tests and Varistor Performance

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Significance:

Part 2 Development of standards – Reality checks

Demonstration ad absurdum:

Accepting the premise of proposed IEC 100/1300 high-energy surges being representative of the environment, and modeling the response of typical metal-oxide varistors, leads to the conclusion that most of the billions of varistors in service should fail at alarming rates – but we know they do not. Ergo, the premise is not valid and the proposed high-energy test should not be considered as an across-the-board requirement. (See also paper “MOV - VDE” in this Part 2 for an experimental demonstration.)

On the other hand, the tests proposed in IEEE Std C62.41 would not result in systematic failure of commonly used varistors and consequently appear more realistic

Validating Surge Test Standards by Field Experience: High-Energy Tests and Varistor Performance

Charles Fenimore, *Member, IEEE*, and François D. Martzloff, *Fellow, IEEE*

Abstract—New, high-energy surge tests are emerging in IEEE and IEC standards. Field experience offers a valuable criterion for validating or invalidating proposed standards. A proposal under consideration by the IEC involves so much energy that a varistor of the voltage rating commonly used in protecting load equipment, if subjected to this test, would almost certainly fail. Yet, reported varistor failure rates do not reflect such a situation. Thus, a reexamination of the premises that led to the proposed test specifications appears necessary. Proposals for high-energy tests as additional waveforms in the new version of IEEE C62.41, on the other hand, lead to current and energy levels that do not place typical varistors in immediate jeopardy. Thus, they appear more consistent with field experience.

INTRODUCTION

A NATURAL approach in defining the surge tests to be performed on any equipment is to attempt duplicating the conditions observed in site measurements. However, this approach would lead to a situation where general conclusions are drawn from limited measurements of specific surge occurrences. It has, in fact, led to a multitude of proposals for test standards that may subsequently be applied outside of their original, correct context because no other standard is available at the time. An example of this situation may be developing with the proposal by Technical Committee 77 of the International Electrotechnical Commission (IEC) for a high-energy 100/1300- μ s surge test.

To evaluate the effects of various proposed or existing high-energy stress tests on commonly used varistors, this paper presents a simple yet effective model of a surge generator. The evaluation proceeds by quantifying the current through the varistor and the corresponding energy deposited in the varistor. The computed results are compared with the published device ratings to predict the likelihood of failure. This likelihood is then compared

with the available information from field experience on failure rates.

Any immunity test should be conducted with an objective that is more subtle than the goal to "duplicate the environment." A test stress is applied to a device not to demonstrate that it can survive any of the stresses that it will encounter in nature but only to demonstrate for the benefit of both manufacturer and purchaser that the device can survive an agreed-on, simple, and reproducible stress. From surviving the test stress, the inference is made, *subject to confirmation by field experience*, that the device does have the ability to survive the infinite variety of stresses that it will encounter during its life in the real world. In other words, simple test stresses are useful because they can be reproduced over a period of time at the same facility and between facilities, providing a common language and a standard of comparison that is essential to conduct orderly transactions. Test standards should not, however, be misconstrued as representing natural phenomena. They are effective only if they discriminate between those devices with a potential for long field survival and those that are likely to fail.

The proposed 100/1300- μ s IEC test should be reexamined with this philosophy in mind because it appears that commonly used varistors would be expected to fail when subjected to this test. Anecdotal experience does not support the prediction of failure in the field, raising questions as to the general validity of this test. On the other hand, high-energy tests derived from new proposals contained in the revised version of IEEE Std. C62.41 do not lead to contradiction between field experience and predicted test results.

PROPOSED IEEE AND IEC HIGH-ENERGY TESTS

Metal-oxide varistors that suppress surges by absorbing energy have proliferated in low-voltage ac power circuits. Consequently, new high-energy tests have been proposed to assess the ability of these varistors to withstand the corresponding stress. In a major revision of the IEEE Guide C62.41 [1] (emerging as a Recommended Practice [2]), an additional waveform has been proposed to assess this ability. The proposal is a 10/1000- μ s surge, with three "system exposure" levels, which are defined below. The IEC Technical Committee TC77 is considering a

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surge test requirement based on the scenario of current-limiting fuses clearing a fault at the end of a cable, where the energy trapped in the system inductance causes a large transient at the time the fuse interrupts the current [3]. That scenario was first described and quantified by Meissen [4] and incorporated in German Standard VDE 0160 [5].

The new C62.41 Recommended Practice proposes, among other waveforms, a high-energy stress defined by an open-circuit voltage and a source impedance at three "system exposure" levels. For the "low exposure" level, no high-energy stress is proposed; for the "medium exposure" level, the surge environment involves a crest of two times the system peak voltage with a source impedance of 1 Ω . For the "high exposure" level, the crest is 2.3 times the system peak voltage, whereas the source impedance is only 0.25 Ω .

The IEC proposal appears to be based on the VDE 0160 standard, which specifies the direct discharge of a large capacitor—thousands of microfarads—into the equipment under test (EUT). The VDE test procedures are not quite clearly outlined at this point but might be interpreted as readjusting the capacitor charging voltage after connecting the EUT to the surge generator in order to maintain the specified test voltage across the EUT. That approach would be diametrically opposed to the generally accepted practice of performing a surge test with a generator having the capability of delivering a well-defined open-circuit voltage and short-circuit current or an open-circuit voltage associated with a specified source impedance (see Fisher and Martzloff [6] and the *IEEE Guide on Surge Testing* [7]).

Another ambiguity in the VDE 0160 test specification is that it might be acceptable to perform a test where the voltage waveform is less than the specification, provided that 80% of the energy stored in the surge generator capacitor is delivered to the EUT. However, there is no provision in the test procedure for measuring this energy, and it is doubtful that this condition can be achieved with a surge generator containing the parallel resistor that is necessary to achieve the specified rate of decay (or duration of the tail of the wave) when the EUT offers a high impedance.

Metal-oxide varistors offered by manufacturers include ratings of 130 V rms for applications in 120-V systems and 250 V rms for application in 220-V systems. The motivation for using these varistor ratings is, of course, the desire to provide the lowest possible clamping voltage to protect sensitive equipment. A paper presented at the Zürich EMC Symposium suggests that premature varistor aging may result from this close clamping (see Martzloff and Leedy [8]). However, the 130- and 250-V varistor ratings are still widely used by equipment manufacturers who take the position that they are not afflicted by unacceptable failure rates. Thus, the authors accept that position as reflecting actual field experience and will apply it as a criterion for validating or questioning the proposed high-energy test standards.

SUMMARY OF RESULTS

This paper reports the results of modeling the application of a surge test to a family of commonly used varistor sizes (14, 20 and 32 mm in diameter). For each varistor size, the computations were performed for three levels of manufacturing tolerances on the varistor: nominal value, -10% , and $+10\%$. A varistor with its clamping voltage at the maximum acceptable tolerance level (the level shown on published $I - V$ curves) will tend to absorb less energy than a varistor with a lower clamping voltage because it will divert current for a smaller part of the surge. The maximum energy deposition in the varistor will occur for a varistor having the lowest acceptable clamping voltage (typically 20% below the maximum), as indicated by the $\pm 10\%$ tolerance on varistor nominal voltages. Should the test generator parameters be at the most severe conditions within its uncertainties (higher peak voltage and longer duration than nominal within allowable tolerances), the stress on the varistor would be even greater.

The circuit model used in the computations reported in this paper is a simple capacitor-discharge circuit that can produce the 10/1000- μs waveform of C62.41 or the 100/1300- μs waveform of VDE 0160, where each has the appropriate selection of the components values. The modeling results, which are discussed in detail below with supporting information in the Appendix, indicate that the smaller size varistors would not be damaged at the "medium exposure" level of C62.41 but would be damaged at the "high exposure" level. The 32-mm varistor would easily accept several applications of the "high exposure" level, whereas the 20-mm varistor would have a limited life. On the other hand, few varistors will survive the VDE 0160 stress.

Table I presents this information in the form of the number of surges that a varistor can survive for the three sizes and three tolerance values of varistors and for the three type of tests: VDE 0160, C62.41 "high exposure," and C62.41 "medium exposure." The results with C62.41 are in good agreement with anecdotal (unpublished) field experience, that is, 14-mm varistors installed at the service entrance are often in jeopardy, 20-mm varistors have a better chance, and 32-mm varistors are generally successful. Failure rates are not reported formally in the literature, but anecdotal information does circulate. The response of industry to the Zürich paper alerting the community to the risk of premature aging caused by repeated swells [8] was that 20- and 32-mm varistors do not suffer from an unacceptable or alarming failure rate.

The predicted survival rates of Table I appear to be consistent with actual field experience, thus validating the stress levels proposed by IEEE C62.41. In contrast, for the VDE 0160 stress, the predicted survival rate is so low that a conclusion appears inescapable: The VDE 0160 stress involves an exceptionally high energy level, making the application of the test questionable if interpreted as a general requirement. The authors do not question the scenario leading to this stress level but do question the

TABLE I
PREDICTED NUMBER OF HIGH-ENERGY SURGES THAT A VARISTOR
CAN SURVIVE AS A FUNCTION OF SIZE AND CLAMPING
VOLTAGE TOLERANCE

Size mm	Varistor Clamping Voltage Tolerance	Predicted Number of Surges Survived		
		VDE 0160 Class 2	C62.41 High	C62.41 Medium
14	-10%	none	none	80
	0%	none	1	3000
	+10%	none	8	> 10 ⁶
20	-10%	none	1	500
	0%	none	3	8000
	+10%	1	20	"indefinite"
32	-10%	none	8	20 000
	0%	1	80	200 000
	+10%	5	800	"indefinite"

IEC proposal to require an across-the-board test at that level for all equipment.

The dramatic effect of the tolerance value on survival rate is also apparent. Greater reliability can be achieved if users would accept—better yet, request—a slightly higher clamping voltage than the lowest clamping voltage offered by the manufacturers of varistors and by the manufacturers of packaged suppressors.

MODELING A SURGE TEST

The normal practice in surge testing of low-voltage equipment, as described in the *IEEE Guide on Surge Testing* [7], is to specify an open-circuit voltage and a short-circuit current to be delivered by the surge generator. With these two parameters specified, the surge generator is considered to be defined for any test involving a specimen of high impedance (typically insulation) or low impedance (typically a surge diverter). For the unidirectional surges of 10/1000 and 100/1300 μ s, a simple four-component model circuit can produce these waveforms. An actual surge generator, of course, requires careful attention to avoid problems of parasitic impedances, but the simple circuit model of Fig. 1 can deliver the required waveforms, as shown in Fig. 2 for the case of the nominal C62.41 10/1000- μ s waveform.

In the specification of that waveform, the tolerances allowed by C62.41 recognize the fact that the open-circuit voltage will inescapably have a longer duration but shorter rise time than the short-circuit current. Because the high-energy aspect of this test makes the current waveform the most significant parameter, the values of the components in the model were selected to most closely approximate the nominal 10/1000 μ s for the short-circuit current while allowing the open-circuit voltage to go to the longest duration permitted by the tolerances. For the VDE 0160 model, the values of the components were selected to comply with the 6000- μ F requirement while producing the specified open-circuit voltage.

In predicting varistor failure rates, the model can take into consideration the possible combinations of manufacturing tolerances on the varistors and the uncertainties of the test (which is something that is more difficult to do by

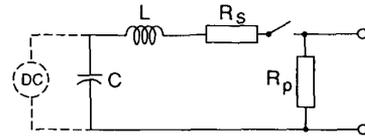


Fig. 1. Four-component circuit for 10/1000- and 100/1300- μ s surge modeling.

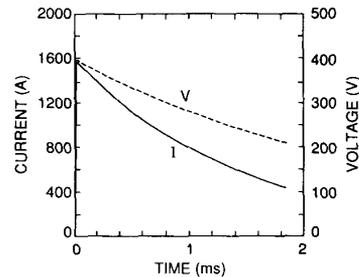


Fig. 2. Open-circuit voltage V and short-circuit current I produced by circuit model with parameters set for the C62.41 10/1000- μ s waveform.

tests on random samples). In the simple computations reported here, three cases have been computed with the varistor at the midpoint and the two extremes of its manufacturing tolerance. The surge generator parameters were set to produce the nominal current waveform in order to make a midrange rather than a worst-case prediction. The conclusions on survival rates and validation of the proposed tests presented above would not be dramatically affected if the surge generator parameter tolerances were included in the computation.

The component values of the circuit shown in Fig. 1 may be selected to generate the desired waveforms of the various standards. The selection method is described below. In order to determine the response of the circuit with the nonlinear varistor, numerical techniques are used as shown in the second step below.

In the circuit of Fig. 1, the capacitance C is charged to an initial voltage V_c . The surge generator has a series resistance R_s and a parallel resistance R_p . A small inductance L is tuned to provide the specified rise time. This simple LRC circuit is described by a characteristic equation

$$L\lambda^2 + R\lambda + \frac{1}{C} = 0$$

where R is defined below. The two decay constants are

$$\lambda^{\pm} = -\frac{R}{2L} \pm \sqrt{\left(\frac{R}{2L}\right)^2 - \frac{1}{LC}}$$

The response of the circuit is a "double exponential" waveform [9].

Using the allowed tolerances of C62.41 for the model, the waveshape of the short-circuit current (in which case R is R_s) was set at 10/1000 μ s. For the open-circuit voltage (in which case R is $R_s + R_p$), the maximum

duration allowed by C62.41 is 2000 μ s. The decay times, which are expressed as full width at half maximum (FWHM) for these two waveforms are designated as t_I and t_V , respectively. The effective source impedance is defined in C62.41 as the ratio of the peak open-circuit voltage V_p to the peak short-circuit current I_p . Its value $Z = V_p/I_p$ has the dimension of an impedance.

Because the time constants are widely separated, the determination of the circuit component values from the values of t_I , t_V , Z , and V_p can be simplified to produce approximate values. The characteristic decay values λ^+ and λ^- are given by

$$\lambda^+ \cong -\frac{1}{RC}$$

$$\lambda^- \cong -\frac{R}{L}$$

In particular, for long times t , the short circuit current I_{sc} and open-circuit voltage V_{oc} , are given by

$$I_{sc}(t) \cong I_p e^{-t/R_s C}$$

$$V_{oc}(t) \cong V_p e^{-t/(R_s + R_p)C}$$

At half maximum, one has

$$t_I = \log 2 \cdot R_s \cdot C$$

and

$$t_V = \log 2 \cdot (R_s + R_p) \cdot C.$$

With a small value of the inductance

$$V_p \cong V_c \frac{R_p}{R_s + R_p}$$

and

$$\frac{1}{R_s} + \frac{1}{R_p} = \frac{1}{Z}.$$

These relations lead to the four equations:

$$R_p = \frac{t_V}{t_I} \cdot Z \quad (1)$$

$$R_s = \frac{t_V}{t_V - t_I} \cdot Z \quad (2)$$

$$C = \frac{t_I}{\log 2 \cdot R_s} \quad (3)$$

$$V_c = \frac{t_V}{t_V - t_I} \cdot V_p. \quad (4)$$

The inductance is determined by considering the 10–90% rise time t_R . The widely separated time constants allow the fast component of the current to be estimated by

$$I_{sc} \cong I_p e^{-Rt/L}$$

at short times t .

Applying a logarithm yields

$$L \cong \frac{R \cdot t_R}{\log .9 - \log .1} = \frac{R \cdot t_R}{\log 9}. \quad (5)$$

The expressions (1)–(5) uniquely define the characteristics of the circuit for given values of the time constants, the source impedance, and the peak open-circuit voltage.

With the parameters of the model test circuit thus defined, the solution of the response of the current and energy in the varistor is obtained numerically using the ordinary differential equation package PLOD [10]. The varistor is presumed to contain an internal series resistance R_m and have the I - V relationship

$$V_m = \left(\frac{I_m}{k} \right)^{1/\alpha} + R_m I_m. \quad (6)$$

The first-order system of equations to be solved is given by the definition of the capacitor current I and by Ohm's law

$$\frac{dQ}{dt} = -I \quad (7)$$

$$L \frac{dI}{dt} = \frac{Q}{C} - R_s I - V_m. \quad (8)$$

The varistor current I_m and I are related by (6) and by

$$I = I_m + \frac{V_m}{R_p}. \quad (9)$$

By exploiting this relationship, a direct numerical solution for the varistor current is possible. In addition, the energy in the varistor E_m is found by integrating

$$\frac{dE_m}{dt} = I_m \cdot V_m. \quad (10)$$

The initial charge is given by $C \cdot V_c$, and the initial current and energy in the varistor are zero. The computations were performed for the two C62.41 exposure levels and for the maximum VDE 0160 stress, as described below.

MODELING RESULTS

C62.41—10/1000- μ s Stresses

To evaluate the effects of the test on varistors, a simple model of an equivalent circuit of the varistor is connected to the terminals of the model generator. The charging voltage of the generator is, of course, left unchanged. For the range of frequencies involved in these waveforms, the only two significant elements of the varistor equivalent circuit (Fig. 3) are the pure varistor R_x , ($I = kV^\alpha$) and the series resistance R_{ON} , R_s in the model. The parallel resistance R_{OFF} , capacitance C , and the series inductance

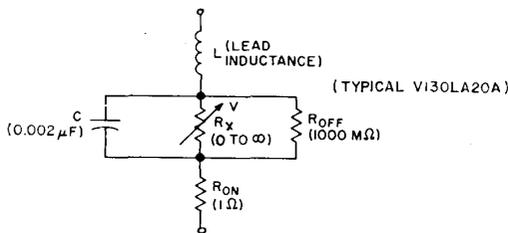


Fig. 3. Equivalent circuit of a varistor (Source: [14]).

L of the complete equivalent circuit can be neglected. Three diameters of 130-V rated varistors were considered, each with its characteristic clamping at -10 , 0 , and $+10\%$ of the nominal value published by one manufacturer. Fig. 4 shows the type of plots obtained from the model where the current through the varistor and the cumulative energy deposited in the varistor are computed as a function of time. Showing the complete set of results for all combinations would require excessive space; a summary of the results is presented in the Appendix. In the typical example of Fig. 5, three curves show the cumulative energy for a 14-mm varistor with nominal rating of 130 V rms and three tolerance values -10 , 0 , and $+10\%$ clamping voltage when exposed to the C62.41 "high exposure" stress level.

VDE 0160 100/1300- μ s Stress

Fig. 6 shows the parameters of the 100/1300- μ s surge, Class 2 described in the most recent amendment to VDE 0160 [11] and in the IEC proposal [3]. The voltage level is specified as 2.3 times the peak of the ac power system voltage. (The amendment also cites a Class 1 category with a level of only 2.0 times the peak of the ac power system voltage and a shorter duration). Accepting for the moment the premises that led to the specification of this test, the authors applied the same circuit model used for the IEEE waveforms to produce the specified VDE waveform with an energy storage capacitor having the value specified in the latest amendment to VDE 0160. (Earlier versions of the VDE 0160 standard suggested a 25 000- μ F capacitor. In the amendment, this value has been scaled down to a range of 700 to 6000 μ F, perhaps implying that the issue is still unsettled, and thus, the IEC proposal is still open to feedback from users.)

In this case, because the VDE places emphasis on maintaining the voltage waveform, the model parameters were set to obtain an open-circuit voltage close to the 100/1300- μ s values, with the exception of the resulting short-circuit current, for which VDE 0160 does not specify a value. Fig. 7 shows the open-circuit voltage and short-circuit current computed by the model.

The computations were performed for the 250-V rms rating because the VDE 0160 does not provide specifications for system voltages of less than 220 V rms. Details of the results are presented in the Appendix together with the corresponding results from the C62.41 stress levels.

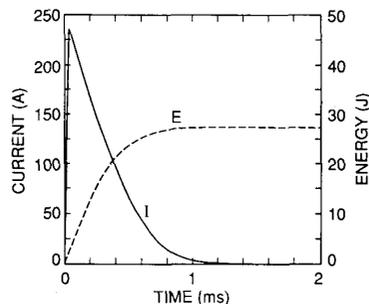


Fig. 4. Energy deposition E and current I in a 20-mm varistor with nominal clamping characteristic (0% tolerance) during the "High Exposure" 10/1000- μ s C62.41 surge.

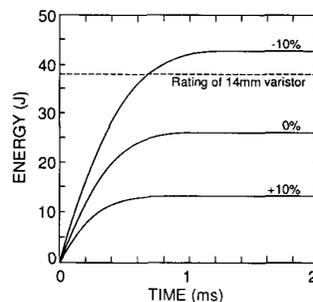


Fig. 5. Energy deposition in 14-mm varistors at -10 , 0 , and $+10\%$ values of clamping characteristics during a "high exposure" 10/1000- μ s C62.41 surge.

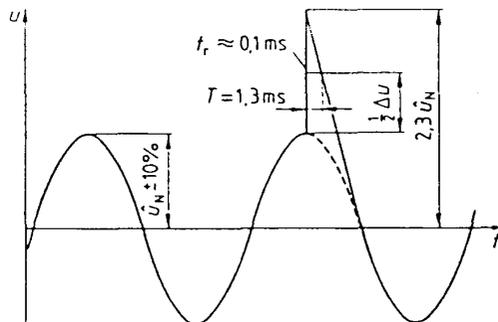


Fig. 6. Voltage waveform of the 100/1300- μ s surge specified by VDE 0160 and proposed by IEC (Source: [5]).

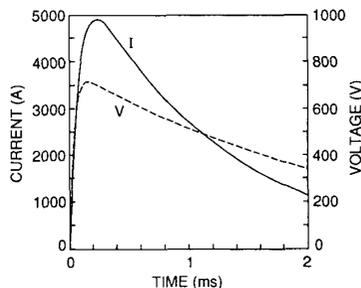


Fig. 7. Open-circuit voltage V and short-circuit current I produced by model with parameters set to approximate the VDE 0160 voltage waveform.

COMPARISONS OF RESULTS FROM MODEL WITH VARISTOR RATINGS

Typical manufacturer specifications [12] include a joules rating for maximum single pulses; however, industry standards (Section 6, *IEEE Standard on Varistor Test Specifications* [13]) raise some questions on the application of such a simple criterion.

The cumulative energy levels for the three varistor sizes (each at three tolerance levels) were computed with the model for the C62.41 and VDE 0160 stress levels. The results are shown in Table II, together with the typical, single-pulse joule rating published for these sizes. By using this somewhat oversimplified joule criterion (more than 10% change in nominal voltage may occur if joule rating is exceeded), it would appear that only the 14- and 20-mm varistor, for the low values of tolerance, might be in jeopardy.

Using the criterion of "pulse rating" proposed by manufacturers [11], [14], where the current peak and duration are taken into consideration leads to more detailed and reliable conclusions, which also agree with field experience (see Martzloff [15]).

Therefore, the current peak and its duration (FWHM) were also computed for the nine combinations of varistor parameters and compared with the "pulse rating" corresponding to the duration and peak in each case. The detailed results, which are the basis for the summary of Table I, are presented in tabular fashion in the Appendix, together with a discussion of the finer points of the analysis.

CONCLUSIONS

1) Predictions of the impact of the 100/1300- μ s surge test proposed by the IEC and based on the VDE 0160 standard show that the millions of varistors in service should experience a greater failure rate than that indicated by available information on actual field failures. This inconsistency raises serious questions on the proposed requirement of such a severe test to a wide range of equipment.

Furthermore, the lingering ambiguity in the VDE 0160 standard (and, consequently, in the IEC proposal) on whether to set constant open-circuit voltage or to adjust the voltage while the specimen is connected needs to be clarified. A constant, specified open-circuit voltage combined with a well-defined source impedance is the generally accepted practice in surge testing.

2) The energy levels and currents resulting from application of a waveform described in the revised IEEE C62.41, on the other hand, range from benign for typical large varistors to severe for small varistors. Thus, this set of stress levels appears to be more consistent with field experience, at least as inferred from available anecdotal information.

3) Although the authors do not question the validity of the fuse-blowing scenario, which is the basis for the VDE 0160 and proposed IEC test, they recommend a critical

TABLE II
SINGLE-PULSE RESULTS AND RATINGS (IN JOULES)

Varistor Size	Tolerance %	VDE 0160 Class 2 250 V varistor		C62.41 High 130 V varistor		C62.41 Medium 130 V varistor	
		Result	Rating	Result	Rating	Result	Rating
14	-10	212		43		6	
	0	126	72	26	38	3	38
	+10	62		13		1	
20	-10	257		45		6	
	0	181	130	27	70	3	70
	+10	86		14		1	
32	-10	306		46		6	
	0	181	330	28	200	3	200
	+10	86		15		1	

NOTES:

- Five numbers are printed in bold face in the results columns for two values of tolerances in the 14-mm and 20-mm varistors. These values exceed the rating of the varistor, and thus would indicate a high likelihood of failure at that stress level.
- The varistor model postulates the same $I = kV^n$ relation for the three ratings, with a series resistance that decreases as the diameter of the varistor increases. The lower series resistance invites a greater current diversion into the varistor in the upturn region of the $I-V$ characteristic, where its effect is more noticeable, especially for the VDE 0160 and the lower tolerance case for the varistors.

review of the statistics of the occurrence of fuse blowing, the use of varistors with low clamping voltage, and the distribution of actual clamping voltage within manufacturing tolerances. They also urge all users to share information on the observed failure rates and thus attain a broader perspective on these issues.

APPENDIX

DETAILED RESULTS FOR CURRENT PEAKS AND DURATION

This Appendix provides a summary of the 54 separate computations made to determine the current in the varistor resulting from the three high-energy tests discussed in the paper. Three varistor sizes were considered (14, 20, and 32 mm), and their "pulse rating" obtained from [12] and [14]. The 32-mm size has been dropped from the current product line of [12] and might appear obsolete. However, it was selected because it has been applied in the past [14], and thus, more field experience is available for that size than for the 40-mm size, which is the present offering.

The computed results are presented in Table AI for the VDE 0160 Class 2 and the C62.41 "high exposure" and "medium exposure" stresses. In each major section of the table, the computed current peak and FWHM are tabulated. Next to these computations, the corresponding current peaks are shown from the "pulse ratings" in [12] or [14] for the computed duration and for 1, 10, and 100 applications of that peak of current pulse.

The usual description of a unidirectional surge is based on the FWHM, and therefore, the computations of the current in the varistor were aimed at characterizing this description of the current waveform. However, the "pulse rating" curves in both [12] and [14] are based on an "impulse duration" defined as the time from virtual origin of the wave and the virtual time to half value. In the case of the C62.41, with a front time of 10 μ s and a FWHM of 1000 μ s, the difference between the FWHM and the "impulse duration" is negligible. In the case of the IEC 100/1300- μ s waveform, the difference is more significant,

TABLE AI
MODELING RESULTS VERSUS DEVICE RATINGS—CURRENT AND DURATION

VARISTOR		VDE 0160 $\approx 100/1300 \mu\text{s}$ $2.3 \times 220 \times 1.4; 6000 \mu\text{F};$ 250-V varistor						IEEE C62.41 10/1000 μs "High" $2.3 \times 120 \times 1.4; 0.25 \Omega;$ 130-V varistor						IEEE C62.41 10/1000 μs "Medium" $2.0 \times 120 \times 1.4; 1.0 \Omega;$ 130-V varistor					
DIA mm	Tolerance %	RESULTS		RATINGS				RESULTS		RATINGS				RESULTS		RATINGS			
		Peak A	FWHM μs	Virt. Dur. μs^*	Allowable Peak Amperes**			Peak A	FWHM μs	Virt. Dur. μs^*	Allowable Peak Amperes**			Peak A	FWHM μs	Virt. Dur. μs^*	Allowable Peak Amperes**		
					For Virtual Duration and No. of Pulses in Columns						For FWHM and Number of Pulses in Columns						For FWHM and Number of Pulses in Columns		
100	10	1	100	10	1	100	10	1	100	10	1	100	10	1					
14	-10	520	650	690	30	50	120	305	400	50	75	200	58	322	55	90	240		
	0	364	520	560	40	60	150	220	325	55	90	240	35	235	60	110	310		
	+10	221	400	440	45	70	180	140	240	60	110	310	116	153	100	160	500		
20	-10	658	625	665	45	80	210	325	410	70	120	360	60	325	90	170	410		
	0	454	515	555	55	90	250	235	325	90	170	410	36	234	100	200	550		
	+10	269	400	440	65	110	300	150	240	100	200	550	16	151	150	300	750		
32	-10	826	625	665	100	200	800	345	410	180	340	1000	60	313	200	400	1200		
	0	562	500	540	120	250	700	250	320	200	400	1200	36	230	220	500	1700		
	+10	325	390	420	150	300	900	155	240	220	500	1700	16	150	400	850	2500		

*Adjustment of approximately half of the rise time made to account for the difference between the computed FWHM and the "virtual duration" used in manufacturers specifications. For the short rise time of the C62.41, the difference is negligible.

**When allowable peak current for the corresponding duration and number of pulses exceeds the rated peak current at that duration, the varistor is deemed in jeopardy; this situation is shown by shading the corresponding area in the rating columns. The unshaded areas represent "survival" of the varistor through the high-energy stress.

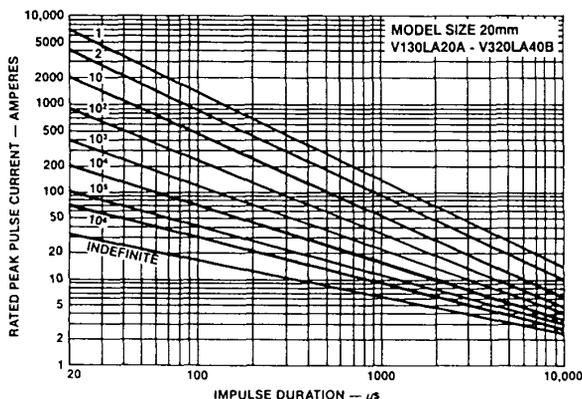


Fig. AI. Typical published family of "pulse rating" curves showing amplitude, duration, and number of allowable pulses.

and therefore, the comparisons of Table AI include a 40- μs adjustment in the duration (about half of the rise time).

The peak values of the current shown in the table that exceed the "pulse rating" have been identified by shading the area in the columns. At a glance, it becomes apparent that the survival rate to a VDE 0160 exposure can be expected to be extremely low; it will be moderate for the C62.41 "high exposure" and will be at its maximum for the C62.41 "medium exposure" stresses.

ACKNOWLEDGMENT

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He is contributing to the work of several IEEE committees, in particular, the revision of ANSI/IEEE C62.41, *Guide on Surge Voltages*, into the Recommended Practice as discussed in the paper, and an update of C62.45, *Guide on Surge Testing*. He is serving as vicechair of the IAS IEEE Standards Coordinating Committee on Power Quality. In the IEC Technical Committee 77 on Electromagnetic Compatibility, he is serving as chair of SC77B (industrial systems) and secretary of WG2B (installation and mitigation).

**APPLYING
REALITY
CHECKS
TO
STANDARDS
ON THE SURGE
ENVIRONMENT**

Applying Reality Checks to Standards on the Surge Environment

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Significance:

Part 2 Development of Standards – Reality checks

Three examples of reality checks are given that shed some light on issues raised during standards development

- The apparent reduction in surge voltage activity is explained by the proliferation of surge mitigating devices.
- A proposed high-energy surge would cause failure of ubiquitous SPDs, but they do not fail in the field.
- Allegedly frequent high-level surges would cause frequent failure of light bulbs, but they do not in the field

APPLYING REALITY CHECKS TO STANDARDS ON THE SURGE ENVIRONMENT

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Abstract - The paper identifies several realities of surge environment and equipment survival that are sometimes ignored in surge-protection practices. It questions the quest for what could be overly conservative requirements for surge immunity or surge mitigation by presenting "reality checks" based on field experience or laboratory data. A first check focuses on the fact that some recent field recordings of surges may be misleadingly low in today's surge environment. Additional checks, aimed at moderating the overly conservative requirements, include the case history of a proposed high-stress 100/1300 μ s surge test, data on failure levels of clock motors and light bulbs that can serve as benchmarks for severity levels, and measurements, validated by parametric modeling, showing that large currents cannot propagate into long cables without causing a flashover of the wiring devices at the beginning of the cable, effectively limiting the energy-delivery capability of a surge at the end of the cable.

1. Introduction

Some proposals for standards on equipment surge immunity or on performance of surge-protective devices are driven by the commendable quest for conservative ratings. However, when this quest produces compounded safety factors, the result may not be cost-effective.

The purpose of our paper is to present facts and rationalizations in support of the development of realistic standards on the surge environment in low-voltage ac power systems. In addition to contributors to the ICLP Conferences, two major players in the development of international standards on the lightning surge environment are IEC Technical Committee 77 on Electromagnetic Compatibility and Technical Committee 81 on Lightning Protection. Other contributors addressing switching surges as well as lightning surges include IEC Subcommittee 28A on Insulation Coordination, Subcommittee 37A on Surge-Protective Devices, and the IEEE Committee on Surge-Protective Devices. The standards developed by these bodies reflect the collective experience of individual contributors as well as the "corporate memory" of these groups. In this paper, four items are described that may serve as foundations, or reality checks, to be added to this corporate memory. The first check can be seen as a reminder note to resist the mistake of dismissing surge threats because contemporary recordings show benign occurrences of surge voltages. The three other checks can serve as a tempering note, to avoid economically unjustified over-specification of surge protection.

2. The ninety-five percent rule

Debates and definitions concerning compatibility levels, immunity limits, or emissions limits generally acknowledge that requirements drawn from these considerations cannot apply simultaneously to 100% of the cases and 100% of the time. The term "high probability" appears in many definitions, rather than a hard number. This avoidance of hard numbers is the result of the difficulty to secure a consensus from individuals who represent different communities of end-users with different perceptions of what is an acceptable level of probability.

The military and aerospace communities often speak of zero tolerance or zero defects where economics is downplayed. In contrast the commercial, mass-production communities speak of realistic requirements where cost-effectiveness is a very prominent factor.

An intermediate position can be found in industry where a rational balance is sought between the costs of exhaustive mitigation and the consequential costs of limited mitigation.

Our reality checks are offered as a guide to striking such a balance, recognizing the diversity of circumstances and expectations from a diversity of end-users.

3. Shrinking surges vs. expanding mitigation

A reality check can explain an apparently puzzling development which has been observed since the beginning of studies concerning the characterization of the surge environment: starting in the sixties, results published by researchers monitoring the occurrence of surges have reported a gradual decrease in the relative severity of surges (Martzloff & Hahn, 1970 [1]; Allen & Segall, 1974 [2]; Bull & Nethercott, 1975 [3]; Goldstein and Speranza, 1982 [4]; Goedbloed, 1987 [5]; Dorr, 1995 [6]).

This puzzling trend can be explained by reference to the reality: the spectacular expansion of surge-protective devices (SPDs) in low-voltage ac power circuits now makes it almost impossible to make measurements at a site where there is not some hidden SPD. Monitors will now report the residual voltage of these SPDs, not the true surge activity in the environment (Aspnes et al. and discussion, 1985 [7]). So, it is not that surges have shrunk, but rather that mitigation has greatly expanded, masking the persistent occurrence of transients which remain a potential threat. This threat has shifted from observed surge voltages -- now mitigated -- to the possibility of excessive surge currents into candidate mitigation means with low limiting voltage which would be particularly attractive to the surges.

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Future surveys of the occurrence of surges in low-voltage ac power systems should shift from the recording of surge voltages to the recording of surge currents [8]. Unless this shift is implemented, the false sense of security will be further promoted that potentially damaging surges are less frequent than once believed. The 1980 title of a seminal IEEE document on the occurrence of surges (Guide on Surge Voltages in Low-Voltage AC Power Circuits [9]) was appropriate at the time, but should now be amended to reflect the shift.

4. The saga of the proposed 100/1300 μ s surge

A proposal was made in the early eighties to require a high-stress surge test for industrial equipment. A reality check would have revealed the contradiction between the failure rates that would result from such a test and the observed field failures.

Starting with observations of the occurrence of long duration surges, typically associated with fuse blowing, the proposal was to require an additional test with a surge longer than the classic 1,2/50 - 8/20 μ s impulse (Meissen, 1983 [10]; VDE 0160, 1989, [11]; IEC 1000-4-1, 1990 [12]). The proposed surge would be characterized by a 100/1300 μ s waveform, with peaks as high as 1,3 per unit, added to the ac sine wave peak. In the original VDE 0160 implementation, the test circuit involved the discharge of a 24000 μ F energy-storage capacitor. An amendment to the VDE 0160 standard [13] scaled the capacitance value down to 6000 μ F, still a large stress for the equipment under test.

For instance, such surges would impose large stresses to the millions (or perhaps billions) of small varistors now installed in low-voltage ac power systems. One would observe a conspicuous failure rate in the field as typical 20-mm diameter varistors cannot survive such surges, but one does not observe this failure rate. An objection to this surge specification was first proposed on the basis of computer simulation of the event (Fenimore & Martzloff, 1990 [14]) because no generator was commercially available to produce that surge. Table 1 shows an excerpt from [14] where the current that would be caused by the proposed 100/1300 μ s surge in a 20-mm varistor rated 250 V rms was computed and compared to the published varistor pulse rating for current-handling capability. Only a varistor at the high end of its $\pm 10\%$ tolerance band could survive one application of that surge (the corresponding computed current peak is only 269 A, compared to a rating of 300 A). In all the other cases, the varistor rating is exceeded and likelihood of failure increases for lower tolerances and for more than one pulse.

Later on, when a prototype generator capable of delivering this surge was developed by a surge generator manufacturer, tests witnessed by one of the authors and by W. Meissen (the originator of the 100/1300 μ s surge proposal), did confirm the fact that 20-mm varistors are destroyed by that surge (Martzloff, 1991 [15]). We now understand that the IEC proposals and the VDE 0160 requirements for the 100/1300 μ s surge have been pulled back or are being reconsidered.

Table 1
Computed peak currents for a postulated 100/1300 μ s surge and varistor rated peak current

Tolerance on varistor nominal voltage rating (%)	Computed results: Current peak and duration of surge in the varistor		Varistor rating: Allowable peak A for number of pulses of computed FWHM		
	Peak (A)	FWHM* (μ s)	100 pulses	10 pulses	1 pulse
- 10	658	625	45	80	210
0	454	515	55	90	250
+10	269	400	65	110	300

*FWHM: Full width at half maximum

5. Failure levels of clock motors and incandescent light bulbs

Two simple devices have been in service in millions of households and can be seen as surge threshold detectors: motor-driven clocks (before the advent of digital clocks) and still today, the ubiquitous incandescent light bulb. We have some knowledge on the failure rate of some of these devices under surge conditions, from which we can derive some inferences on approximate limits on the level of frequent surges. As we will see in the following two subsections, the data are not precise and are subject to undetermined statistical variations. However, the large number of observations does give some value to the inferences, compared to the limited number of observations obtained by more precise recording instruments.

5.1 Failures of motor-driven clocks

In the sixties, a report was published citing a 100:1 reduction in the failure rate of clock motors when the withstand level of the motor was raised from 2 kV to 6 kV [9], thus providing a verification of the "slope" of surge occurrence rate vs. peak reported by many researchers [16].

Figure 1 shows a plot of the frequency of occurrence of surge voltages versus their peak amplitude as reported by many researchers, and the two-point line of the 100:1 relationship between 2 kV and 6 kV occurrences. The parallel position of all these lines is remarkable. While the plots reported by the researchers are generally limited to a few thousand hours of observation, at only a few locations, the clock data represent the integration of more than 10000 clocks over a period of two to three years.

Because the data reflect only the number of clocks returned by dissatisfied customers, the exact number of clocks involved in "monitoring" the surge voltages, and the duration of the observation are not known. Therefore, rigorous statistical analysis would be meaningless. The point of our reality check, however, is still that a very large number of observation points were involved, for a period of several years, something that no survey could do -- but for this particular case history, it was all done before the environment began to be tamed by the proliferation of the new generation of low-voltage SPDs.

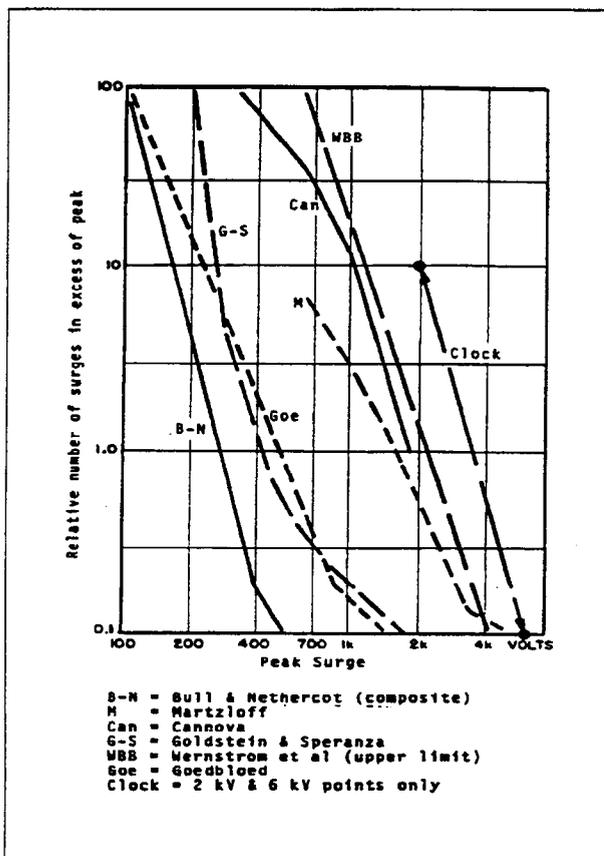


Figure 1 - Plots of frequency of occurrence versus peak amplitude reported in 1970-1987 surveys by independent researchers. The line marked "clock" only shows the slope from 2 kV to 6 kV, not the actual frequency of occurrence. Note how the lines are essentially parallel. (Plot reproduced from [16].)

5.2 Failure of incandescent light bulbs under surges

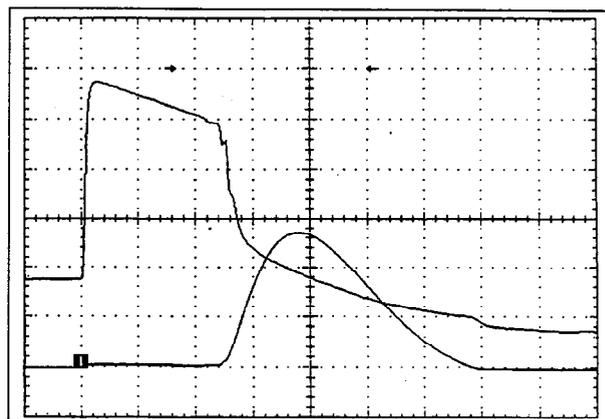
Tests recently performed by the authors on 120-V incandescent light bulbs show that few bulbs will survive surges in excess of 1200 to 1500 V †, and that a surge of as little as 800 V, when it occurs under the most sensitive condition, can trigger the failure. Limited tests performed on 240 V bulbs yield similar results. Comparison of tests performed by a researcher in Austria and by the authors on both 120 V and 240 V bulbs might be available at the time of presentation of this paper.

Since we do not hear reports of endemic failure of light bulbs beyond what can be expected from their known service life, we must conclude that the reality is that there is not a high rate of occurrence of surges at levels in excess of 1500 V.

† The measurements reported in this paper have been made with instrumentation for which the combined uncertainty should not exceed ± 5 to $\pm 6\%$. Given the process of applying the measurement results to the failure levels of light bulbs exposed to environments with characteristics that are at best known within an order of magnitude, this level of uncertainty does not affect the practical conclusions.

This observation merits a brief summary of our findings about the mechanism of bulb failure *triggered* by a surge. We emphasize the triggered aspect because surges of relatively low amplitude do not cause directly the failure of the bulb, but cause a secondary flashover at the power frequency that burns out the filament. Surges of higher amplitudes can deposit enough energy into the filament to melt it, but our point for the reality check is that even low-amplitude surges can result in failure of the bulb.

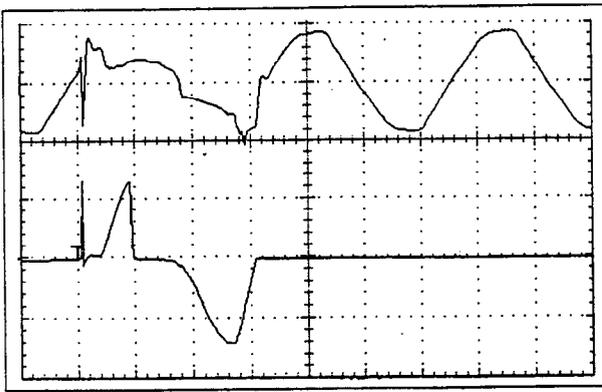
Figures 2, 3, 4, and 5 present oscillograms of the voltage across an energized 120-V bulb and of the current flowing in the bulb. Figure 2 shows a narrow window commensurate with the duration of the surge delivered by a 1.2/50 μ s - 8/20 μ s surge generator. We observe the chopping of the voltage wave, typical of a gap sparkover, and the rise of the surge current after the sparkover. The scale of the current trace selected to record the surge (hundreds of amperes) does not show the normal current (1 A) in the bulb. Observations during this test include hearing a pinging noise and seeing a bright flash of light, followed by darkness as the filament can then be seen broken at its points of attachment to the stems.



Top trace: Voltage across bulb terminals, 200 V/div
Bottom trace: Current in bulb, 100 A/div
Sweep: 10 μ s/div

Figure 2 - Voltage and current in light bulb during application of a 1.2/50 μ s - 8/20 μ s surge, resulting in surge sparkover

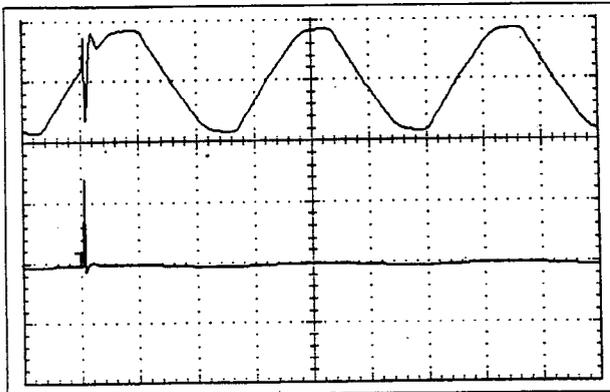
Figure 3 was recorded (for a new bulb) with a longer window to display two full cycles of the power frequency. At that sweep rate, the surge is no longer resolved, and its apparent peak on the trace may be lower than the actual peak because not enough data samples are collected around the peak. However, the timing of the surge, and the events following the surge are what is important in this figure. The surge event appears as a voltage spike and a current spike, followed by return to practically normal voltage and no visible large current. Then, suddenly, a pulse of power-frequency current appears, with a large amplitude -- the source of the observed flash. We believe that it is this current that causes the burn-out of the filament, not the "trigger" surge.



Top trace: Voltage across bulb terminals, 200 V/div
 Bottom trace: Current in bulb, 100 A/div
 Sweep: 5 ms/div

Figure 3 - Voltage and current in light bulb during application of a 1,2/50 μ s - 8/20 μ s surge at 30°, resulting in surge sparkover, followed by power-frequency flashover

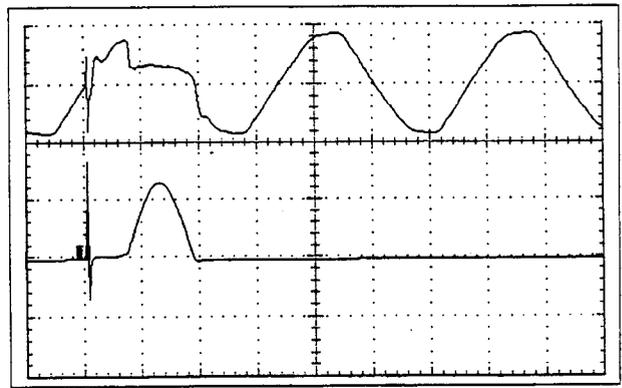
This belief is supported by the recordings shown in Figures 4 and 5. Figure 4 is actually the recording of a surge application to the bulb of Figure 3, in a test that preceded the fatal surge recorded in Figure 3. The applied surge was the same (900 V) for both Figure 3 and Figure 4. The randomness of the process of igniting the power arc is such that in the case of Figure 4, the power arc was not ignited, while in the subsequent surge application on the same bulb (Figure 3) and in the same conditions, the power arc was ignited, resulting in burn-out of the filament.



Top trace: Voltage across bulb terminals, 200 V/div
 Bottom trace: Current in bulb, 100 A/div
 Sweep: 5 ms/div

Figure 4 - Voltage and current in light bulb during application of a 1,2/50 μ s - 8/20 μ s surge at 30°, with surge sparkover but no power-frequency flashover

As further evidence, Figure 5 shows the process (in a new bulb) when the surge was applied at zero degrees, the time at which there is the least power-frequency voltage to ignite a power arc. In that test sequence, the first applied surge had an amplitude of 1000 V. Sparkover under the impulse did occur (the pinging noise was heard), but the power-frequency arc was not ignited and the bulb survived.



Top trace: Voltage across bulb terminals, 200 V/div
 Bottom trace: Current in bulb, 100 A/div
 Sweep: 5 ms/div

Figure 5 - Voltage and current in light bulb during application of a 1,2/50 μ s - 8/20 μ s surge at 0°, resulting in surge sparkover and delayed power-frequency flashover

The applied surge was then raised in 100 V steps, still with pinging heard but no fatal power-frequency arc. Figure 5 was recorded when the applied surge voltage was increased to 1300 V. At that level of energy deposited by the surge, enough plasma was generated in the path of the surge current to eventually ignite the power-frequency arc, but it had to wait until the power-frequency voltage had reached its peak.

To conclude this summary, Table 2 shows the relationship between the timing of the surge with respect to the sine wave and the amplitude of the surge sufficient to trigger ignition of the power-frequency arc. When the surge is applied at 90 degrees (the peak of the sine wave, making immediate ignition of the power arc easiest), a surge of 800 V is sufficient to trigger the power arc. At zero degrees, the surge must be raised to 1500 V to produce sufficient plasma to result in a subsequent power-frequency arc.

The phenomena are of course subject to the statistical variations of sparkover. The values shown in Table 2 are the averages of tests performed on a total of 20 bulbs of the same manufacturer, rated 100 W, replicating the test at several timing angles. This paper is not the medium for reporting in detail our series of experiments with other manufacturers and other watt ratings which produced similar results; the point is, however, that our inferences are not based on just the 20 bulbs of Table 2.

Table 2

Relationship between timing angle of the surge and amplitude necessary to produce a fatal power-frequency flashover

Angle (°)	0	15	30	45	90	135	150	165 *
Peak (V)	1500	1200	900	800	800	850	850	1100

* One specimen produced "pinging" starting at 1000 V, but no power arc. Eventually, the bulb failed at 1600 V without power arc, as a direct result of the energy dumped into the filament by the surge alone.

6. Limits to pushing surges into branch circuits

Some proposals have been made to require SPDs intended for installation on indoor circuits to withstand surges with relatively high peaks and short rise time. Such a requirement would mean that a substantial voltage drop would be developed ($L \cdot di/dt$) along the wiring. Added to the limiting voltage of the SPD at the end of the line, the voltage necessary to drive such a surge at the origin of the line would very likely cause flashover of wiring devices at the origin of the line.

This flashover, occurring during the rising part of the surge, would effectively shut off further propagation of the surge toward the SPD (except for the energy stored in the line during the current rise, which is easily dissipated by the SPD). Thus, the requirement of a large surge capability for SPDs installed with even a modicum of line length would be unrealistic. In support of this statement, we present here a summary of measurements on actual wiring and EMTP [17] computer simulation of a range of parameters.

Figure 6 shows the experimental circuit with a varistor connected at the downstream end of a "branch circuit" consisting of two copper conductors of 2-mm² cross-section (#12 AWG), typical of residential wiring. The first current transformer monitors the total current impinging at the upstream end. The second current transformer monitors the current flowing toward the downstream end, which will be imposed on the varistor. The clearances at the upstream end, such as clearances in a service-entrance panel, are represented by a discrete gap that can be set to produce sparkover at some given voltage during the test as well as in the model.

To determine the response of the circuit without the clearance limitation and verify that the model produces the same result, the gap setting was adjusted for this particular test so that no sparkover occurred at the upstream voltage developed for the current delivered by the generator. Once the model was validated, parametric variations could be performed for any combination of circuit length, applied surge, and clearance (gap) sparkover.

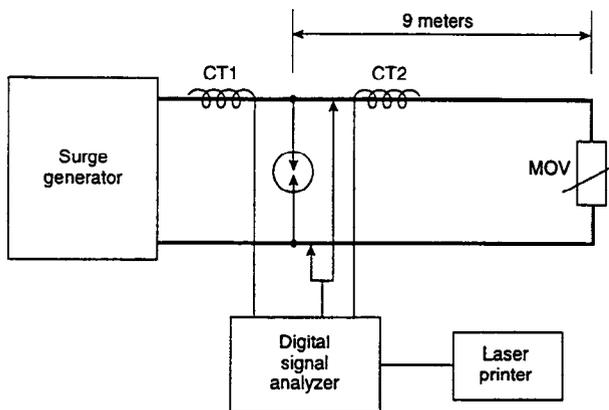
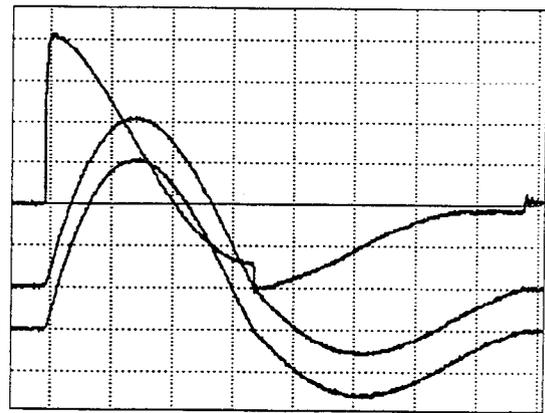


Figure 6 - Test setup for driving surges into a varistor installed at the end of a 9 m branch circuit.



Top trace: Gap voltage, 500 V/div
Center trace: Total current, 500 A/div
Bottom trace: MOV current, 500 A/div
Sweep: 10 μs/div

Figure 7 - Voltage and currents measured in the circuit of Figure 6

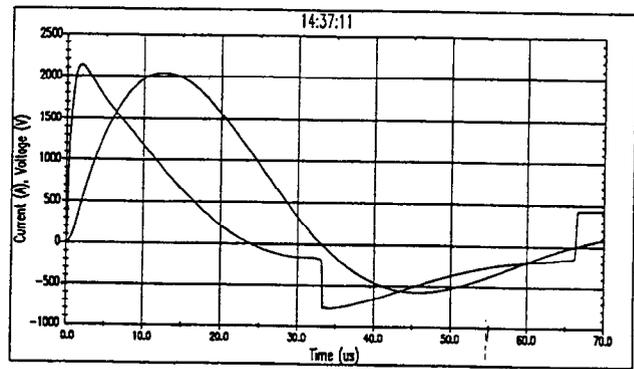


Figure 8 - Plot from model of voltage across the gap (compare with voltage trace of Figure 7) and impinging current (compare with the two current traces of Figure 7) for the circuit of Figure 6

Inspection of Figures 7 and 8 clearly shows the agreement between real-world measurements and model. The voltage traces are quite comparable. In Figure 7, the two current traces are identical since no current is diverted in the arrester. In Figure 7, the current trace is the one postulated in the modeling. This correspondence allows us to make parametric variations in the model with postulated impinging surges of higher values, such as those being proposed for consideration in some standards-writing groups. Another advantage of the model is that it allows postulating a current source – the consensus choice among lightning researchers – rather than using a surge generator which is only a charged capacitor with a wave-shaping network. Consequently, the surge generator interacts with the circuit into which the surge is injected, changing the waveform and losing the postulated constant waveform. This changing of the waveform makes it more difficult to perform parametric tests, compared to the ease of modeling. Table 3 below shows the results of such computations for the current waveform of Figures 7 and 8.

Table 3

Computed upstream voltage (in kV) necessary to drive a current of the peak value shown (columns) and rise time of 10 μ s into a branch circuit of length as shown (rows), terminated with a 130-V rated varistor

Peak Length	2 kA	3 kA	5 kA	7 kA	10 kA
10 m	2,3	3,3	5,2	7,2	10,1
30 m	5,8	8,5	13,9	19,4	27,0
50 m	9,3	13,7	22,7	31,6	45,0

As mentioned above, the insertion of an inductance in the load connected to the surge generator increased the rise time beyond the standard 8 μ s. In making the parametric computations, we chose to stay with this 10 μ s value to maintain continuity with the test/model validation.

Typical wiring devices used in 120-V installations have minimum flashover points in the range of 5 to 8 kV (this as a result of prevailing clearance requirements rather than insulation withstand levels as recommended by IEC Publication 664 [18]). Comparing this flashover level with the voltage values of Table 3 shows that, indeed, there is a strong possibility that within the range of current levels and branch circuit lengths of the table, a flashover would occur to throttle further propagation of the surge toward the varistor at the end of the branch circuit. In other words, postulating ever-larger surge currents into ever-longer branch circuits leads to a contradiction with the physical reality of the eventual occurrence of self-limiting flashover. This paradox has been discussed in greater details in a paper dedicated to that very subject [19].

7. Conclusions

1. Reality checks on the surge environment can bring a sense of perspective and help developing realistic standards for performance and application of surge-protective devices. This perspective can help avoid both over- and under-specification.
2. The proliferation of surge-protective devices in low-voltage ac power systems has made the recording of surge *voltages* practically irrelevant at best, and misleading at worst because the recorders indicate the let-through voltage of the SPDs, not the impinging surge. Surveys of the surge environment should focus on the ability (threat) of a surge to deliver a *current* to those ubiquitous surge-protective devices.
3. Field failure rates of various devices for which the failure level is known can provide some realistic and useful information on the rate of occurrence and level of surges in the environment.
4. Modeling the propagation of surges in a circuit, in particular computing the resulting voltages and comparing them with the known withstand level of insulation can provide a restraining influence on over-specifying surge protection requirements.

8. Acknowledgments

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**USING
INCANDESCENT
LAMP
FAILURE
LEVELS FOR
ASSESSMENT
OF THE SURGE
ENVIRONMENT**

Using Incandescent Lamp Failure Levels for Assessment of the Surge Environment

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Significance:

Part 2 Development of standards – Reality checks

Investigations were conducted in the US as well as in Austria, on 120-v and 240-V incandescent lamps to determine the levels of surges that can trigger an internal flashover of the hot filament, resulting in filament burnout. Repetitive surge application below the threshold do not result in premature failure of the lamp, but above the threshold, a single application can trigger a fatal flashover. By combining measurement of currents and voltage during the event with high-speed video recording, the mechanism has been clearly determined.

Depending on the characteristics of the surge (waveform, amplitude, and timing with respect with the power-frequency sinewave), thresholds of failure range between 800 V and 2000 V. Very few bulbs survive surges above 2200 V. Therefore, the conclusion is inescapable: if such surges were occurring frequently – according to some SPD advertizing claims – lamps would fail very promptly. We know they do not, ergo the alleged frequency of occurrence is incorrect.

USING INCANDESCENT LAMP FAILURE LEVELS FOR ASSESSMENT OF THE SURGE ENVIRONMENT

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Abstract The paper reports a joint investigation of the failure modes and levels of incandescent lamps ("light bulbs") exposed to surges occurring in low-voltage AC power systems. Tests were performed in one European laboratory and in one U.S. laboratory on typical 100-W bulbs used in the two environments, the North American 120-V systems, and the 230-V European systems. Through complementary tests and high-speed video observation of the flashes, more detailed understanding of the parameters has been obtained. Having determined what it takes to fail a light bulb by a surge, this information can be used to assess the surge environment by noting that frequent bulb failures do not occur, therefore surges above the failure threshold must be infrequent.

1. Introduction

Some proposals for standards on equipment surge immunity or on performance of surge-protective devices are driven by the commendable quest for conservative ratings. However, when this quest produces compounded safety factors, the result might not be cost-effective. The purpose of our paper is to present information on the failure mechanisms and levels of incandescent lamps ("light bulbs" or "bulbs" for short) under surge conditions, in support of the development of realistic standards on the surge environment in low-voltage ac power systems. Since we do not hear reports of endemic failure of light bulbs beyond what can be expected from their known service life [1], we must conclude that the reality is that there is not a high rate of occurrence of line-to-neutral surges at levels in excess of the threshold voltage at which bulb failure occurs.

The concept of applying reality checks to standards on the surge environment has been presented in earlier papers [2], [3], producing discussions on what the mechanism of bulb failure might be and how repeatable test results might be to define a representative threshold. This interest motivated further research into the subject at a U.S. laboratory where the initial measurements were made and at an Austrian laboratory for possible replication.

Therefore, we embarked on a systematic comparison where bulb specimens and test methods would be identical in the two laboratories. The test bulbs were taken from two shared batches, a 120-V type manufactured in Canada, and a 230-V type manufactured in Europe. Each laboratory planned to use a "Combination Wave" surge generator to apply the surges to the bulbs under similar conditions.

As it turned out, the test equipment available in the two laboratories were not identical, so that some unavoidable differences crept into the initial plan of exact replication. However, as the tests progressed in the two laboratories, enough other significant parameters affecting the outcome were found for a given generator, so that the generator variations became less significant, and the final conclusions cover a range of parameters rather than a single threshold

value. This finding does not affect the conclusion that a pragmatic range of surge levels can be identified, beyond which high bulb failure rates would become noticeable (which historically they have not been), thus providing a reality check on possible occurrence of frequent line-to-neutral surges.

2. Preliminary tests on 120-V bulbs

In these preliminary tests, first reported in [3] and summarized here, the test circuit used in the U.S. laboratory consisted of a Combination Wave surge generator capable of applying surges to the light bulb which was powered through the back-filter typically included in commercial surge generators. The phase angle of surge application with respect to the power frequency sine wave could be set at any value within 360 degrees. Monitoring the event was performed by a multi-channel digital oscilloscope with differential voltage probes.

Figures 1, 2, 3, and 4 present oscillograms of the voltage across an energized 120-V bulb and of the current flowing in the bulb. Figure 1 shows a narrow window, commensurate with the duration of the surge delivered by a 1.2/50 - 8/20 μ s surge generator. We observe the chopping of the voltage wave, typical of a gap sparkover, and the rise of the surge current after the sparkover. The scale of the current trace selected to record the surge (hundreds of amperes) does not show the normal current (1 A) in the bulb.¹ Observations during this test include hearing a pinging noise and seeing a bright flash of light, followed by darkness. After the test, the filament can be seen broken at one or both its points of attachment to the supporting connecting stems.

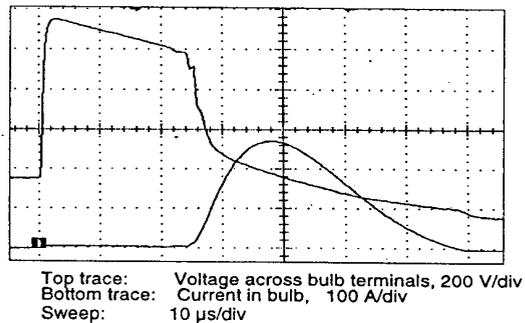
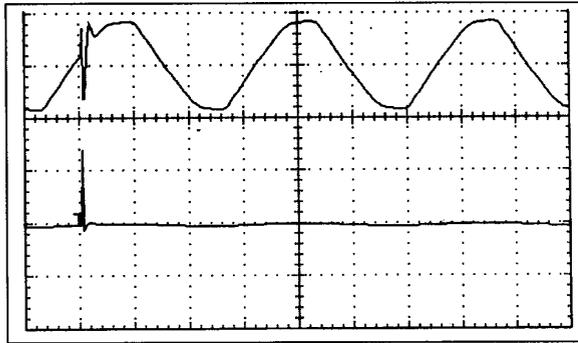


Figure 1: Voltage and current in bulb during application of a 1.2/50 μ s - 8/20 μ s surge, resulting in surge sparkover

¹ The measurements reported in this paper have been made with instrumentation for which the combined uncertainty should not exceed $\pm 5\%$ to $\pm 6\%$. Given the process of applying the measurement results to the failure levels of light bulbs exposed to environments with characteristics that are at best known within an order of magnitude, this level of uncertainty does not affect the practical conclusions.

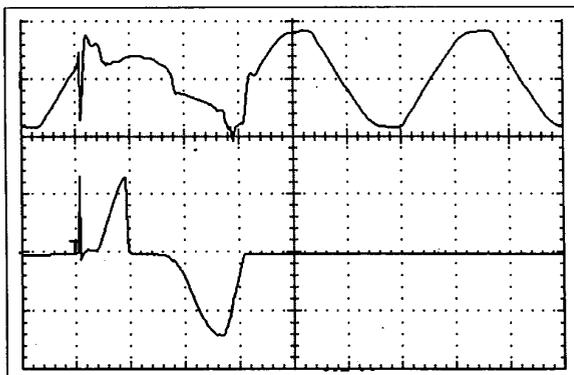


Top trace: Voltage across bulb terminals, 200 V/div
 Bottom trace: Current in bulb, 100 A/div
 Sweep: 5 ms/div

Figure 2: Voltage and current in bulb during application of a 1.2/50 - 8/20 μ s surge at 30°, with surge sparkover but no power-frequency flashover

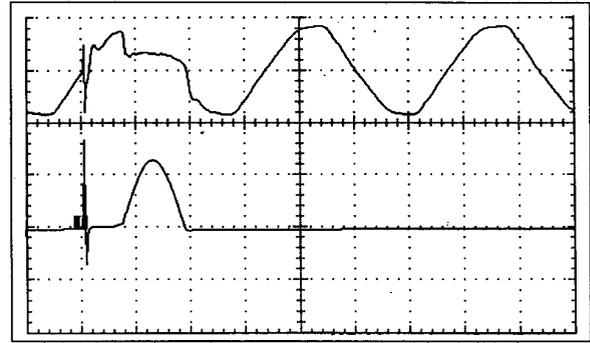
Figure 2 was recorded (with a new bulb) with a longer window than Figure 1, so as to display three full cycles of the power frequency. At that sweep rate, the surge is no longer resolved, and its apparent peak shown on the trace may be lower than the actual peak because not enough data samples are collected around the peak. However, the timing of the surge, not its amplitude is what is important in this figure. The surge event appears as a voltage spike and a current spike, followed by return to practically normal voltage and no visible large power-frequency current.

Figure 3 shows for the same bulb as that of Figure 2 the events during a subsequent shot under the same conditions. At first, after the surge, the pattern is identical to Figure 2. Then, suddenly, a pulse of power-frequency current appears, with a large amplitude -- the source of the observed flash. We believe that it is this current that causes the burn-out of the filament, not the "trigger" surge. The randomness of the process of igniting the power arc is such that in the case of Figure 2, the power arc was not ignited, while in the subsequent surge application on the same bulb (Figure 3) and in the same conditions, the power arc was ignited, resulting in burn-out of the filament -- a threshold situation.



Top trace: Voltage across bulb terminals, 200 V/div
 Bottom trace: Current in bulb, 100 A/div
 Sweep: 5 ms/div

Figure 3: Voltage and current in bulb during application of a 1.2/50 - 8/20 μ s surge at 30°, resulting in surge sparkover, with delayed two-pulse power-frequency flashover



Top trace: Voltage across bulb terminals, 200 V/div
 Bottom trace: Current in bulb, 100 A/div
 Sweep: 5 ms/div

Figure 4: Voltage and current in bulb during application of a 1.2/50 - 8/20 μ s surge at 0°, with surge sparkover and delayed single-pulse power-frequency flashover

As further evidence, Figure 4 shows the process (in a new bulb) when the surge was applied at 0°, requiring more energy for ultimately igniting the power-frequency flashover. At zero degrees, there is the least power-frequency voltage to ignite a power arc. Figure 4 shows the last of a seven-shot sequence in which the first applied surge had an amplitude of 1200 V. The applied surge was then raised in 100-V steps to 1500 V, still with pinging heard but no fatal power-frequency arc. To explore the hypothesis that the filament might be burned out by the surge energy, we held the surge at 1500 V for the next three shots. Only the last of the three triggered the fatal flashover, recorded in Figure 4. At that level of energy deposited by the surge, enough plasma was generated in the path of the surge current to eventually ignite the power-frequency arc, but for that shot it had to wait until the power-frequency voltage had reached its peak, again indicating a threshold situation.

To conclude this summary of preliminary findings, Table 1 shows the relationship between the timing of the surge with respect to the sine wave and the minimum peak amplitude of the surge sufficient to trigger ignition of the power-frequency arc. When the surge is applied at 90 degrees (the peak of the sine wave, making immediate ignition of the power arc easiest), a surge of 800 V is sufficient to trigger the power arc. At zero degrees, the surge must be raised to 1500 V to produce sufficient plasma to result in a subsequent power-frequency arc. The phenomena are of course subject to the statistical variations of sparkover.

The values shown in Table 1 are the lowest of several tests performed on a total of 20 bulbs of the same manufacturer, rated 100 W, replicating the test at several timing angles. At and near 90 degrees, the values were identical, at and near zero degrees, there was up to 200 V difference among tests. There is not enough space in this paper for reporting in detail our series of experiments (involving several hundred bulbs) on other manufacturers and other watt ratings. These produced similar results, so that our inferences are not based on just the 20 bulbs tested under the conditions of Table 1.

Table 1: Relationship between timing angle of the Combination Wave surge and threshold amplitude necessary to produce fatal power-frequency flashover

Angle of surge application	0	15	30	45	90	135	150	165
Open-circuit voltage setting of generator (V)	1400	1100	900	800	800	800	850	1100

3. Comparative tests at two laboratories

One of the motivations for comparing the tests at two laboratories was that preliminary data indicated some possible behavior differences between 120-V bulbs and 230-V bulbs, as well as some differences in the laboratory facilities and procedures. To alleviate this uncertainty, researchers on both sides of the Atlantic joined forces, hence the multinational authorship of this paper. As mentioned in the introduction, we started out with the goal of exact replication in the two laboratories: lamps from the same manufacturing batches, generators with similar characteristics, and power-frequency supplies with comparable available fault current.

However, practical limitations of available equipment and resources made that goal elusive. Instead, we then focused on identifying similarities in general behavior rather than identical numerical results. We also recognized that many independent parameters would affect the numerical results. Since our goal was to find the range of threshold values where failures begin to occur, our main concern became one of confirming the initial hypothesis that the failure was triggered by a surge and thus provide insights on the surge characteristics at the threshold of bulb failures. With that narrower goal, we made some side experiments by changing the parameters to develop a set of anecdotes, allowing us to identify the most significant parameters of the surges, and thus make inferences on their (limited) occurrence in the real world, given the absence of endemic bulb failures.

3.1 Influencing parameters

The influencing parameters that we considered involve lamp characteristics, surge characteristics, and power-frequency supply characteristics. The list below is given as an indication of the possible complexity of a comprehensive experiment -- which was not our goal -- as well as an invitation to other researchers who might be interested in lamp behavior. Clearly that level of detail is not in the scope of an EMC concern, and we will not give it much space, save for a few intriguing observations.

Lamp characteristics

- Geometry (axial or longitudinal, straight or c-shaped filament);
- Base position (up, down, horizontal);
- Nature of the gas fill (breakdown tendency);
- Temperature gradient near the filament;
- Characteristics of the fuse contained in the stem.

Surge characteristics

- Waveform (peak, duration, Combination or Ring Wave);
- Source impedance (ability to deposit energy in the arc);
- Timing with respect to power-frequency sinewave.

Power-frequency supply

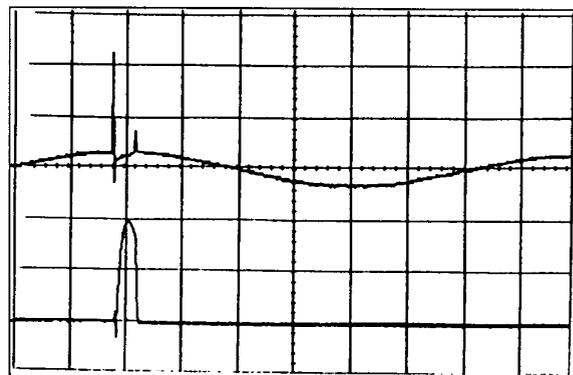
- Voltage;
- Available fault current (ability to establish stable arc).

3.2 Typical test results

Figures 5, 6, and 7 show typical recordings obtained at the Austrian laboratory during application of a 1.2/50 μ s - 8/20 μ s surge with a peak amplitude sufficient to produce a sparkover, either immediately or after a short delay. (In the figures, the surge peaks are not resolved at the millisecond sweep rate.)

In Figure 5, a 230-V bulb shows the occurrence of a single-pulse of power-frequency flashover occurring immediately after the surge sparkover. Figures 6 and 7 show the occurrence of a delayed flashover with a single or double pulse, similar to the pattern for 120-V bulbs, respectively in Figures 4 and 3.

The peak values of the power-frequency flashover observed in the Austrian laboratory are different from those obtained in the U.S. laboratory because the available generators in the two laboratories have different effective back-filter impedances.



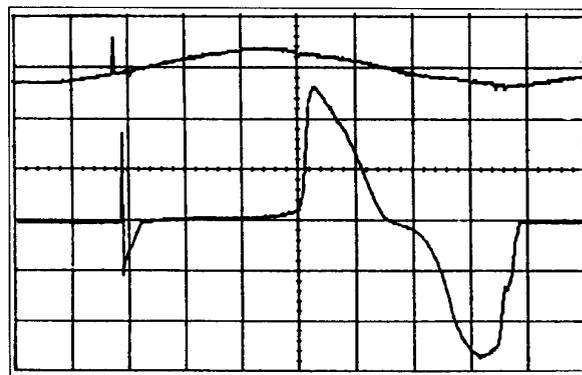
Top trace: Voltage across bulb terminals, 1000 V/div
 Bottom trace: Current in bulb, 50 A/div
 Sweep: 2.5 ms/div

Figure 5: Voltage and current in 230-V bulb during application of a 1.2/50 μ s - 8/20 μ s surge at 90°. Surge spark-over is followed immediately by power-frequency flashover



Top trace: Voltage across bulb terminals, 500 V/div
 Bottom trace: Current in bulb, 50 A/div
 Sweep: 2 ms/div

Figure 6: Voltage and current in 120-V bulb during application of a 1.2/50 μ s - 8/20 μ s surge at 90°. Surge spark-over and delayed one-pulse power-frequency flashover



Top trace: Voltage across bulb terminals, 500 V/div
 Bottom trace: Current in bulb, 20 A/div
 Sweep: 2.5 ms/div

Figure 7: Voltage and current in 120-V bulb during application of a 1.2/50 μ s - 8/20 μ s surge at 350°. Surge spark-over and delayed two-pulse power-frequency flashover

Table 2 shows a summary of test results obtained in the Austrian and U.S. laboratories for 120-V bulbs and 230-V bulbs, under various parameters of surge application. These results show some differences in the thresholds for the tests conducted on the same type of bulbs.

Table 2 - Comparison of results from Austrian and U.S. laboratories

Lamp Type	Austrian Laboratory			U.S. Laboratory		
	Surge type Source imp.	Angle	Typical threshold	Surge type Source imp.	Angle	Typical threshold
120 V 100 W	'Combination' 24 Ω	90 30	1100 V 1400 V	Combination 2 Ω	90 30	800 V 1500 V
	Ring Wave 22 Ω	90 60	1600 V 2600 V	Ring Wave 12 Ω	90	2100 V
230 V 100 W	'Combination' 24 Ω	90 60	2200 V** 2800 V	'Combination' 10 Ω	90	1800 V
(**=75 W) (* =40 W)	Ring Wave 22 Ω	90	3100 V	Ring Wave 30 Ω	90	2200 V*

The differences in the threshold levels observed for the two types of lamps shown in Table 2 are likely the results of many parameters, as listed in paragraph 3.1. One is probably the filament configuration. In the 120-V, 100-W bulbs, the filament is a 22 mm long straight section strung between the two stems, with a single support at mid-span which does not affect the straight line aspect. This configuration offers the easiest path for a sparkover to occur. In the 230-V, 100-W bulbs, the 25 mm long filament has a shallow c-shape obtained by two intermediate supports that deflect the filament from a straight line between the two stems. This configuration creates a slightly longer path than that of the 120-V bulbs. In a few anecdotal tests on a 120-V 40-W bulb which has a 40 mm long horseshoe shape, we found that sparkover occurs along the filament or straight from stem to stem, typically about 4000 V.

Another possible reason for the higher threshold voltages found in the Austrian laboratory using its "Combination Wave" generator might be that its source impedance is 24 Ω, compared to the Combination Wave generator used in the U.S. laboratory, with its conventional 2 Ω source impedance. The difference will be in the surge current following sparkover, creating less plasma in the case of the Austrian laboratory, and therefore requiring a higher surge voltage to ignite the flashover. Nevertheless, oscillograms show similar patterns.

4. High-speed video observations

On the basis of the oscillograms, the most likely mechanism which we could propose was that a sparkover occurs when a surge of sufficient voltage amplitude is applied, producing an ionized path which enables the much lower power-frequency voltage to initiate an arc. In turn, this arc would produce enough heat at its point of attachment to cause local melting of the filament. During discussions with colleagues, other possible mechanisms were mentioned, such as a local melting of the filament caused by the surge current alone, mechanical shock on the hot filament, and the possibility that the reignitions of the power-frequency arc, such as that of Figure 2 and 7 might be associated with a broken filament that would oscillate and sweep by the stem close enough for a reignition.

In an attempt to settle this uncertainty, we obtained a high-speed video system consisting of two image converters, a digital acquisition memory, display monitor, and video recorder. The image converters had a capability of 1000 full frames per second, with the possibility of multiple scans of a limited field during the 1 ms basic full-frame scan time.

Figure 8 shows the arrangement of the two converters, with the left converter (L) controlled to perform four scans limited to the filament area, and the right (R) converter controlled to perform a full-frame view of the filament and fuse contained in the base stem of the bulb. The recording system was triggered by a signal from the surge generator, providing the beginning of the recording a few milliseconds before the surge, and lasting about 100 ms after the surge.

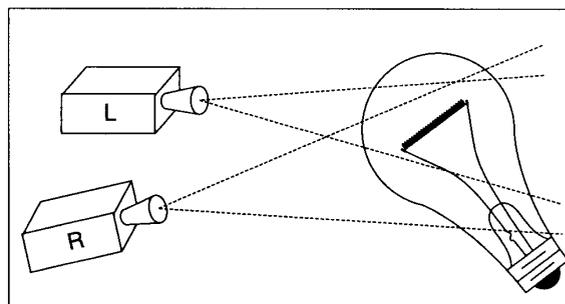
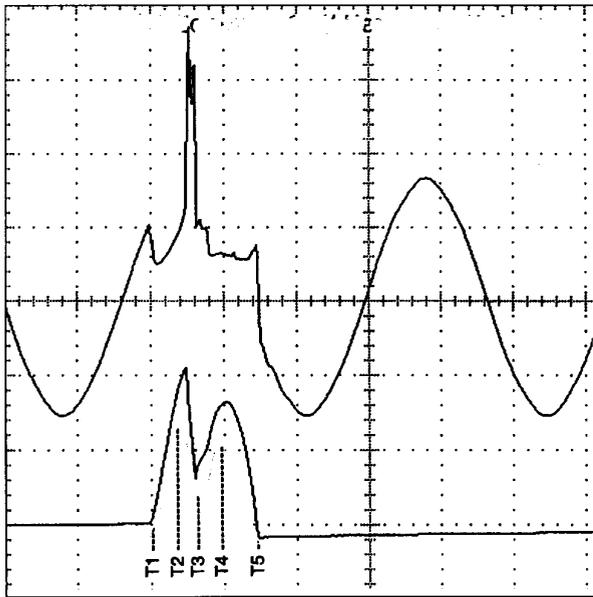


Figure 8: Arrangement of image converters to scan spark-over and flashover limited to the filament (L) and a full frame (R) including the base where the fuse is located

With this arrangement, recordings such as those shown in Figure 9 and Figure 10 were obtained. As an example of many oscilloscope recordings, Figure 9 shows the electrical record of the failure of a 230-V, 100-W bulb triggered by a 100 kHz Ring Wave applied 60 electrical degrees after the zero crossing. At time T1, sparkover of the filament occurs, accompanied in this case by immediate flashover of the power frequency arc, which rises to a first peak at T2, about 2.3 ms after its beginning. At that time, the current is throttled while the voltage increases sharply, indicating operation of the fuse. The fuse does not force a current zero at time T3, but allows a second peak of current at time T4, before decaying to a natural current zero at time T5.

This type of oscilloscope recording was the only one that was available until the video system was obtained. From such oscillograms we could only draw speculative inferences on the mechanism and behaviour, hence the motivation to enhance our tests by video recording. Figure 9 shows the voltage across the lamp terminals (top trace) and the current through the lamp (bottom trace). With the sweep set to cover 2 cycles of the power frequency, neither the surge voltage nor the surge current are resolved in this oscillogram. In this test series performed after the preliminary series, a separate oscilloscope with faster sweep was used to record the surge voltage and the surge current, as was done in the case of Figure 1. To conserve space, these surge recordings are not shown here, but the peak values of the resulting sparkover voltages recorded by the second oscilloscope are listed in Table 2 in the column "Typical threshold".

We now will examine in Figure 10, opposite page, four frames of video recording corresponding to the electrical record of Figure 9. The left half of the screen shows four scans from converter L of Figure 8, proceeding from top to bottom and each scanned in 250 μs. The right side of the screen shows the full frame, scanned over a period of 1 ms. The timing of the frame is shown in the lower right corner of the picture, such as "ET + 000000010" which indicates the 1 ms window during which the surge was initiated. Other descriptive data in the margins are not significant to our story. Figure 10 shows the four frames respectively in the sequence from top to bottom for 0-1 ms; 1-2 ms; 2-3 ms; and 4-5 ms.



Top trace: Voltage across bulb terminal, 200 V/div
 Bottom trace: Current in bulb, 20 A/div
 Sweep: 5 ms/div

Figure 9: Failure of a 230-V, 100-W bulb triggered by a 100 kHz Ring Wave applied at 30°, showing fuse operation

In the first frame, 0-1 ms, the Ring Wave sparkover is not visible because of its relatively low intensity/duration. The first 250- μ s scan (top) of the left side shows only the filament glow. Starting with the second scan, the beginning of the flashover (time T1 in Figure 9) is visible, while the right side scan, adjusted for less sensitivity, only shows the filament glow without the flashover beginning.

In the second frame, the flashover growth is visible on the four left scans (T1-T2 in Figure 9), while the right scan now has enough light integrated over the full 1 ms time to show the flashover.

In the third frame, the first two (top) left scans still show the flashover around the filament, but the last two (bottom) scans show the flashover current being starved as the fuse in the lamp base begins to operate and transfers the current to the space around the fuse. This corresponds to the time T2-T3 in Figure 9. The right side scan shows the beginning of the glow in the base of the lamp as the current around the fuse grows.

In the fourth frame, the four left scans show the filament still glowing, *but unchanged (not yet broken)*, while the right scan shows the integrated glow of the fuse operation, corresponding to the time interval T4-T5 of Figure 9.

These four frames, typical of many others recorded during our experiments, clearly show the mechanism of a flashover at the power-frequency current, and in this particular test sequence, the operation of the fuse in the base of the bulb. Other records taken with different light sensitivity show an intense glow near the connection of the filament, where the arc is concentrated at its point of attachment (the cathode hot spot) while the path around the filament is more diffuse. The concentrated heat near the point of attachment eventually causes a local melting of the filament. In other records, the filament is then seen separating from the stem and slowly falling in the time frame of about 20-50 ms after the flashover.

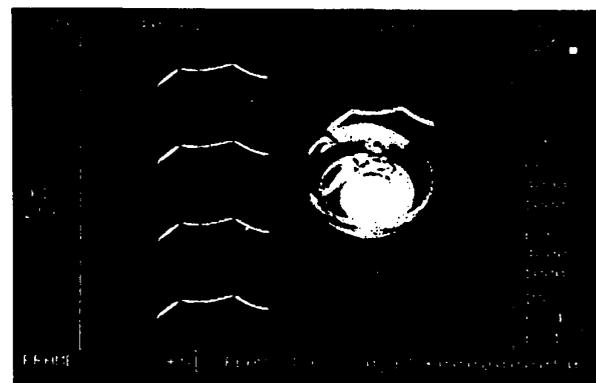
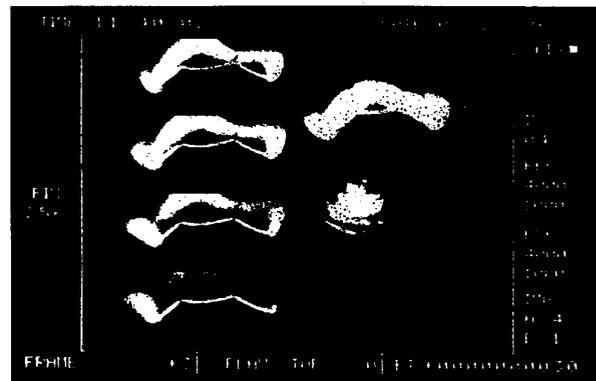
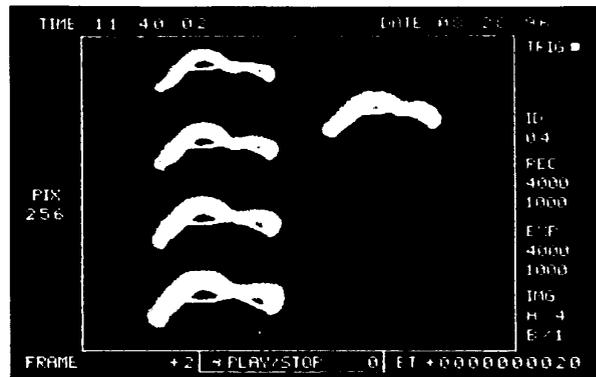
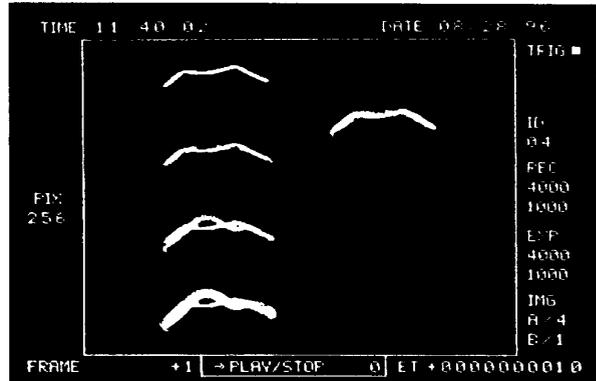


Figure 10: Sequence of flashover and fuse operation corresponding to the oscillogram of Figure 9

5. Discussion

Observation of the failure mechanisms and the levels of surges necessary to trigger the fatal power-frequency flashover as recorded in the two laboratories are consistent with each other and point out that a two-stage process is involved at lower amplitudes of the impinging surge, as shown by the video recordings and inspection of the oscillograms.

When the surge current ceases, and after some variable delay influenced by the instantaneous voltage of the power system voltage at that time, a flashover fed from the power-frequency source occurs along the path pre-ionized by the surge plasma.

It is noteworthy that the path along the filament is longer than the clearance between the two stems as they emerge from the glass envelope at the base of the bulb and yet the sparkover and the flashover occur at the longer (but hotter) clearance along the filament. By comparison, we found that a surge sparkover in a cold bulb which is not energized occurs for a much higher surge level, typically in the order of 5 kV to 6 kV, and that sparkover in that case does not occur near the filament but at the point where the metal stems emerge from the glass base, the shortest clearance.

According to this understanding of the mechanism, we can expect that a surge with a different waveform, the 100-kHz Ring Wave for instance, would require a different level to produce a sufficient amount of plasma necessary for ignition of the power-frequency arc. This is indeed the case as seen in Table 2. Applying the 100-kHz Ring Wave, sparkover can occur for open-circuit levels as high as 3 kV (pinging is heard) but without the following power-frequency flashover. Thus, the assessment of the surge environment severity given by the thresholds of bulb failure not only tracks the amplitude of the surge but also its waveform. There is a blessing as well as a curse in this situation: the assessment we can obtain reflects the energy-delivery capability of the surge as well as the peak voltage level of the surge. The two are not separable and thus the information remains imprecise, but still valuable as giving an indication of the severity of the surge environment.

This application of bulb failure levels to assess the surge environment must be made with the understanding that it rests on observations of in-use failure rates, a common experience to millions of users, noted before the proliferation of surge-protective devices (SPDs). Nowadays, the observed surges in low-voltage power systems are much lower than the threshold levels identified in this paper for bulb failures because of this proliferation of SPDs [4], [5].

It should also be noted that this assessment is limited to the surges occurring in the line-to-neutral mode at the location of the bulb. Depending upon the neutral grounding practices of the installation, surges impinging at the service entrance will propagate toward the victim light bulb in different manners. In the U.S. practice, where the neutral and earth conductors are bonded at the service entrance, a surge impinging from the power system in the common mode is converted into a differential mode, that is, a line-to-neutral surge is always applied to the bulb. In some countries where the service entrance bond is not present, an impinging surge in the common mode remains as such as it propagates within the house wiring, and thus the stress applied to the bulb from an external surge, such as a lightning-induced surge, may have different consequences for the light bulb. This point is important and should serve as a reminder of the significant differences in the effectiveness of surge-protection schemes according to the grounding practices in use among different countries.

6. Conclusions

Light bulbs failures levels and mechanism

1. The principal failure mode of light bulbs involves a power-frequency flashover triggered by the surge which produces a sparkover and sufficient plasma around its path to ignite an arc fed by the power-frequency source. This arc then melts the filament at its point of attachment. High-speed video recordings confirm tentative conclusions derived from observation of the current and voltage oscillograms.
3. The surge level necessary to trigger the power-frequency flashover depends on the phase angle of the surge with respect to the power-frequency sine wave. When applied near the peak voltage, the necessary voltage may be less than 1000 V peak for an 8/20 surge. When applied near zero crossing, the necessary voltage may be twice that sufficient near the peak.
4. Tests performed at two different laboratories show good agreement on the mechanism, with some differences attributable to some differences in the surge generators which could not be matched exactly. Such a difference serves to show that the phenomenon covers a range of thresholds, not a single sharp value.
5. For surges with high energy delivery capability, such as the 1.2/50-8/20 μ s Combination Wave, typical 120-V bulbs experience failures with surges as low as 800 V under the most sensitive phase angle of timing. For typical 230-V bulbs, the corresponding level is as low as 1800 V.
6. For surges with low energy delivery capability, such as the 100 kHz Ring Wave, typical 120-V bulbs experience failures with surges as low as 2100 V under the most sensitive phase angle of timing. For typical 230-V bulbs, the level at the most sensitive time is as low as 2200 V.

Application to standards on the surge environment

Knowledge of failure mechanisms of light bulbs and the levels at which failure occurs in a surge environment brings a sense of perspective and helps developing realistic assessment of the surge environment because surge-induced failures are rare. From this assessment, proposed standards for performance and application of surge-protective devices can be reconciled with reality and avoid under- or over-specification of the devices.

7. Acknowledgments

Support and encouragement for this work was provided by the parent organization of each author. Additional support was received from Delmarva Power Company, and from Pacific Gas & Electric Company. Tom Conrad, Gerald FitzPatrick, and Don Worden contributed comments on the draft.

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**DEVELOPING
A
CONSUMER-
ORIENTED
GUIDE
ON
SURGE
PROTECTION**

Developing a Consumer-Oriented Guide on Surge Protection

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Significance:

Part 2 Development of Standards – Reality checks

Caught among contradictory stories on the need for surge protection as well as unsupported anecdotes of surge-related failures, the typical consumer is in a quandary on how to best allocate personal resources to protect the expensive electronic equipment found in a modern household.

To help provide some answers to this quandary, a team of experts developed an engineering guide on the basics of surge protection. One of the recommendations addresses the issue of ineffective configuration of utility connections that are responsible for surge-induced failures. This paper served as a progress report, documenting the status of the project at the end of 1996. An update on this project was reported under Key et al. 1999, which is included in Part 6 of this Anthology

Developing a Consumer-Oriented Guide on Surge Protection

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ABSTRACT

Caught among contradictory stories on the need for surge protection as well as unsupported anecdotes of surge-related failures, the typical consumer is in a quandary on how to best allocate personal resources to protect the expensive electronic equipment found in a modern household. To help provide some answers to this quandary, a team of experts is currently developing a practical application guide on the basics of surge protection, providing a tutorial suitable for the "average consumer." This paper shows how the guide intends to take the reader through the thicket of surge protective devices and applications.

The guide covers a range of application issues from basic information on the occurrence of lightning and switching surges to the selection of cost-effective and technically sound mitigation methods. It explains how protection applications must be suitable for the geographic area (lightning flash density), power distribution type (urban or suburban), and grounding practices (recent NEC or grandfather). The ultimate goal is that the guide will become the basis for better mitigation practices, will decrease losses and the number of loss claims, and will reduce the fear and frustration levels among end-users relative to surge-related upset or damage to their appliances and electronics.

INTRODUCTION

Each year lightning is estimated to be responsible for 250 to 500 million dollars in property damage in the United States. This estimate is based on an analysis of insurance claims compared to cloud-to-ground lightning flash data by operators of the National Lightning Detection Network, described by Byerley et al [1]. Since about 1989 this system has provided a very accurate count and location of lightning flashes to ground and of the resultant ground flash density in the United States. For some insurance companies the data have been used to confirm or deny lightning damage claims.

An analysis of 5500 detailed claims found more than half of the loss was related to telephone and electronic appliances. Considering that many of these losses are preventable, the insurance companies that pay millions of dollars per year replacing lightning- or other surge-damaged appliances are highly motivated to help prevent such damage. As a first step toward minimizing surge damage, State Farm Insurance Companies, Illinois Power, and EPRI have joined forces to devise a recommended practice for residential surge protection, wiring, and grounding. To achieve

* Electricity Division, Electronics and Electrical Engineering Laboratory, Technology Administration, U.S. Department of Commerce. Contributions from the National Institute of Standards and Technology are not subject to United States copyright.

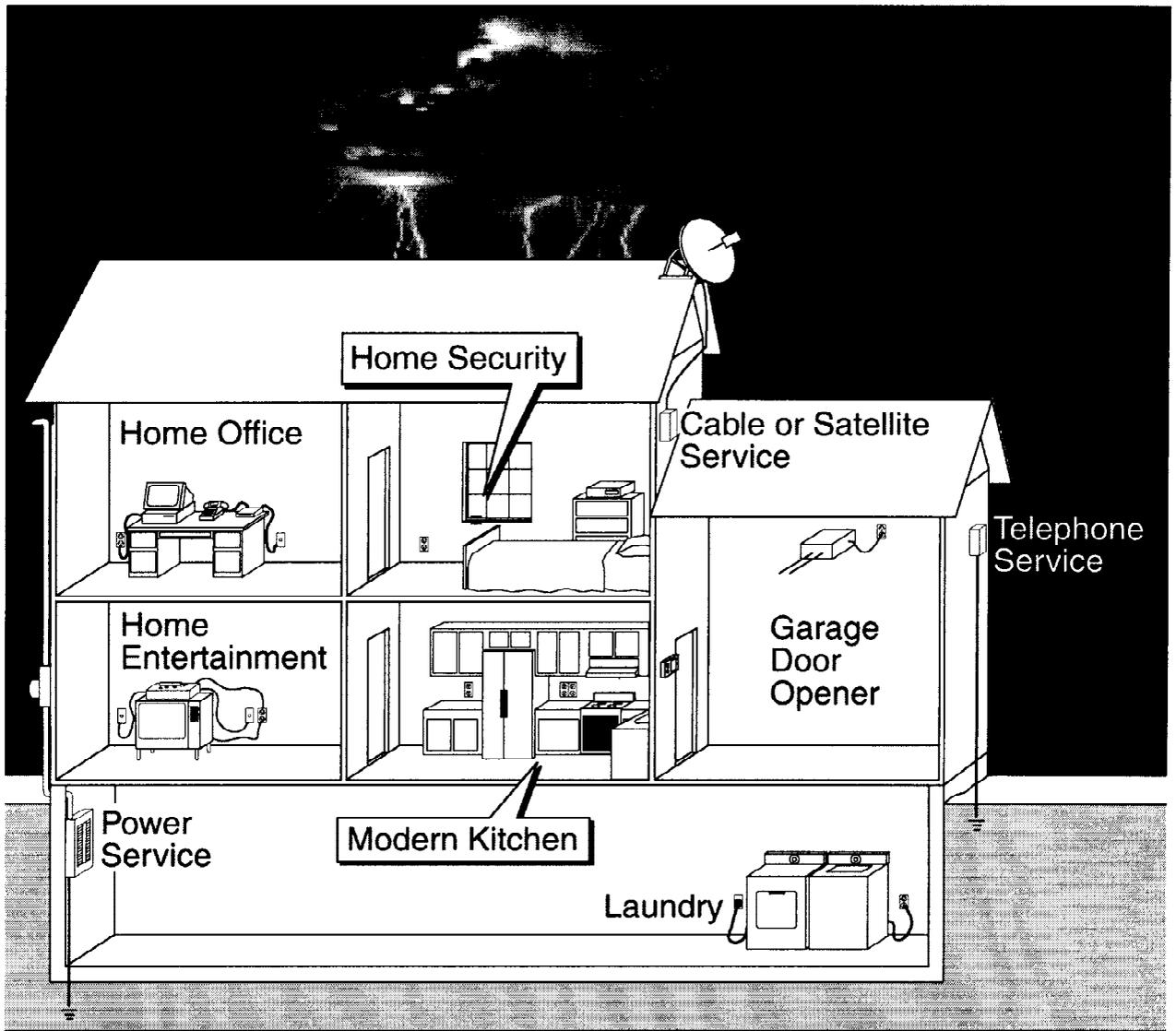


Figure 1. Typical candidate appliances and systems for surge protection in a residence

this goal, the project team is completing three specific tasks: 1) initial fact-gathering, 2) a workshop to discuss surge protection issues, and 3) completion of the manual of recommended practice.

A typical modern residence contains many electronic and multi port appliances. In order to identify specific protection practices, a typical residence was defined with electronic appliances and systems illustrated in various rooms as shown in figure 1. The main systems are home entertainment including cable TV, home office including telephone, and electronic kitchen appliances. Other common vulnerable systems are home security, intercom, satellite receiver, garage door opener, and laundry equipment. Generally, any system with micro-electronics plus exposure due to power and signal wiring can be found in lightning damage claim reports.

Technical inputs were gathered on the expected surge threat and the key elements of protection.

These including lightning, surge propagation, grounding, surge protection practice, and built-in appliance immunity. The workshop was successful in building consensus despite the fact that different experts had different backgrounds and experiences. At the end all agreed on essential principles of how to protect sensitive equipment. However, specific devices and procedures that could be applied case by case were more difficult to obtain. Also, there is currently no information about compatibility levels and points of vulnerability for many modern residential electronic appliances and systems. Consequently, specific installation methods and precise recommendations on how to protect these electronic systems from surge threats are not yet fully developed.

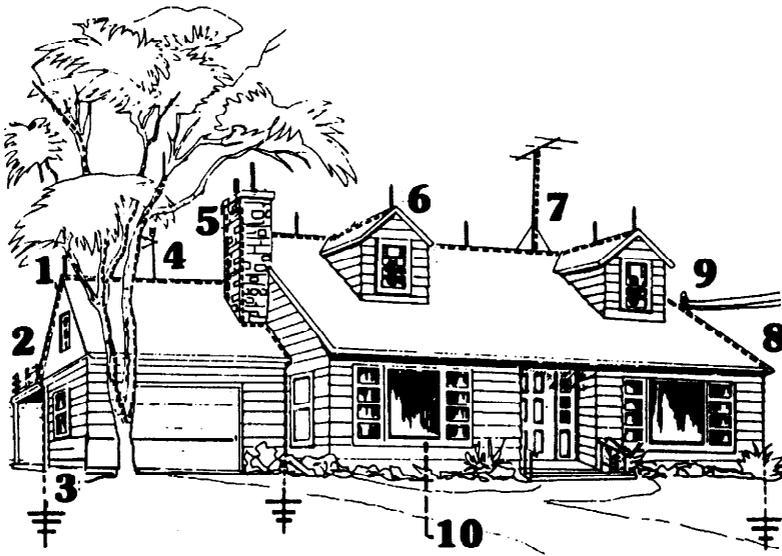
A few appliances seem to have varying susceptibility to surges. The most notable losses, according to insurance claim records, are telephones and modems, computerized equipment, TV, VCR and satellite receiver systems. These are generally multi-port appliances—that is, appliances connected to several different systems, such as an entertainment center connected to power, cable, and telephone or a security system connected to power, sensor, and control. These multiple connections, and the likelihood of potential differences between them, are believed to be major factors in surge damage susceptibility of many residential appliances and systems.

ESSENTIALS OF SURGE PROTECTION

Lightning is not the only threat, but remains a dominant one.. Most of the Continental United States experience at least two cloud-to-ground (C-G) flashes per square kilometer per year. About one half of the area will see three C-G flashes per km², which is equivalent to about 10 discharges per square mile per year. The maximum flash densities are found along the southeastern Gulf Coast and the Florida peninsula, where the values approach 20/km² or 50/mi². Overall about 30 million C-G flashes strike the United States each year, and lightning is clearly among the nation's most severe weather hazards.

In high lightning areas, perhaps a threshold of three C-G flashes per square km per year, some degree of structural lightning protection is recommended. This threshold may be economically justified by the growing value, and vulnerability, of residential electronic systems. There is good information available on how to accomplish this protection. Basic lightning protection practices are well defined in the Lightning Protection Code, NFPA 780. An illustration of the basic elements of this protection is provided in figure 2. These protection techniques will not eliminate surges that enter the residence either via different wiring systems—including the grounding electrode system—or coupled into wiring from nearby flashes. In addition to the lightning threat, some basic household appliances can act as surge generators, for example a light switch or a furnace igniter.

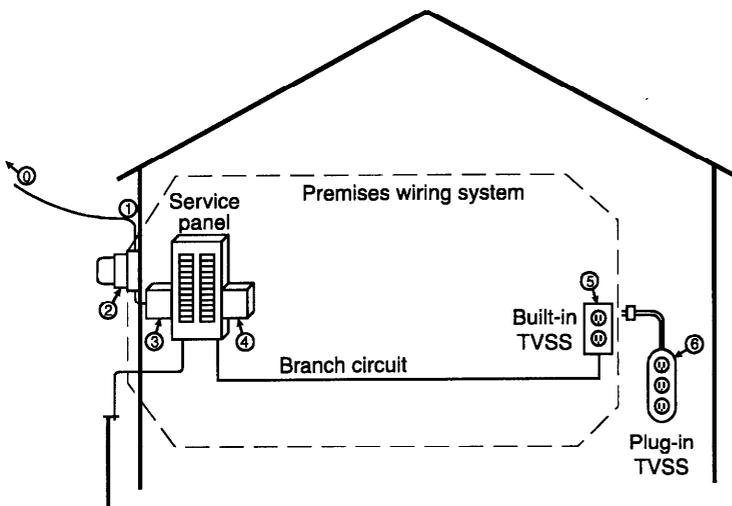
Protection against these surges can take several forms. The possibilities are: preventing the surge at its origin (impossible for lightning and difficult for surges associated with normal operation of the power system), diverting the surge to ground as it impinges on the building, before it enters the building (the most effective approach), and finally, clamping by a surge protective device (SPD) at the equipment (either by an add-on, plug-in, or built in SPD). Of these three approaches, only the service entrance and add-on SPDs are options available to the end-user. Service entrance protection may be offered by the local utility. If not, a licensed electrical contractor may offer installation service. In either case installation of service entrance protection must be done by a professional. Various locations for power line or transient-voltage surge suppressors (TVSS) are shown in figure 3.



Legend

1. Air terminal spaced 6 meters (20 feet) apart along ridges and within 0.6 meter (2 feet) of ridge ends
2. Down conductors (minimum of two)
3. Grounding rods; minimum of two, at least 3 meters (10 feet) deep
4. Roof projections such as weather vanes connected to grounding system
5. Air terminals located within 0.6 meter (2 feet) of outside corners of chimney
6. Dormers protected with air terminals
7. Antenna mast bonded to roof ground conductor
8. Gutters and other metal objects bonded to grounding system
9. Surge arrester installed at service panel to protect appliances
10. Transient voltage surge suppressors installed at receptacles powering sensitive electronics

Figure 2. Typical comprehensive lightning protection of a residence (reprinted with permission from Underwriters Laboratory Inc.)



Power Line Surge Protectors

- Service Entrance Arresters ①,②,③
- Branch Circuit Suppressor ④
- Receptacle Plug-in Suppressor ⑤
- Plug Strip with Suppressor ⑥

Figure 3. Options for power-line surge protection in a residence

BASICS OF APPLICATION

A fundamental application issue in surge protection is grounding. Proper grounding is critical for the case of ground-seeking surges such as lightning currents. However, even more critical for multi-port appliances or systems is the difference in ground reference between port connections. This vulnerability issue is addressed in the National Electric Code (NFPA 70), which requires that the grounding conductors of all systems entering a facility be bonded together. As shown in figure 4 (left), a split bolt is used to bond different system grounding conductors to the power system grounding electrodes. Unfortunately this bonding is not always done and a common, but incorrect and dangerous to personnel and equipment, scenario is shown in figure 4 (right).

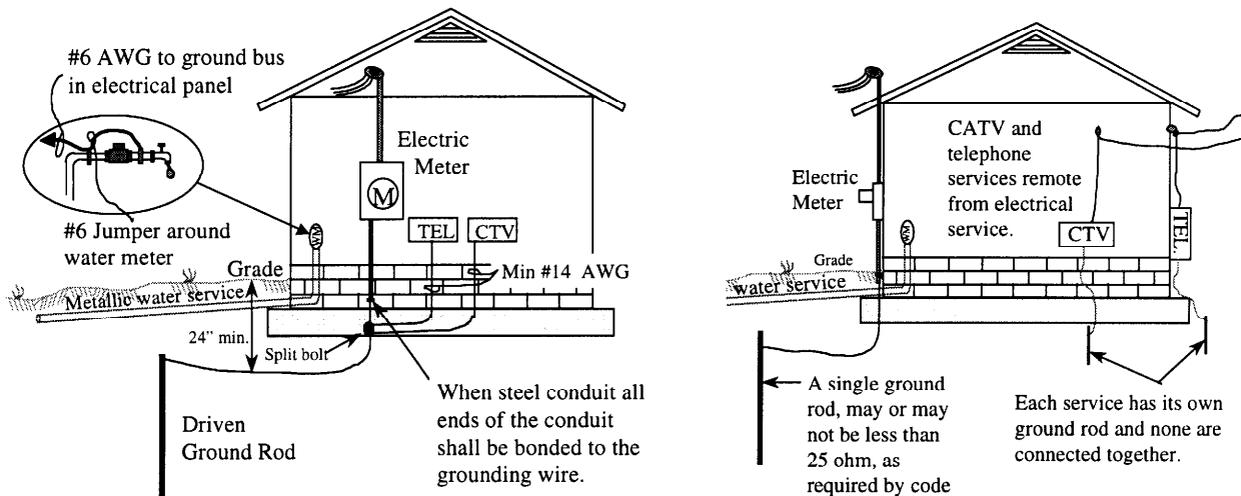


Figure 4 – Minimum code-required service grounding (left) and commonly found, but incorrect grounding of existing house (right)

The incorrect grounding of the house, figure 4 right side, was identified by the study group as the primary problem in residential system surge protection. Even with independent surge protection of the power and communications ports, smart electronic appliances are left vulnerable to surges. During a surge, an elevation of only one of the port reference potentials relative to the other(s) can upset or damage appliances. The problem is depicted in figure 5, which shows the connection of a facsimile machine (FAX), to both the power system and communication system. The SPD (also called TVSS in the industry jargon) on the power port of the FAX can be built-in or installed externally by the end user. The arrester at the service entrance, shown in dotted lines, may or may not be present. According to standard practice, the telephone company has installed a network interface device (NID) at the point of entry. The NID is installed primarily to protect the telephone system and human users from hazards of faults and surges that travel from the premises toward the telephone system, rather than to protect from surges impinging from the telephone system into the premises. Nevertheless, should a surge impinge on the telephone system, the NID will divert it to ground, in this case the nearest cold water pipe.

Consider the case of a surge impinging on the entrance of the telephone system. The surge current shown as "Surge I" in figure 5 flows in the long path from the entrance of the telephone system to the grounding point in common with the power system entrance. The inductance of this path is typically tens of micro-henries, and the surge current can rise to several hundred amperes in one microsecond. The resulting voltage drop along the current path from the telephone port to ground

is $L \times di/dt$ (voltage elevation at the FAX communications port). In contrast, the FAX power port is not affected by the surge because none of the power port conductors carry any surge current. Assuming $10 \mu\text{H}$ for L and $500 \text{ A}/\mu\text{s}$ for di/dt , the voltage drop that appears between the power port and telephone port of the FAX is $V_{\text{difference}} = 10 \mu\text{H} \times 500 \text{ A}/1 \mu\text{s} = 5000 \text{ V}$. This voltage is likely sufficient to cause a flashover on the printed circuit boards, or a semiconductor failure in the FAX. Most FAX failures can be readily explained by the elevated reference potential associated with independent surge protection of the two ports.

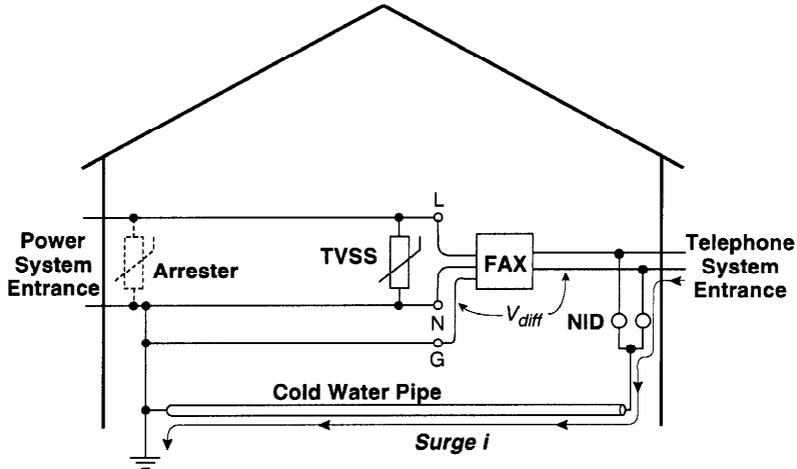


Figure 5. Surge voltage potential difference develops between two protected ports of appliance

The *INTERSYSTEM BONDING POINT* for connection of telephone, television, and radio antenna grounding wires.

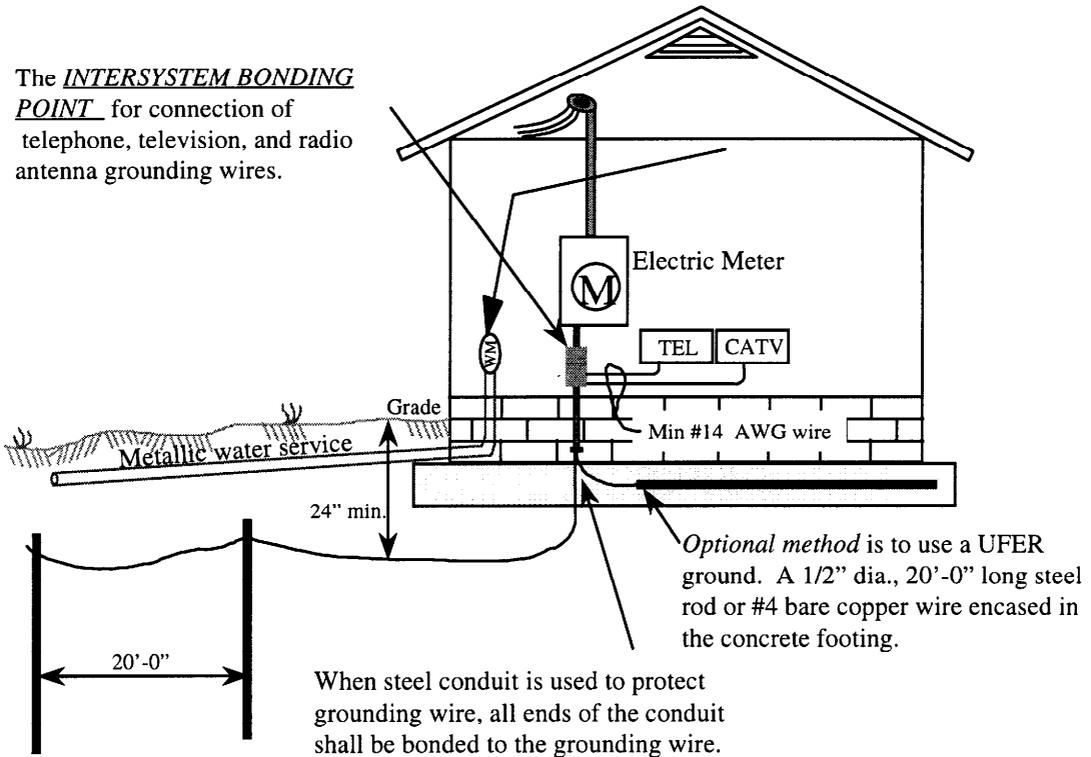


Figure 6. Recommended service grounding for enhanced surge immunity

The recommended practice to avoid the problem discussed above is an “intersystem bonding point,” as shown in figures 6 and 7. This approach addresses the issue on a facility basis. The two services, power and telephone, must enter at the same point and must be bonded together at that point. The NEC requires bonding together the two service grounds, but does not require entry at the same point. As shown in figure 5 system bonding is provided by the cold water pipe, which is electrically common to both services. But the voltage potential difference still occurs because of the separated entry points. Figure 7 shows the details of how an inter-service bonding point might be constructed and installed.

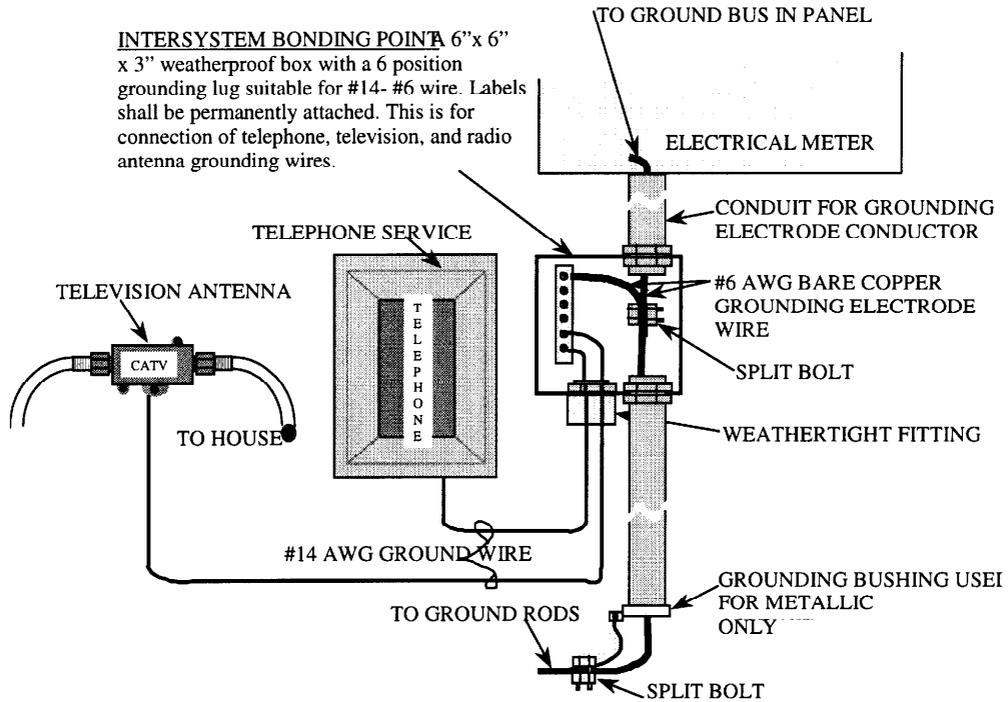


Figure 7. Details of the essential intersystem bonding point

Beyond grounding, for a surge-protective device (SPD) to be effective it must provide a protective level, or surge clamping, below a level potentially harmful to the equipment. But the level should not be so low that the device absorbs energy to the point that its life is reduced or it fails prematurely under the stresses resulting from a temporary overvoltage. This principle holds for power, signal, telephone or cable, albeit at different threshold levels. The SPD must also have a surge current handling capability commensurate with the surge currents that might occur at that location.

The application of an SPD satisfying the criteria cited above must also take into consideration the rest of the circuit where the device is to be applied. For instance, operation of the SPD must not cause adverse side effects. In the case of protection of load equipment which is connected to the power system as well as to a communication system (telephone, cable or satellite TV) particular attention must be given to this issue. It is possible that protecting the two interfaces of the equipment, each with a separate SPD, might leave the equipment in jeopardy as the result of overvoltages appearing between the two separate systems, see Key, Martzloff, [2,3].

Because effective operation of an SPD involves diverting the surge to ground, the actual grounding system and its connections in a residence must be taken into consideration. The impedance of the

grounding system to “true earth” is far less important than the integrity of the bonding of the various parts of the grounding system. As noted earlier the worst possible mistake—and a violation of the NEC—is to provide separate grounds for the power system and for the communications system. Even so, many instances are found of such multiple grounding practice in residences, either intentionally, or by accident. A typical example is services such as cable TV that used the metal pipe of an outdoor faucet as its grounding connection. This arrangement was an acceptable ground at the time of installation, but can easily be defeated when the water piping inside or outside the house is replaced with plastic. So an improper separation of grounds is camouflaged by the short link of steel pipe going through the foundation wall and sealed in the concrete.

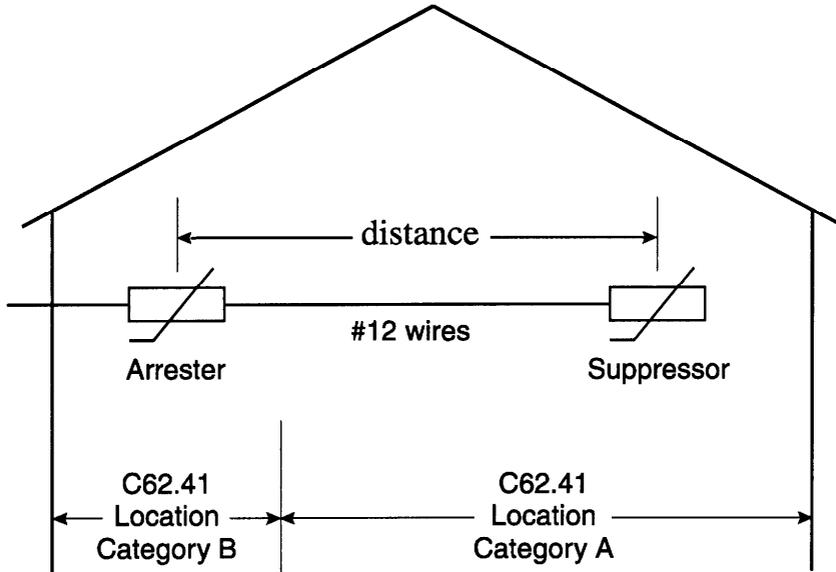


Figure 8. Cascade arrangement of upstream and downstream SPDs

OPEN AND UNRESOLVED ISSUES

Several issues remain open at this point and will be addressed in the Guide. We hope that the Guide will resolve some of these issues, but others might take more research to resolve. In some cases an iterative process will be required among end-users, utilities, and equipment manufacturers to optimize the process instead of exchanging blame.

One important problem that can be resolved by future research is the coordination of an SPD cascade, (see figure 8). If the selection of a service entrance SPD is appropriately coordinated with the installation of appropriate SPDs within the residence, then the resources will be allocated in an effective manner, both technically and economically (see Martzloff, Lai [4,5]). Clearly, this coordination can only be achieved by cooperation of the utility, SPD manufacturers, and premise occupants and full knowledge of the characteristics of the SPD's and the surge itself.

Another problem that can be resolved is the effective protection of equipment connected to two different systems. The concept of equalizing ground references has now been sufficiently advocated to motivate SPD manufacturers to offer SPDs with both power and telephone protection, or both power and TV protection, in the same enclosure (see figure 9). The IEEE designation of “Surge Reference Equalizer” was published in 1992, [6]. It has not yet found general use in the

industry, but for most residential applications the device can be found in electronics supply stores. Still, a possible problem in that solution is the absence of industry standards on the performance of surge reference equalizers. At this point, the user is left with some uncertainty as to how effective a particular brand may be compared to another. In this intensely competitive market, claims and counter claims need to be sifted via experiment, a role that some utilities might be willing to assume, or that consumer organizations should address.

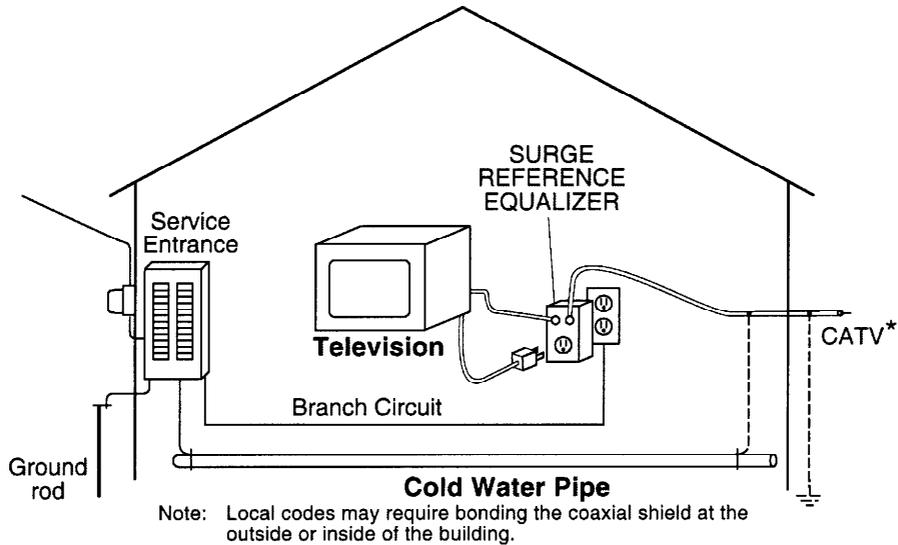


Figure 9. Surge reference equalizer to protect multi-port appliances

Last but not least, application of SPD or lightning protection must consider risk analysis. Protection of appliances against surges generated within a building, or impinging at the power service entrance, can be accomplished with relatively low cost. However additional investment may be required if multiple services (telephone, TV, and power), and multiple grounds are present. External protection against a direct lightning strike may also be needed with cost depending on the structure and location. In any case good practice should be sought in the face of an “act of God” event, where damage can be minimized by observing appropriate rules of grounding, bonding, and protection, as the Guide will propose. However, even with the best practices some degree of risk will always remain.

CONCLUSION

More attention to surge protection practices is warranted by the increased use and inherent surge vulnerability of residential electronic systems and appliances. While there is consensus on the basic principles of protection, specifics and installation procedures for cost-effective applications are not readily available. By applying data and practices for grounding, lightning and surge protections to specific appliance sets, these needed details are beginning to take form. To be successful a Consumer-Oriented Guide on surge protection will have to address complex topics of whole-house protection including such techniques as multi-port devices and cascade coordination. When complete, the Guide should provide a comprehensive document that insurance companies can use to educate their policyholders, employees, and agents about how to reduce damage resulting from surges caused by lightning.

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**DRIVING
HIGH SURGE
CURRENTS
INTO LONG
CABLES:**

**MORE
BEGETS
LESS**

Driving High Surge Currents into Long Cables: More Begets Less

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Significance:

Part 2 Development of Standards – Reality checks

Part 4 Coupling and propagation of surges

In the propagation of a surge current injected at the service entrance of a building, two significant factors can prevent the propagation of a postulated “large” surge current to the end of the branch circuits of the facility.

1. The combination of the inherent inductance of the wiring and the high rate of current change for such a current to begin flowing into the branch circuit results in a high voltage at the driving end ($V = L \times di / dt$).
2. In the absence of a surge-protective device at the service entrance, the withstand voltage of the wiring devices at the driving end – the service entrance – is very likely to be exceeded by the voltage that this rising current will develop along the branch circuit.

The resulting flashover will abort further propagation of the surge current toward the far end, thus establishing a limit to what is physically possible. If there is a surge-protective device at the service entrance, the scenario becomes a matter of cascade coordination.

The paper provides quantitative information on this limitation, as a function of wiring length and current rate of rise.

Driving High Surge Currents into Long Cables: More Begets Less

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Abstract - Reality checks can and should be applied to proposals for characterizing the surge environment and application of surge-protective devices (SPDs) to end-user, low-voltage power systems. One such check is the fact that driving a large current with steep front toward an SPD installed at the far end of a branch circuit cable could require such a high voltage that the connections at the near end of the cable will flashover, limiting the stress applied to the far-end SPD. Tests and numerical modeling were performed to support this thesis. The results of real-world measurements and modeling, presented in the paper, are in good agreement and validate each other. From that point on, the model allows parametric variations of cable length and surge current amplitude and waveform, of which several examples are presented.

I. INTRODUCTION

In the never-ending quest for better data on the frequency of occurrence and level of threat of overvoltages, we should not overlook some "reality checks" that can be applied to proposals for characterizing the surge environment. One such check is the fact that forcing a large surge current with steep front toward a surge-protective device (SPD) installed at the far end of a branch circuit cable could require such a high voltage that the wiring device connections at the near end of the cable will flashover, limiting the stress applied to the far-end SPD.

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Large surge currents considered by standards-writing bodies and discussed in this paper are presumed to impinge from the outside of a building, as a result of a direct or indirect lightning flash. These involve postulated rise times in the order of a few microseconds, with a duration ranging from a few tens to a few hundreds of microseconds. While there are different propositions made on what duration should be considered as "representative" waveforms, there is a consensus on rise times ranging from about 4 μ s to 20 μ s [1]. However, consensus on what value to select for "representative" amplitude(s) has been challenged by proposals to increase the current surge capability of devices intended for installation at the end of branch circuits.

A growing trend in the application of SPDs to residential or commercial installations is to provide "whole-house protection" with an *upstream* SPD connected at the service entrance, and *downstream* SPDs in the form of plug-in devices installed at receptacles. Selecting the ratings for these two devices is the subject of some debate. The *voltage rating* of the devices introduces the issue of cascade coordination which has been addressed at length in the literature [2]-[8] and will not be discussed here. At this point in time, the vast majority of installations do not include an upstream SPD intentionally connected at the service entrance, other than a gap in the revenue-meter socket. This gap is provided by the meter manufacturer to protect the meter more than the downstream installation. Nevertheless, there are other "gaps" at the service panel -- the clearances of the wiring devices, which have some limits to their voltage withstand capability.

II. SURGE PROPAGATION IN WIRING

The possibility of a clearance flashover is the basis of our thesis: If a large surge current is postulated as propagating downstream (and then taken as a requirement for the downstream SPD), the propagation characteristics of this surge current would result in high voltages at the service entrance, upstream. In turn, the high voltage would cause flashover of upstream clearances, acting as a relief valve for the surge energy headed for the downstream SPD. This relief action would then contradict the proposed requirement for high energy-handling capability of the downstream SPD. Thus, appropriate selection of *current ratings* for the downstream SPD, in the light of our thesis, should take into consideration this reality check that defines an upper limit for the current rating required for the downstream SPD.

The surge propagation characteristics mentioned in the preceding paragraph are controlled by three parameters: the impinging surge, the impedance of the wiring from the service entrance to the downstream SPD, and the I-V response of the downstream SPD. The impinging surge could be considered either as a voltage source or as a current source. The present consensus is to consider it as a current source, resulting from the coupling and subsequent division of a lightning surge, part of which impinges on a given service entrance.

The impedance of the wiring is that of two parallel wires of known dimensions and separation. It can be represented either by lumped parameters -- series R and L and parallel C -- or by a "short" transmission line. The reason for placing quote marks around the qualifier of "short" is that the term is to be viewed by comparing travel time over the length of the transmission line and duration of the traveling pulse -- another subject discussed in the literature [9] that we will not discuss here, with the exception of a brief comparison of results obtained when modeling the propagation with lumped parameters or with a transmission line.

When using the lumped RLC model, during the rise of the surge current, the significant parameter of the wiring impedance is its inductance, L . The voltage at the upstream end resulting from driving the surge current into such an impedance is primarily $L \times di/dt$, with di/dt determined by the amplitude and rise time.

By performing surge measurements on real-world wiring components, followed by numerical modeling with the Electro-magnetic Transients Program (EMTP)² [10], this proposition can be verified and applied to a range of postulated surge waveforms and typical configurations found in the premises wiring of low-voltage systems. These results will allow developing realistic recommendations for the rating of SPDs offered for surge protection at the equipment location -- either as plug-in additions by the end-user, or as permanently wired devices at the end of typical branch circuits. The measurement results also show the need to consider the possibility of "blind spots" in the protection schemes, and illustrate our title paradox of "*more begets less.*"

Measurements were conducted on a simple circuit consisting of 9 meters of nonmetallic jacket cable typical of residential installations, with a metal-oxide varistor connected downstream at the far end. A Combination Wave surge generator, suitable for producing the waveform described in IEEE/ANSI C62.41-1992 [1] was used to inject a surge current at the upstream end of the cable. Current and voltage waveforms were recorded. The current waveform resulting from this injection was duplicated in a closed-form equation to be applied as the postulated surge current injected into the EMTP model of the circuit, allowing computation of the corresponding voltages.

² Certain commercial instruments and software packages are identified in this paper in order to adequately specify the experimental procedure. Such identification does not imply recommendation or endorsement by the National Institute of Standards and Technology, nor does it imply that these are necessarily the best available for the purpose.

III. MEASUREMENTS AND MODELING

A. Characterizing the varistor

First, the varistor to be connected at the far end was tested to determine its I-V response and demonstrate that the model to be used for this highly nonlinear component would be adequate to simulate its behavior in the circuit when connected at the downstream end. Figure 1 shows the test circuit used for making that measurement. The surge generator used for the tests was the KeyTek 711 with a P7 wave-shaping output network.

The varistor used in these tests was a 20-mm diameter metal-oxide varistor (MOV) disc, rated 130 V rms (200 V at 1 mA dc). The inductance L_p shown in series with the varistor is not a deliberate addition of a real component, but is the representation of the coupling between the loop where the surge current flows and the voltage measurement loop formed by the varistor leads and the two probes used for the differential measurement. That inductance is included in the model as a discrete series inductance, with a value of 0.5 μ H selected to emulate the observed voltage at the point of measurement -- which is not the "pure" varistor voltage, as discussed in the narrative of Figure 2.

Figure 2 shows the recording obtained for a particular setting of the surge generator, and Figure 3 shows the result of modeling the circuit shown in Figure 1 for an injected current surge corresponding to the actual current surge recorded in Figure 2. The equation used for the modeling is a damped sine wave that allows a close approximation of the current delivered by typical Combination Wave generators into inductive loads [7]. It is known that actual generators tend to produce an "undershoot" when connected to an inductive load, and this test was no exception. However, computational artifacts occur when using a simple damped sine wave because its di/dt derivative (a cosine) is not zero at time zero. Furthermore, we know that nature does not allow an instantaneous jump of current from zero to a steep rise. By adding a multiplier term $[1-e^{-t}]$, these artifacts are eliminated and the waveform has a "gentle toe" which is a better model of reality. This improved equation is then:

$$I = 4200 * \sin(0.126t) * e^{-(t/28.1)} * [1-e^{-t}] \quad (1)$$

with I in amperes and t in microseconds.

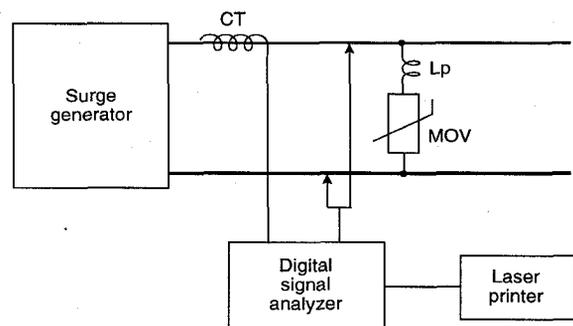
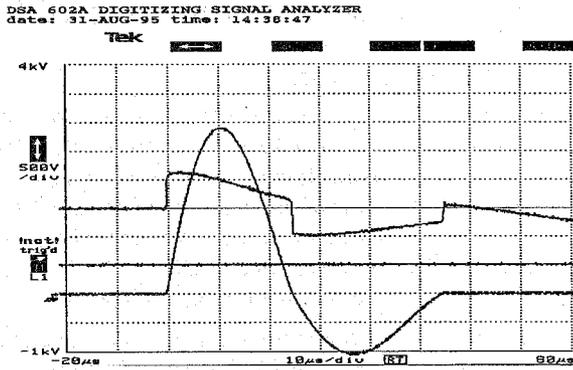


Figure 1 - Test circuit for determination of the I-V characteristics of the varistor



Top trace: Voltage, 500 V/div
(Center trace: inactive)
Bottom trace: Current, 500 A/div
Sweep: 10 μ s/div

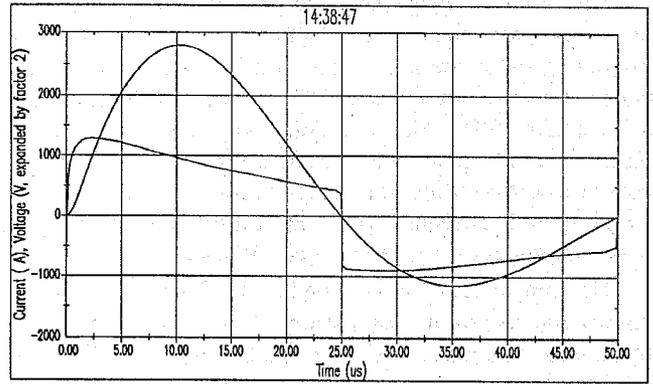
Figure 2 - Real-world recording

Inspection of Figures 2 and 3 clearly shows the agreement between real-world measurements³ and model, and thus merits some observations. One might have expected a flat-top voltage waveform reflecting the clamping action of the varistor. Instead, a drooping waveform is observed. This droop is caused by the parasitic inductance L_p in series with the ideal varistor. At the time of current peak ($di/dt = 0$), the "true" varistor voltage is seen on the oscillogram. Before the peak, the positive $L_p \times di/dt$ adds a spurious voltage to the recording. After the peak, the negative $L_p \times di/dt$ subtracts the spurious voltage.

These observations are significant in appreciating the all-important inductive effects during the rise and fall of a surge current in the wiring of branch circuits. The issue of the importance of inductance versus other circuit parameters [11] hopefully has been put to rest by the surge and impedance measurements with corresponding computations performed in the so-called "Upside-Down House" [12], a real-world replica of a typical residential wiring system. In [12], it was shown that inductive effects prevail, so that *rate of rise* of the surge current and *circuit inductance*, more than any other parameter, are the significant parameters for the voltage necessary at the upstream end to drive a given current into the branch circuit.

The model used in the simulation for the varistor is derived from the published varistor I-V characteristic (general shape and slope of the curve) with one specific point defined by the "true" varistor voltage read from the oscillogram of Figure 2 at the point of zero $L_p \times di/dt$ contribution. In turn, this varistor model will be used for the modeling of a varistor connected at the downstream end of a branch circuit, as discussed in the following reported measurements and simulations.

³ The measurements reported in this paper have been made with instrumentation for which the cumulative uncertainty should not exceed 5 to 6%. Given the process of applying the measurement results to the response of surge-protective devices exposed to environment with characteristics that are at best known within an order of magnitude, this level of uncertainty does not affect the practical conclusions.



Note: the voltage trace has been expanded by a factor of 2 to enhance resolution on the vertical scale.

Figure 3 - Modeling the circuit of Figure 1 with the impinging current set to match the test current, as shown in Figure 2

B. Measurement and modeling with varistor installed at the downstream end of a branch circuit

The circuit of Figure 4 shows the varistor characterized by the test and modeling in the preceding paragraphs, connected at the downstream end of a "branch circuit" consisting of two copper conductors of 2-mm² cross-section (#12 AWG) with solid insulation and a separation of 6 mm between centers. The first current transformer monitors the total current impinging at the upstream end. The second current transformer monitors the current flowing toward the downstream end, which will be imposed on the varistor. The clearances at the upstream end, such as clearances in a service-entrance panel, are represented by a discrete gap that will be set to produce sparkover at some given voltage during the test as well as in the model.

Figure 5 shows the recording obtained with the circuit of Figure 4, with the surge generator left at the same setting as that used for Figure 2. To determine the response of the circuit without the clearance limitation, the gap setting was adjusted for this test so that no sparkover occurred at the upstream voltage developed for the current delivered by the generator.

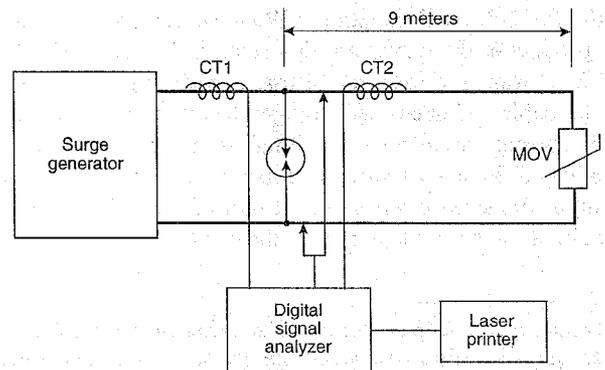


Figure 4 - Test circuit for determination of the voltage necessary at the sending end to drive a given current into the far-end SPD

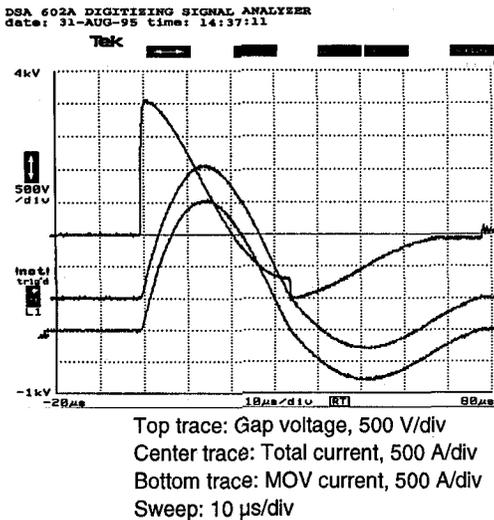


Figure 5 - Real-world recording of sending-end voltage with gap set for no sparkover

Comparing the traces of Figure 5 and Figure 2, the addition of the inductance of the 9 meters of branch circuit changes the load on the surge generator, reducing the current peak from the 2.8 kA in Figure 2 down to 2 kA in Figure 5.

The two current traces of Figure 5 are identical. Since there is no current diverted by the gap, the current in the branch circuit is the same as the current delivered by the surge generator.

Another effect of the added inductance is the increase in the time from origin to the first current zero, 33 μs in Figure 5, compared to 25 μs in Figure 2. In the subsequent model, that change of the actual impinging current surge is taken into consideration by modifying the current equation as follows:

$$I = 3571 * \sin(0.095 t) * e^{-(t/26.1)} * [1 - e^{-t}] \quad (2)$$

with I in amperes and t in microseconds.

Turning to the modeling, Figures 6 and 7 show the waveforms of the impinging current, as defined by Eq. (2), and the resulting voltage at the upstream end. To address some concerns expressed by colleagues in discussions of this subject, the EMTP modeling was also done with the transmission-line model which is readily available in the EMTP code. Figure 6 was obtained with the lumped-parameter circuit model, and Figure 7 was obtained with the transmission-line model.

Inspection of the two figures reveals no difference in the results. The only difference is in the consumption of computing time: with the transmission line model, the computation time-step has to be significantly shorter (0.02 μs in this case) than the travel time for the reflections, while in the case of the lumped model, the time-step can be longer (0.1 μs in that case). The result is that the simulation of Figure 6 took 43 seconds on a 486-based PC, compared to 263 seconds for Figure 7. Therefore, the lumped-parameter model is perfectly adequate to represent reality, and performing a transmission-line analysis [5] is an unnecessary consumption of computing time and resources.

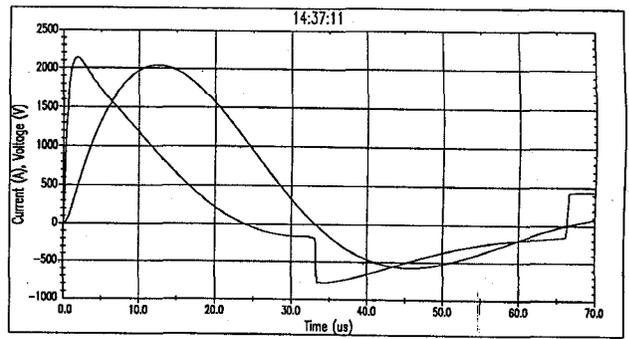


Figure 6 - Impinging current and resulting upstream voltage as computed with lumped-parameters model

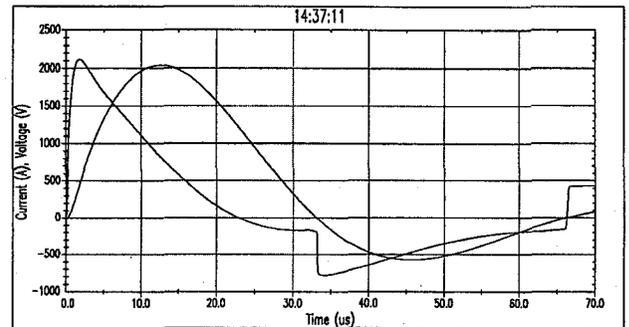


Figure 7 - Impinging current and resulting upstream voltage as computed with transmission-line model

In both Figures 6 and 7, the effect of the branch circuit inductance on the resulting voltage is apparent as the peak voltage occurs at the beginning of the rise (as soon as the “gentle toe” effect ceases), not at the peak of the current. The step change in the voltage trace corresponds to the reversal of the current in the varistor, showing the relative contributions of the varistor effect and of the inductive effect as seen from the upstream end.

Table 1 below shows the results of such computations for the waveform of Figures 5, 6 and 7. As mentioned above, the insertion of an inductance in the load connected to the surge generator increased the rise time beyond the standard 8 μs. In making the parametric computations, we chose to stay with this 10 μs value to maintain continuity with the test/model validation.

TABLE 1

Upstream voltage (in kV) necessary to drive a current of the peak value shown (columns) and rise time of 10 μs into a branch circuit of length as shown (rows), terminated with a 130-V rated varistor

Length \ Peak	2 kA	3 kA	5 kA	7 kA	10 kA
10 m	2.3	3.3	5.2	7.2	10.1
30 m	5.8	8.5	13.9	19.4	27.0
50 m	9.3	13.7	22.7	31.6	45.0

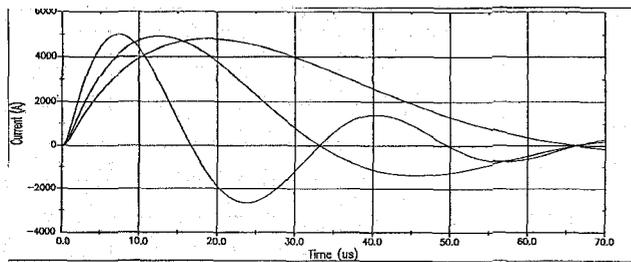


Figure 8 - Three surge current waveforms with different rise times used to compute the values of Table 2

Figure 8 shows three waveforms of same amplitude, with nominal rise time of 5 μ s, 10 μ s, and 20 μ s, obtained by taking half or double of the frequency used in Eq. (2). The actual rise time [$1.25 \times$ (time from 10% to 90%)], as opposed to the nominal rise time used to describe the waveforms, was computed as well as the maximum rate of rise for each wave. The maximum rate of rise (which is obtained when the second derivative of the current is equal to zero) occurs initially, once the gentle toe is over, and determines the maximum resulting voltage produced by the inductive effect. Table 2 shows the corresponding values of the rise time, maximum rate of rise, and resulting voltage for a branch circuit length of 10 m and amplitude of 5 kA. Note that for a 1-to-4 increase in nominal rise time, the maximum di/dt decreases only by one half, with the same decrease appearing in the resulting voltage, showing once again that initial rate of rise is more important than rise time and amplitude.

TABLE 2

Effect of the rate of rise of the postulated current on the resulting voltage at the upstream end of the branch circuit

Nominal rise time, μ s	5	10	20
Actual rise time, μ s	4.3	9.5	13.5
Maximum di/dt , A/ μ s	1250	850	630
Resulting voltage, kV	7.0	5.2	3.6

In the scenario tested and modeled so far, no flashover possibility was considered. Nevertheless, the values shown in Table 1 clearly indicate that some real-world circuit lengths and surge parameters postulated in some SPD application standards under development can produce high upstream voltages that will cause a flashover of the upstream wiring devices.

C. The paradox of "more begets less"

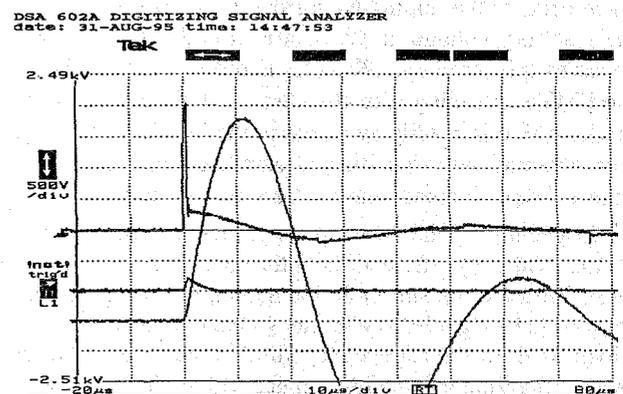
Common-sense intuition might lead the unwary to expect that higher surge currents would impose a greater stress on the circuit components, including the downstream varistor. Also, a longer branch circuit, with its corresponding higher inductance, could be expected to have the capability of storing more energy during build-up of the surge current toward the downstream varistor, into which that stored energy ultimately has to be dissipated. Cascade coordination studies [4], [6], [8], have shown that in some cases, the downstream varistor continues to carry current long after the impinging surge current has gone past its peak.

To explore the validity of such expectations, we performed tests and modeling, with an actual gap in the test circuit, and a switch in the model circuit, to bypass the current at the upstream end when sparkover voltage is attained. By measuring the current that flows in the branch circuit toward the downstream varistor and the voltage across the varistor, the energy deposited in the varistor during the total surge event can be determined. Likewise, the modeling can determine the current in the varistor, hence the voltage across it, and allow computation of the energy.

In [4], agreement was reported between, on the one hand, computing the deposited energy through actual measurement of the current and voltage, followed by computation of the energy by means of the digital signal analyzer used for measurements and, on the other hand, the model computations. Therefore, in the tests reported here, we were satisfied to verify waveform agreement between the actual varistor current measurement and the computed varistor current, and let the model alone compute the energy deposited in the downstream varistor.

Figure 9 shows the real-world recording of the situation that develops for a "clearance" sparkover of 2 kV. This relatively low value, compared to the 6 kV to 10 kV level that we might expect from typical low-voltage wiring devices, is made necessary for the test case where only 9 meters of branch circuit were considered, and the setting of the surge generator was maintained at the same nominal 3 kA short-circuit current. The object, of course, is to demonstrate that the clearances are likely to flash over, as indicated by progressively higher values of the necessary upstream driving (or resulting) voltage shown in Table 1.

Under the conditions of Figure 9, sparkover of the gap occurred at approximately 1 μ s. After sparkover, the current delivered by the surge generator is the sum of the currents in the gap and in the branch circuit. Its peak (3.2 kA) is greater than those of Figures 2 and 5 because the generator does not need to overcome the varistor that reduced the voltage available for driving the current, nor the impedance of the 9 meters of cable.



Top trace: Resulting voltage, 500 V/div
Center trace: MOV current, 500 A/div
Bottom trace: Total current, 500 A/div
Sweep: 10 μ s/div

Figure 9 - Voltage and currents with gap sparkover at 2 kV

Figures 10 and 11 show the results obtained by the model for voltages and current in the circuit. In the modeling, only one current waveform was applied to the circuit, the one prevailing until flashover occurs, which the postulated current-source real world would maintain. In contrast, the surge current delivered by the surge generator (Figure 9) increases after the flashover, but that is not relevant to our consideration of what happens to the circuit before and up to the time of flashover.

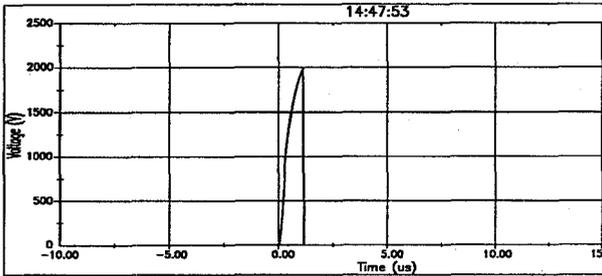


Figure 10 - Voltage across the gap set to sparkover at 2 kV

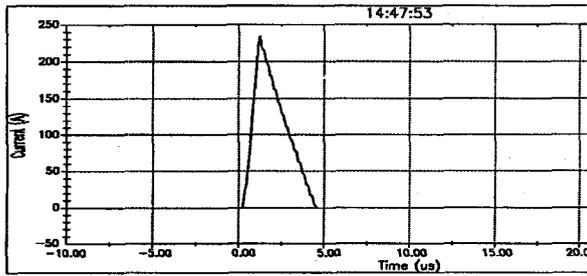


Figure 11 - Current in downstream varistor

The waveforms of Figures 10 and 11 are shown with an expanded scale, compared to that of Figure 9, that gives a better resolution for the gap voltage and current in the varistor. There is good correspondence between the waveforms of the two traces and the gap voltage and downstream current traces of Figure 9. In Figure 10, however, the gap voltage collapses to zero, while it does not in Figure 9. The difference is that the real-world circuit has a parasitic inductive voltage added to the true gap voltage, already discussed for the varistor of Figure 2. Figure 11 shows the linear ramps typical of current changes in an inductance.

As mentioned above, we can expect that the energy deposited in the downstream varistor for a given impinging surge will be influenced by the length of the branch circuit. Using the model developed and validated according to Figures 5 and 6, the energy can be readily computed. In the case described by Figures 9, 10, and 11, the gap sparkover voltage was preset at 2 kV so that sparkover could indeed occur for the surge current available from the real-world generator and the resulting upstream voltage.

Now that we are in the (validated) model-world, we can arbitrarily set the sparkover voltage at a level more typical of the flashover point of clearances, say 6 kV. Of course, we have the possibility of assessing energy for a wide range of parameters.

In the example reported below, we kept the same three values of branch circuit length and performed the computations for the same five values of impinging current as those used for the computations of Table 1. Table 3 shows the energy deposited in the downstream varistor for these combinations of branch circuit length and peak current values, for the applied current waveform of Figure 5, and a 6 kV flashover point.

TABLE 3

Energy deposited into a 130-V rated far-end varistor as a function of the branch circuit length shown (rows), current peak (columns) of waveform shown in Figure 5, and flashover of the clearances set to occur at 6 kV

Peak/Length	2 kA	3 kA	5 kA	7 kA	10 kA
10 m	17 J	27 J	51 J	670 mJ	218 mJ
30 m	17 J	128 mJ	30 mJ	23 mJ	18 mJ
50 m	69 mJ	34 mJ	17 mJ	11 mJ	10 mJ

The results shown in Table 3 merit close examination as they reveal some counter-intuitive trends: we might have expected that for higher impinging current values, the resulting energy deposited in the downstream varistor would be higher. Likewise, we might also have expected that for a longer branch circuit, the greater inductance would store more energy, ultimately to be deposited in the varistor. In fact, the opposite occurs. The table also reveals the interesting finding that the first three lower-current, short-line cases (**bold face type in the table**) produce larger energy deposition, compared to the other cases. Actually, the explanation that follows is simple and might be anticipated (especially with hindsight, illustrating that intuition is a hazardous process when dealing with nonlinear circuit components).

Starting with the second observation (more joules at lower threat levels), we have a beautiful illustration of the *blind spot* effect -- not limiting tests and designs to the maximum stress of a worst-case scenario -- [13]: for 10 meters of circuit and at the lower current levels, the resulting voltage at the clearance is not sufficient to cause flashover, and all the energy has to go to the downstream varistor. At the higher threat level of 7 kA, the voltage produced in the inductance of 10 meters of line, added to the varistor voltage, is sufficient to sparkover the 6 kV gap, relieving the varistor from further involvement beyond that of discharging the energy stored in the line. In the case of the 30-m long line, this transition occurs between 2 kA and 3 kA.

Turning now to the first observation, that higher current or greater inductance result in less stress, this apparent paradox is caused by the fact that with the higher values of di/dt and L , the voltage at the clearance rises more quickly to the flashover point. Consequently, the build-up of energy in the line inductance is shut-off earlier so that the current level in the line reached at that point is lower and, in spite of the greater inductance, the stored energy $\frac{1}{2} L i^2$ is lower for higher applied current peaks and longer branch circuits.

IV. CONCLUSIONS

The development of a validated EMTP model using existing computational tools allows us to look into all scenarios of surge propagation and surge mitigation schemes. The reality check proposed by the measurements and modeling reported in this paper should be useful in the process of selecting stress levels to be specified in the application of SPDs downstream from the service entrance, from the point of view of successful cascade coordination as well as integrity of electromagnetic compatibility. Specific conclusions can be drawn:

1. Realistic surge current amplitudes and rise times can be defined for SPDs installed at the end of branch circuits, with upper limits set by the laws of physics applied to real-world conditions.
2. The general practice for describing surge waveforms is to cite "rise time" or "front time", followed by duration, as in 8/20. However, when the effects of circuit inductance are assessed, in particular by numerical modeling, the maximum rate of rise must be considered, not an average over the rise time. It is especially important to define the conditions at the origin of the waveform, such as inclusion of a gentle toe.
3. The importance of looking for blind spots is, once again, demonstrated by the parametric computations, a much simpler task than exhaustive equipment-exhausting tests.
4. Reliable computational tools make it possible to obtain a wide range of parametric assessments, and thus avoid recourse to intuition when dealing with nonlinear circuits, where blind reliance on common-sense may lead to flawed conclusions.
5. The parametric computations offered in the paper point out the need to consider a balance or trade-off among several critical factors in the design of branch circuit protection, in particular the uncontrollable length of branch circuits in actual installations.

V. ACKNOWLEDGMENTS

The measurements reported in this paper were conducted by Kermit Phipps at the EPRI Power Electronics Applications Center. Support for the modeling was provided by the Electric Power Research Institute. Support for the development of this reality check, motivated by the discussions and contributions of IEEE and IEC colleagues, was provided by Delmarva Power & Light and by Pacific Gas & Electric.

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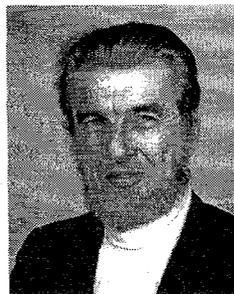
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François Martzloff (M'1956, F'1983) Born and educated in France, with additional MS degrees from Georgia Tech and from Union College, worked at General Electric for 29 years and now ten years at the National Institute of Standards and Technology. He is contributing to several committees for development of standards on EMC and surge protection in the IEEE and the IEC.

Discussion

M. Darveniza (University of Queensland, Australia 4072):

The authors are to be congratulated for drawing attention to an important feature in surge protection. Namely, the magnitude of the voltage up-line from a surge protective device will exceed the protective level of the SPD, the extent of the over-voltage depending on distance and on waveshape of the incident surge. Because of this, flashover at an upstream device (for example, another SPD) will limit the severity of the surge stressing the downstream SPD.

Two examples are offered which support the author's statement "more begets less" and which can be rephrased by saying that in some cases, a less onerous surge may impose more severe overstress than a more onerous incident surge. The two examples are:

1. A cable-entry substation protected by an upstream SPD connected at the overhead line-to-cable junction. The most severe stress at the substation occurs when the surge incident from the line onto the cable is just not large enough for operation of the SPD at the line-cable junction.
2. A hybrid surge protection system for low-voltage and electronic equipment involving two SPD's coordinated by an intervening series impedance. The series impedance is selected to ensure that the downstream SPD is not overstressed, by virtue of operation of the upstream SPD caused by the voltage drop in the impedance (which adds to the clamp voltage of the downstream SPD). However, if the voltage drop is not large enough to "turn-on" the upstream SPD, either because the magnitude or the steepness of the incident surge current is not

sufficiently large, then the downstream SPD may still be overstressed if the duration of the surge current is too long. Paradoxically, a more severe incident surge will "turn-on" the upstream SPD, thus protecting the downstream SPD from excessive overstress.

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François Martzloff :

We are glad that the message we were presenting has found a favorable echo with Professor Darveniza, and appreciate his kind words. The two examples he cites are indeed good illustrations of the "more begets less" theme which we have expressed in the manner of a paradox/epigram to make it easy to remember. This reality check on the likelihood of a stress-limiting flashover should be applied whenever a scenario is proposed that involves the propagation of surge currents. We hope that our colleagues involved in standards development will remember this well and assess some of the proposals now under consideration for SPD requirements in the light of that epigram.

Manuscript received November 13, 1996.

**THE EFFECT OF
NEUTRAL
EARTHING
PRACTICES
ON LIGHTNING
CURRENT
DISPERSION IN
A LOW-VOLTAGE
INSTALLATION**

The Effect of Neutral Grounding Practices on Lightning Current Dispersion in a Low-Voltage Installation

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Significance:

Part 2 Development of standards – Reality checks
Part 4 Propagation and coupling of surges

In the case of a direct lightning stroke to a building, the earth-seeking current is dispersed among all available paths to earthing electrodes, including intentional made electrodes and opportunistic electrodes. A substantial part of that current will exit the building via its connection to the power distribution system.

The configuration of this power distribution system (daisy chain from the transformer or radial from the transformer) influences the sharing of the current among these possible paths.

From simulations performed with a 10/350 waveform, the paper provides quantitative information on these effects.

The Effect of Neutral Earthing Practices on Lightning Current Dispersion in a Low-Voltage Installation

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Abstract - Computer modeling with the EMTP code has been applied to several configurations and earthing practices in use in various countries to show the effect of any differences in the dispersion (sharing) of a lightning stroke current among the available paths for the earth-seeking lightning current. Simplifying assumptions have been made to some details of the configurations to focus on the main difference -- earthing practices. Identifying such differences provides the necessary perspective on their significance and the strong need to take them into consideration when developing international standards on surge-protective device applications.

I. INTRODUCTION

When designing a lightning protection scheme for a low-voltage power system within a building, several scenarios must be considered for the point of termination of the lightning stroke. Common wisdom classifies these by decreasing order of severity: directly to the building, directly to overhead low-voltage distribution lines (or other utilities) outside of the building, to other objects near the building, distant cloud-to-earth strokes, and finally perhaps cloud-to-cloud discharges. Several standards-writing projects are underway, at the IEEE and at the IEC, based on present knowledge of the lightning flash characteristics and on assumptions about the way the lightning current divides among the many paths available for distributing (dispersing) this current to the ill-defined "earth" which is the termination of the cloud-to-earth strike.

The purpose of our paper is to show the effect that different practices for neutral earthing in the low-voltage distribution system can have on the relative dispersion of the lightning current which is seeking the path of least impedance to earth.

To accomplish this purpose in an eight-page paper, and to concentrate on the essential difference, the models we present are simplified from the detailed reality, so that one of our first tasks will be to explain and justify the simplification. To avoid confusion in the meaning of the word "distribution" which can relate to the distribution of electric power by the utility or to the distribution of the lightning current among the available paths, we will use the term "dispersion" for the second meaning, lighting current dispersion. Another term used by some authors to convey the concept is "sharing" (among available paths). Note that the actual return stroke actually goes from earth to cloud in the majority of cases, but the scenario is generally described as if the stroke "terminated" on earthbound objects.

In the case of a low-voltage power distribution system, different countries have adopted different practices on earthing the neutral conductor, and writing a history of why that is so would give an interesting insight into the development of power systems. The fact is that today, two approaches are well entrenched in their respective territories, the so-called TN system and TT system where the difference lies in the mode of earthing the neutral. We will give a brief overview of the differences in a following section. Our purpose is to show how the difference in these practices affect the sharing, or dispersion, of the lightning current among the available paths to earth, and consequently affect the rating of surge-protective devices which may be included in these paths. We used the EMTP simulation code [1] to model several scenarios in each of the TN and TT systems, with small but possibly significant differences in the configuration. By postulating a direct stroke to one building, and requesting EMTP to compute currents in the (simplified) complete power system, we obtained results for the two most severe cases of lightning termination: the case of a direct stroke to one building, and the case of a nearby stroke which propagates and impinges at the service entrance of many buildings on that part of the low-voltage distribution system.

The literature and draft standards contain many examples of such scenarios, but it seems that each is confined to a specific approach or power system configuration with fairly detailed arrangements of load connections. The result is that from this plurality of examples, it is difficult to extract a clear perception of the significant parameters in the dispersion of the lightning current resulting from different earthing practices. In this paper, we will simplify the scenarios to concentrate on the fundamental difference between the neutral earthing practices.

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II. THE TT AND THE TN SYSTEMS

The IEC has promulgated a letter code system describing the arrangement of the neutral earthing in single-phase and polyphase power systems [2]. For the purposes of our paper, we can summarize the TT system as being a distribution system where the neutral is earthed only at the distribution transformer secondary, and the protective earth in a building is obtained from a local earth electrode. This system is used in some countries. The TN system has its neutral earthed at any available opportunity outside of a building, including the distribution transformer secondary, some or all poles, and the service entrance. In the United States, an "Equipment Grounding Conductor" (EGC) is created at the service entrance, bonded to the incoming power system neutral and to the common local earthing point, after which the neutral conductor and the EGC are carefully (and by mandate from the National Electrical Code [3]) kept separate from one another.

III. NECESSARY SIMPLIFICATIONS

Another difficulty in making a detailed comparison of results from different authors is that different models are often used. When apparently different results are reported, a lingering question is that of differences attributable to the simplifying assumptions and possible modeling artifacts. We have used the well-known EMTP code [1] for which our previous experience in cross-validation between the computer model and full-scale experimental measurements [4], [5] gave us great confidence in the validity of the results.

The literature offers many contributions on the system simulation but our purpose is not literature review -- again, our purpose is only to focus on the neutral practices considerations. However, to support some of our postulates, we will cite some papers to show that in the maze of assumptions, simplifications, and simulations, we are not alone.

A. Down-conductor representation

Some authors have included in their modeling a down-conductor feeding the stroke current to the common bonding point of the building [6]. In our model, since we postulate that the current is delivered from a current source, the impedance of the down-conductor has no effect on the current being injected at the common bonding point which is the point at which dispersion (sharing) begins. Therefore, we did not include a down-conductor in our models.

B. Earthing impedance as a function of time and current

Some authors consider the fact that the exact value of the earthing impedance is variable as a function of time and current level. For instance, [6] initially proposes a model involving resistance, capacitance, and inductance, with some dependency on time or current, or both. But after studying the problem closer, the authors of [6] conclude that a reasonable approximation is merely a fixed 10- Ω resistance. We have used this value in our models of the building earthing, and postulated an improved, lower 5- Ω resistance at the earthing electrode of the distribution transformer.

C. Other available current paths

Some standard proposals include telephone, water and gas connections as possible paths for the earth-seeking lightning current. Considering that the telephone service is a balanced system normally isolated from earth (until a network interface device becomes involved), that some water and gas services can include a cathodic-protection isolation or be implemented with plastic pipes, we chose a conservative approach of not including these as additional paths to earth.

D. Actual Circuit Configuration for Service Entrances

Figure 1 shows a schematic of a single-phase 3-wire TN 120/240-V service to a building. One surge-protective device (SPD) is connected between each of the two lines and the common earth at the service entrance, ignoring any SPDs within the building under the assumption that in a well-coordinated cascade [7] the majority of the current is carried by the service entrance SPD which has the lowest limiting voltage in the installation. The stroke current, postulated to have terminated on a point of the earthing system of the building, can seek a path to earth in two ways: directly through the earth electrode of the building, and by means of the three conductors back toward the power system.

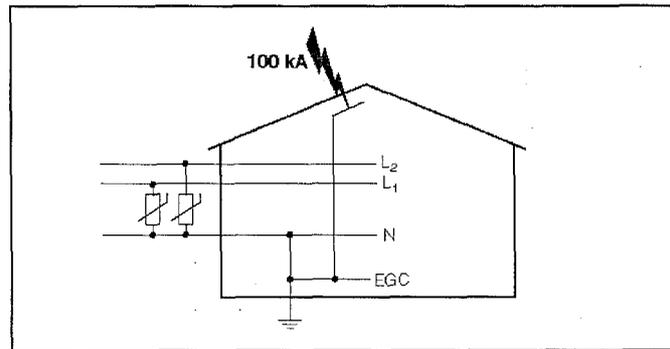


Figure 1. Service connections in a 3-wire TN system

Figure 2 shows a schematic of a three-phase 4-wire TT 230/400-V service to a building. A dedicated protective earth is created and connected to a local earth electrode, while the incoming neutral of the power distribution system is not bonded to this protective earth. At the service entrance, SPDs are connected between the local earth and each of the incoming lines and the neutral.

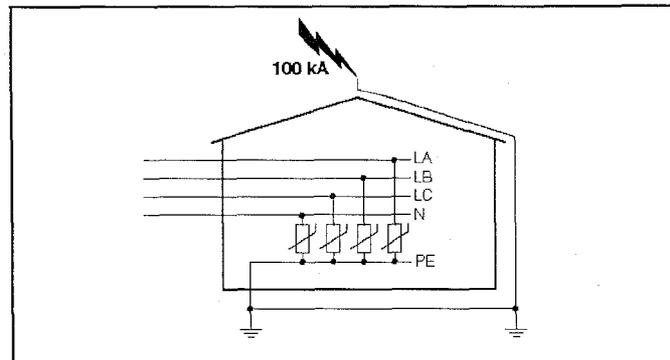


Figure 2 - Service entrance connections in a 3-phase, 4-wire TT system

E. Postulated lightning stroke current

While some authors propose a 200 kA, 10/350 μ s surge [8], [9], others suggest that even a 100 kA peak might already be too high a value [6], [10], [11]. In agreement with the latter three references, we postulated a 100 kA peak, 10/350 μ s surge current. This selection also offers the convenience that when we report current levels in kiloamperes in the various circuits, the numbers also represent the percentage of the sharing, making it easier to follow the process. Since many standards for surges impinging on SPDs (at the service entrance) are still based on an 8/20 μ s current waveform, we will also show one example of the energy deposition in the SPDs when such an 8/20 μ s surge is postulated.

The surge currents are modeled using the EMTP Type 60 Slave Source. Using the "Freeform FORTRAN" expression, any surge current waveform that can be expressed as a closed-form equation can be used as signal source in the main EMTP program. The equations for the 10/350 μ s and 8/20 μ s waveforms with a 100 kA peak are respectively (1) and (2) below:

$$10/350 \mu\text{s}: I(t) = [I_p/\eta] [\exp(-t/\tau_1) - \exp(-t/\tau_2)] \quad (1)$$

$$\begin{aligned} \text{where } I_p &= 100 \text{ kA} \\ \eta &= 0.9542 \\ \tau_1 &= 480 \\ \tau_2 &= 4 \end{aligned}$$

$$8/20 \mu\text{s}: I(t) = A I_p t^2 \exp(-t/\tau) \quad (2)$$

$$\begin{aligned} \text{where } I_p &= 100 \text{ kA} \\ A &= 0.01243 \\ \tau &= 3.911 \end{aligned}$$

(In both equations, t and τ 's are in μ s; $I(t)$ is in same units as I_p)

F. Influence of Distribution Transformer Simplification

The presence of distribution transformers has been included in many models in the literature, but their characteristics are not the same among authors. Some authors have used a coupled inductor with parasitic capacitor to represent the inter-winding capacitor in the transformer model [12].

While these models are more accurate in studying transformer failure modes due to low-side surges, for our main focus which is current dispersion among available paths, we have chosen the simple model postulated in [8] of a simple inductor to represent the winding. As results show, the presence of a transformer at the far end of a daisy-chain low-voltage distribution system does not have considerable effect on the results. Therefore, we felt justified in adopting the same transformer model as described in [8] for all of our circuit configurations.

G. Simplifying the Circuit for Modeling

The circuit impedances have been modeled in EMTP using discrete components. The wiring between buildings and from building to transformer is modeled as a series inductance with the following parameters: $R = 1 \text{ m}\Omega/\text{m}$ and $L = 1 \text{ }\mu\text{H}/\text{m}$, typical values for aluminum conductors of 34 mm^2 cross section (#2 AWG) [13]. The SPDs are modeled using the EMTP Type 92 Nonlinear Element model. Because of the simplified nature

of the model, we performed parametric variations on factors such as line impedance and transformer inductance, and found that their influence on current dispersion is not large enough to warrant concern on the somewhat arbitrary values we have postulated in the baseline scenario.

IV. MODELING RESULTS

In this section, we present selected results of EMTP runs for each of three TN or TT system configurations with points of lightning termination next to the distribution transformer ("first" case) or at the opposite end of the transformer ("last" case), for a total of seven scenarios. We postulated a separation of 100 m between buildings and 20 m from the transformer.

For each scenario, a pair of figures is given. The first figure of each pair is a schematic showing the configuration and point of stroke, together with indications of the peak current values in the circuit branches. The second figure of each pair shows selected current waveforms, generally currents leaving the house by way of the earthing electrode and the service conductors. Note that the peaks can occur at different times so that the sum of peak branch currents shown on the figures, Kirkhoff notwithstanding, is not always exactly zero.

A. TN-Radial, strike on one of the buildings

A distribution transformer supplies three buildings in a radial arrangement where all the service drops originate at the pole where the transformer is installed (Figure 3). This configuration is a typical U.S. residential configuration. The lightning stroke is postulated to terminate on the earthing system of one of the three buildings. Figure 4 shows the current waveforms.

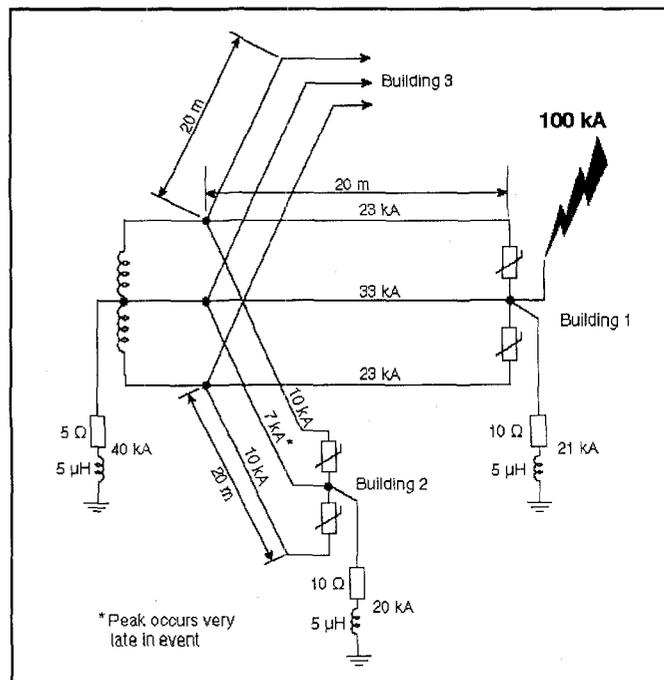
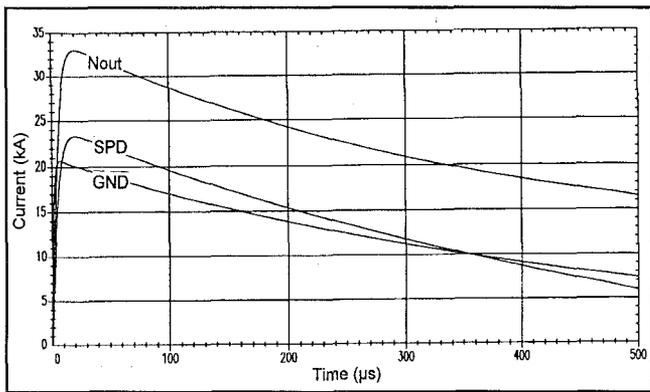
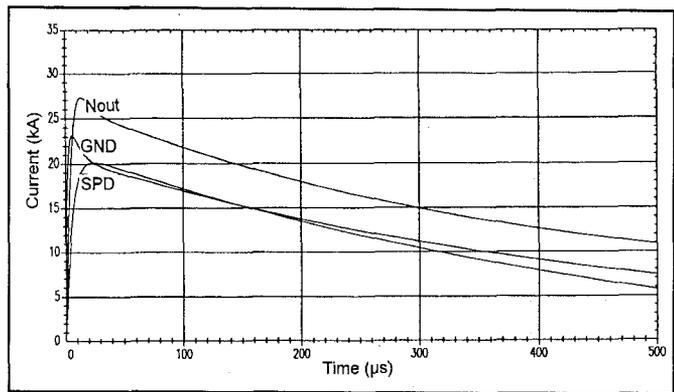


Figure 3 - Radial TN configuration with three buildings supplied by one distribution transformer, one building struck by a 10/350 μ s, 100 kA surge, showing peak values of currents shared among available paths.



SPD - Current into each line of service drop, through SPDs
 GND - Current into local building earth electrode
 Nout - Current into neutral conductor of service drop

Figure 4 - Waveforms of currents leaving Building 1, as defined in Figure 3, for a 100 kA, 10/350 μ s surge terminating on the building earthing system



SPD - Current into each line of service drop, through SPDs
 GND - Current into local building earth electrode
 Nout - Current into neutral conductor toward the transformer earth

Figure 6 - Waveforms of currents leaving Building 1, as defined in Figure 5, for a 100 kA, 10/350 μ s surge terminating on the building earthing system

B. TN-Daisy chain, strike on first building

Another typical arrangement uses a distribution transformer which supplies several buildings along a street, with short service drops from the poles to each building. The lightning stroke is postulated to occur upon the first building, next to the transformer. Figure 5 shows the circuit configuration and the peak currents in the branches; Figure 6 shows the waveforms of the currents leaving the building. Note the early peak of the current in the neutral -- directly connected to earth at the pole, thus a lower inductance compared with the inductance of the line conductors that include the transformer winding.

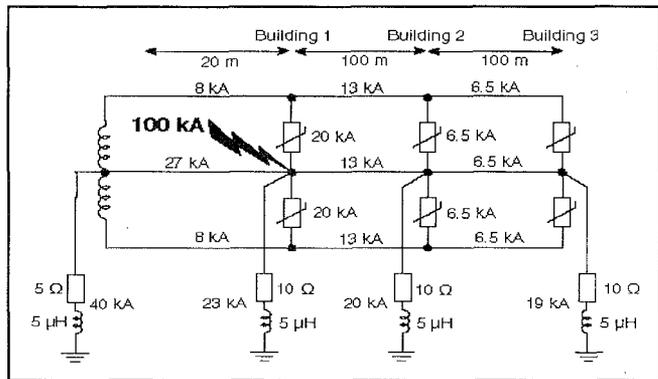


Figure 5 - Daisy chain TN configuration with building next to transformer struck by a 10/350 μ s, 100 kA surge, showing peak values of currents

C. TN-Daisy chain, strike on last building

This is the same configuration as B, but the building being struck is at the opposite end (Figure 7). The difference, if any, would give insight on the relative importance of modeling the presence of a specific transformer. In fact, the difference in the SPD stress for a strike on the first building (20 kA) compared with a strike on the last building (26 kA) is small, showing the small effect of transformer position. In the building earthing, where there are no SPDs, a strike on the last building produces 42 kA compared with 23 kA for a strike on the first building.

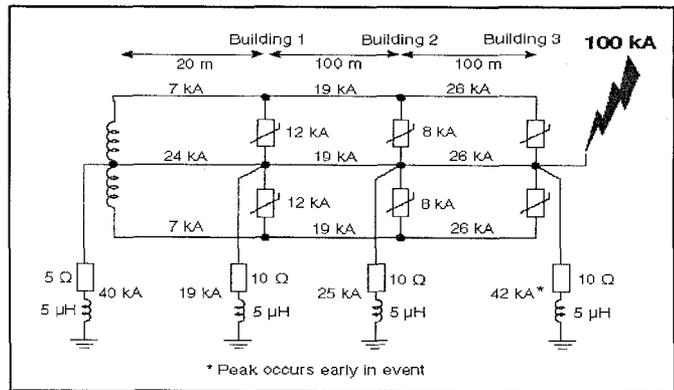
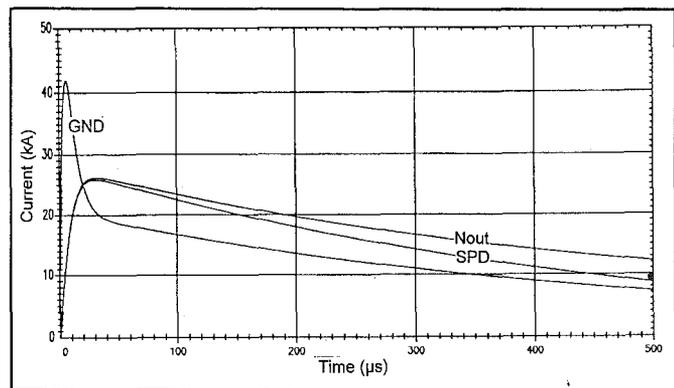


Figure 7 - Daisy chain TN configuration with building at opposite end of transformer struck by a 10/350 μ s, 100 kA surge, showing peak currents



SPD - Current into each line of service drop, through SPDs
 GND - Current into local building earth electrode
 Nout - Current into neutral conductor toward the transformer earth

Figure 8 - Waveforms of currents leaving Building 3, as defined in Figure 7, for a 100 kA, 10/350 μ s surge terminating on the building earthing system

For Figures 7-8, the greater distance (inductance) from the transformer earth electrode forces initially more current flow in the building earth than in Figures 5-6 for a closer transformer.

D. TT 2-wire, strike on first building

A transformer (single-phase or one phase of a three-phase transformer) supplies several buildings along a street, with short service drops from the street poles to each building. The lightning stroke is postulated to occur upon the building next to the transformer (Figure 9). The waveforms of the currents leaving the building are shown in Figure 10.

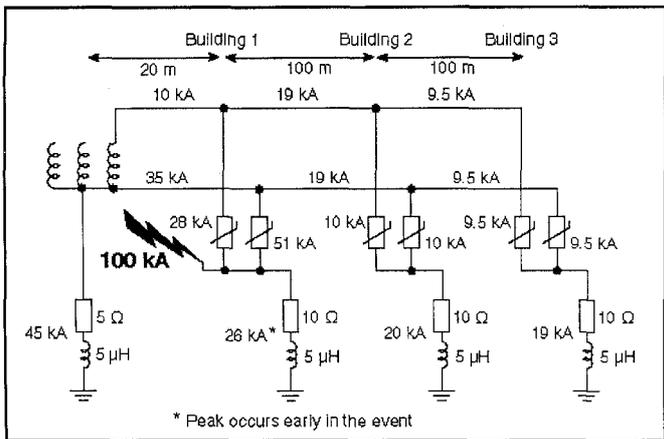
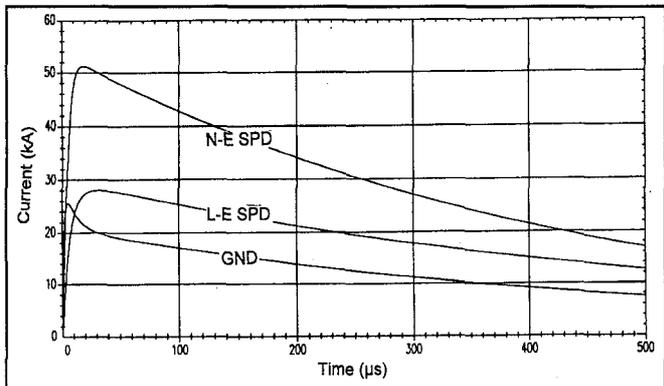


Figure 9 - Daisy chain TT 2-wire configuration with building next to distribution transformer struck by a 10/350 μs, 100 kA surge



N-E SPD - Current through neutral-to-earth SPD
 L-E SPD - Current through line-to-earth SPD
 GND - Current into building earthing electrode

Figure 10 - Waveforms of currents leaving Building 1, as defined in Figure 9, for a 100 kA, 10/350 μs surge terminating on the building earthing system

E. TT 2-wire, strike on last building

The configuration is the same as in D, but the lightning stroke is postulated to strike the building at the opposite end of distribution line, away from the transformer (Figure 11). Figure 12 shows the waveforms of the currents leaving building 3.

F. TT 4-wire

Where end-users are provided with three-phase service, a three-phase transformer supplies several buildings along a street, with short service drops from the street poles to each building. In this configuration, the difference from a 2-wire,

single-phase service is that four conductors instead of two are available as exit paths for the lightning current postulated to have struck the building of interest (first or last building).

To conserve space, we do not present two pairs of figures for that configuration, but the summary of Table 1 includes the current values computed by EMTF for the two scenarios in that configuration.

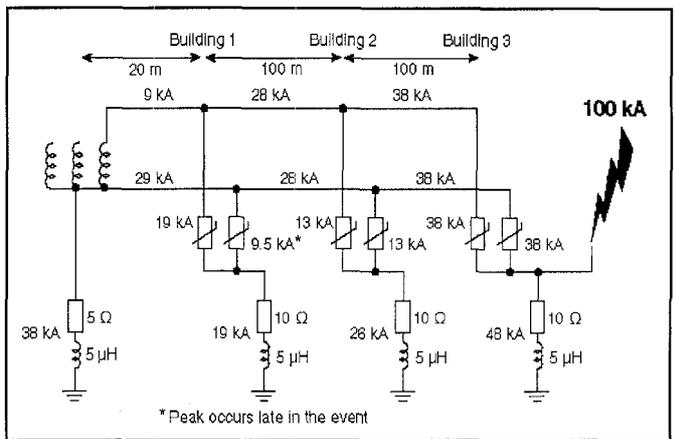
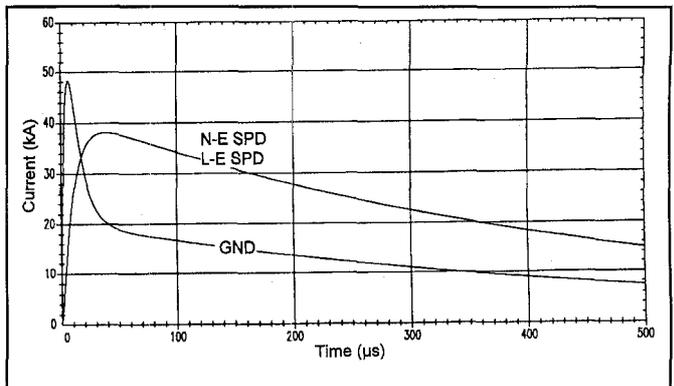


Figure 11 - Daisy chain TT 2-wire configuration with building at opposite end of distribution transformer struck by a 10/350 μs, 100 kA surge



N-E SPD - Current through neutral-to-earth SPD
 L-E SPD - Current through line-to-earth SPD
 GND - Current into building earthing electrode

Figure 12 - Waveforms of currents leaving Building 3, as defined in Figure 11, for a 100 kA, 10/350 μs surge terminating on the building earthing system

G. Comparison of the seven scenarios

Results of our model runs for the seven scenarios (Table 1) show that, contrary to some speculations or intuitive considerations on the sharing among service conductors, the earthing connection of the building does not carry anywhere near the 50% quoted in some proposed standards [9].

The most severe stress, for the parameters postulated, occurs in the neutral SPD in Scenario D (TT 2-wire, first building struck) for which the configuration has the lowest impedance to earth and thus invites the largest share. Other scenarios generally reflect primarily the number of service-drop wires available for the current exit.

TABLE I
SUMMARY OF CURRENT SHARING AMONG CONDUCTORS FOR THREE CONFIGURATIONS IN SEVEN SCENARIOS FOR 100 kA STROKE

Configuration: Distribution system	Scenario: Building being struck	See figures	Most severe but rare - Building being directly struck Currents leaving building via building earthing and service conductors (peak kA or %) *				Less severe but more frequent Currents impinging onto adjacent buildings (peak kA or %)	
			Building earthing	Service neutral	SPD in the neutral	SPD in the lines	SPD in the neutral	SPD in the lines
TN Radial	Any	3-4	21	33	N/A	23 x 2	N/A	10 x 2
TN Daisy	First	5-6	23	27	N/A	20 x 2	N/A	7 x 2
TN Daisy	Last	7-8	42	26	N/A	26 x 2	N/A	8 x 2
TT 2-wire	First	9-10	26	N/A	51	28 x 1	10	10 x 1
TT 2-wire	Last	11-12	48	N/A	38	38 x 1	13	13 x 1
TT 4-wire	First	--	22	N/A	32	16 x 3	5	5 x 3
TT 4-wire	Last	--	38	N/A	20	20 x 3	6	5 x 3

* Peak values do not occur at the same time in the different paths so that totals of numbers shown may be more than the impinging 100 kA peak.

V. ENERGY CONSIDERATIONS

In the model parameters, to start the iterative process, we have postulated that the SPD consists of a metal-oxide varistor (MOV) with relatively large cross-section that might be capable of absorbing the energy involved in diverting the 10/350 μ s surge. For the TN configurations, we selected a 150 V rms rating, and a 300 V rms rating for the TT configurations. For the cross section, we postulated an area equal to ten 20-mm discs in parallel because available manufacturer's data [14] readily gives the 20-mm disc characteristic. Such a combination would have a total one-shot joule rating of 800 joules for a 10/1000 μ s surge in the 150 V rating.

Because we suspect that even this array of ten discs might not be capable of dissipating the energy involved in a 10/350 μ s surge, the next step in this iterative process is then to compute the energy that would be deposited in the SPDs, under the current distribution patterns computed in the seven scenarios.

As one example, Table 2 shows the energy deposited in the MOVs, computed for the case of the TN Radial configuration where one SPD is connected between each of the two lines and the earth point of the installation (Figure 3). Two waveforms are shown in the table, the 10/350 μ s and the 8/20 μ s surges.

TABLE 2
ENERGY DEPOSITION IN SERVICE ENTRANCE MOVs
FOR THE TN RADIAL CONFIGURATION AND TWO WAVEFORMS

Waveform	Rating for ten 20-mm discs	Energy deposition	
		Direct strike	Nearby strike
10/350 μ s	800 J	3500 J	840 J
8/20 μ s	800 J	200 J	80 J

For the 10/350 μ s waveform, the rare scenario of a direct strike (energy deposited is 3500 J) would require a very large varistor at the service entrance -- four times the ten discs we postulated, while this ten-disc array would be sufficient in the less rare scenario of a nearby strike (840 J).

On the other hand, if we were to stay with the 8/20 μ s as a postulated waveform, even the large 100 kA peak would be handled with comfortable margin by the ten-disc array. These results provide quantitative data which we will discuss further under the Cost/Risk heading.

VI. PARAMETRIC VARIATIONS

We performed several parametric variations for the purpose of exploring the typical "what if" questions, and also to show whether or not our postulated values might be viewed as too arbitrary because of their influence on the results.

A. Line impedance and building separation

The value of 1 μ H/m for conductors has long been used by many researchers as a typical value. To investigate the significance of that postulated 1 μ H/m combined with the 100-m separation, we ran two cases, one with half the value and one with double the value. The first case corresponds to either half the separation for the same unit impedance or half the unit impedance with the same separation. For the second case, one of the parameters is doubled while the other is held constant. Table 3 shows a comparison of the baseline case with these two parametric variations.

TABLE 3
EFFECT OF LINE IMPEDANCE ON SHARING - TN DAISY LAST

Percent of 100 kA peak	Baseline 100 m, 1 μ H/m	Half baseline	Double baseline
Current into building earthing	42	32	53
Current in service neutral	26	27	25
Current in SPDs	26	26	25

This comparison shows no significant differences in the current sharing for each of the three available conductors (there are two line conductors, each with an SPD) when the postulated unit impedance or building separation is varied over a 1:4 range, so that our selection for these two parameters should not be a matter of concern.

B. Transformer pole earthing resistance and building earthing system resistance

By their relationship, these parameters can be expected to have an influence on the outcome. In the baseline case, we postulated a 5- Ω pole earthing resistance and a 10- Ω building earthing resistance. Table 4 shows the comparison of the baseline case with the reversed relationship between the pole earthing resistance and the building earthing resistance.

TABLE 4

EFFECT OF POLE EARTHING/BUILDING EARTHING - TN RADIAL

Percent of 100 kA peak	Baseline 5- Ω pole, 10- Ω bldg.	Reverse baseline 10- Ω pole, 5- Ω bldg.
Current in building earth	21	31
Current in service earth	33	14
Current in SPD	23	22

Indeed, the relationship of pole versus building earthing resistance has a significant effect on the current carried by the neutral, but not on the current carried by the SPDs. This is particularly true, although not obvious in the table (where only the peak values are shown, reflecting the inductive effect on initial current dispersion), for the tail of the 10/350 μ s waveform where the subsequent sharing is determined by the resistance ratios [6], [12].

C. Length of circuit (more buildings along a street)

Postulating a greater number of buildings along the daisy chain, while keeping the resistance of the building earthing constant, can be expected to offer a path of lesser impedance to the currents exiting the building, because of the greater number of available earth electrodes. Table 5 shows the effect of going from 3 buildings (baseline) to 9 buildings, still with the last building being struck.

TABLE 5
EFFECT OF NUMBER OF BUILDINGS IN TN CIRCUIT
ON SHARING, DISTANT HOUSE STRUCK

Percent of 100 kA peak	Baseline 3 buildings	9 buildings
Current in building earth	42	42
Current in service earth	39	14
Current in SPD	26	27

Again in this case, a difference is noticeable in the neutral conductor current, but not in the SPD current. Thus, this parametric variation shows that the number of buildings between the building being struck and the distribution transformer, while affecting the neutral current, does not affect the stress imposed on the SPDs in this TN configuration.

VII. DISCUSSION

A. Effect of postulated waveform

While we have adopted for our baseline the 10/350 μ s waveform, many SPD standards cite an 8/20 μ s or a 4/10 μ s surge waveform as an SPD capability requirement [15], [16] or as a surge environment description [17]. To explore the effect on sharing of the stroke current with different waveforms, in particular during the initial part of the 10/350 μ s surge where inductive effects dominate for the circuit parameters selected, we made one run with a 8/20 μ s surge instead of the 10/350 μ s used in the baseline case of the TN Radial. Predictably, given the small difference between a 10 μ s and an 8 μ s rise time, little effect was noted in the sharing during the first 20 μ s. Of course, the energy involved for the total surge duration is another matter, already discussed in Section V.

B. Selection of SPDs

If the design objective is to provide protection for a direct stroke to the building (a topic which will be the subject of the cost/risk analysis mentioned below), the SPDs must be selected with sufficient current-handling capability to survive the surges resulting from the postulated surge.

Alternate proposals have been made to use a spark gap as service entrance SPD. Such a gap must then be capable of clearing the resulting follow current, which may be an issue for systems having a large available fault current, such as the 10 kA rms specified for U.S. installations [3], [18].

We have made one run with a spark gap model instead of a varistor model. From the sharing point of view, the difference is small, which can be readily explained by the fact that inserting in the lightning current paths a varistor with a limiting voltage of 1 kV or so, or a gap with an arc voltage of 100 V or so, should have a very small effect on the sharing because of the many kilovolts developed by the lightning current flowing in the inductances and resistances of the line conductors and earthing connections.

C. Cost/Risk Analysis

An essential aspect of designing an effective surge protection system is to perform a cost/risk analysis involving the probability of a building being struck by a large surge, such as 100 kA, versus the cost of ensuring survival of the service entrance SPDs to be installed. This analysis introduces factors such as the flash density in the locale, the randomness of the distribution of the flashes over the area of attraction of the building which depends in part on the height of the building, and the distribution of peak amplitudes of lightning strokes. For instance, [11] reports statistics [19] whereby an 80 kA amplitude is exceeded for less than 5% of the strokes. Such an analysis is beyond the scope of our paper, but it must be mentioned here to keep the situation in perspective and remind developers of SPD application guides or standards to include it in their recommendations.

D. Applying field experience to standards

The ultimate test of the usefulness of a standard is that equipment manufactured according to that standard has satisfactory field experience, while being produced at a cost that users are willing to accept. Very low field failure rates can be seen as overdesign, high failure rates obviously as underdesign. It is the dream of one of the authors to establish a clearinghouse where field experience of manufacturers could be collected and applied to optimize the definition of the environmental stress [20]. Given the competitive nature of the industry, this is likely to remain only a dream. However, many U.S. utilities are now offering to their customers the installation of a meter-base adapter SPD. The field experience for these SPDs might be collected from utilities -- with safeguards on proprietary information -- and become an input to the process of moderating some proposals for high-stress requirements, on the basis of the successful field experience of SPDs with capabilities below those implied in proposed standards.

VIII. CONCLUSIONS

Modeling several typical TN and TT configurations of neutral earthing practices and scenarios of lightning strike point provides insights on significant effects, which should lead to more effective application of surge-protective devices (SPDs).

1. A direct lightning stroke to a building can produce high stresses on the service entrance SPDs as the earth-seeking current will exit in part by way of the utility service drop. SPDs in that building will be strongly affected, while nearby buildings will be impacted by much lower surge currents.
2. The major difference among the scenarios we have modeled appears in the current carried away from the building by the neutral conductor.
 - In a TN system where the neutral is bonded to earth at the service entrance, there is no SPD in that path, and thus no concern about neutral SPD integrity. In typical residential single-phase U.S. systems, the line SPDs can carry about 25% of the stroke current.
 - In a TT system where there is an SPD in the neutral path, a single-phase two-wire configuration can have 50% of the stroke current being carried by the neutral SPD. In a three-phase TT system where there are four conductors to carry away the stroke current, the neutral SPD can carry up to 30% of the stroke current.
3. For line conductors, the difference reflects primarily the total number of conductors in the system, which can be two, three or four. The earth-seeking lightning current will divide (but not always equally) among these conductors. While the initial dispersion (during the first 20 μ s) is controlled by the inductances, the later dispersion is controlled by the relative values of the earthing resistances.
4. If the postulated stroke is as high as some of the proposed standards suggest, modeling the behavior of service entrance SPDs of the type installed in increasing numbers by U.S. utilities shows that some failures could be expected. As field experience seems to indicate an acceptable failure (if any) rate, one can question the need for imposing such severe requirements, unless the mission of the facility is such that even a rare failure would be unacceptable.

IX. ACKNOWLEDGMENTS

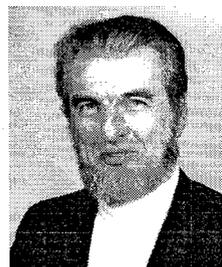
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Discussion

P. Hasse and J. Birkl (Dehn + Söhne, 92318 Neumarkt, Germany): The problem of lightning current distribution depending on the different kinds of current distribution systems and under the influence of possible variables has been very clearly represented with this contribution.

The curve development gained by the EMTP program matches very well with the simulation calculations conducted in Germany with the PSPICE program.

In respect to the described results, however, a few additions are necessary from our point of view:

1. Section III. G. and VI. A.:

The induction of a conductor system always results out of the geometric system of the slip-knot consisting of coming and going conductor. A separation in coming conductor impedance and going conductor impedance is not realistic.

In particular, in case of multiple conductor cables it is to be observed that in case of the same flow direction of the lightning current, the inductivity of the total system differentiates to a single conductor system.

2. Section VI. Schedules 3 - 5:

In particular, in case of longer connecting cables between buildings and between building and transformer a change in waveform of the surge flowing through these cables. Only the observance of the amplitude factor of the flowing lightning currents is not sufficient. In this situation, it would be more meaningful to consider also the energy distribution.

3. Section VII. B.:

For decades now, in Europe, spark gap arresters, with a mains follow current quenching capability, are being installed successfully as lightning current arresters at the building entrance. In particular, the high down-lead ability and impulse-time shortening of the rest impulse make a favourable co-ordination with connecting MOV's possible.

4. Section VII. 4:

The lightning protection necessity for a system, as well as the deduced lightning protection class resulting from this, is described in IEC 1662. At the same time the lightning protection class is determined, the layout of the lightning protection system necessary lightning parameters are defined (IEC 1312-1). A deviating layout of protection measures on the basis of test currents $8/20 \mu\text{s}$ is therefore not permissible.

François D. Martzloff (National Institute of Standards and Technology, Gaithersburg MD) :

We thank the two authors of the discussion for taking the time to review our paper and provide comments aimed at broadening the consensus on the subject. In particular, we are delighted to hear that our computations based on EMTP matches very well with the simulation calculations conducted in Germany with the PSPICE program. With respect to their specific four comments, we offer the following responses, preceded by the general remark that the purpose of our computations was to reveal the differences among various *postulates* for the circuit configurations, as influenced by the grounding practices for the neutral in effect in different countries, rather than the precise values for a particular set of parameters. We emphasize the concept of postulate, lest we fall into the trap of taking electromagnetic environment standards as an exact duplication of reality, while they are in fact only the documentation of an industry consensus on how reality might be represented ¹.

1. Section II G and IV A

Indeed, the concept of inductance is based on a conductive loop that carries the current in a closed circuit. However, in the circuits we postulated for our computations, the conductors in question — phase and neutral — may be considered as one part of the closed circuit and might be called "coming," according to the terminology used by our colleagues, while the path consisting of the earth, the distant return to the cloud, the lightning channel, and even the down-conductor (see Section III A) may be considered as the other part of the loop and might be called "going" conductors.

For this reason, we represented in our figures the phase and neutral as if they were separate, while in reality they can be at some finite distance from each other (the so-called "open wiring" used in overhead lines) or in close vicinity, as in the case of an underground cable or an overhead "triplex." Aware of the differences, we started our computations for a given, postulated configuration — always the same for the variations in the neutral grounding — but performed a parametric variation in the line inductance (taking twice or half the value used in the baseline), as stated in the subject paragraph, to convince ourselves that the influence on dispersion is not large enough to cause concern. Space limitations for the paper prevented us from providing detailed numerical results — as they also do here — and we were hoping that our simple statement that we did consider the issue and found little effect on the differences *among neutral grounding scenarios* might be acceptable.

2. Section VI 3 to 5

One of the results of our computations based on a postulated $10/350 \mu\text{s}$ waveform was to show that, for the distances we selected, the impedance of the cable between buildings — and therefore their length — has only a small influence on the long-term current waveform and dispersion among conductors, which is primarily influenced by the postulated values for respective earthing resistances. With the values selected for inductances,

the current dispersion is substantially affected by the respective inductances only for the first 20 or 30 μs .

We agree that additional information might be conveyed by reporting the energy distribution along the complex path of the lightning current, but here again space limitations intervene. We can offer the response, however, that in view of the large values of the earthing resistance compared to the other resistances in the circuit — cable resistances and dynamic “resistances” of the varistor or gap SPDs — the latter are not a priority in reporting results. The EMTP model of course has the capability of reporting any set of parameters if “asked” to do so.

For specific applications of one type or another of SPD technology, the EMTP model can provide detailed information on the energy that will be deposited in these SPDs for the various scenarios to be considered.

Section VII B

We are aware that in some countries, the installation of a service-entrance arrester is a common practice, and that gapped arresters may be used for that purpose. The issue is one of cost vs. benefits for an arrester designed for the large lightning currents associated with a rare direct strike to the building. We have observed, during our interactions with several international or IEEE technical committees, that consensus has not been reached on what current waveform and peak amplitudes should be considered when making the cost vs. benefits analysis. Depending on the nature of the installation, the cost vs. benefits equations are different. Several proposals for “risk analysis” are currently under consideration in several standards-developing bodies, and consensus is clearly not achieved at this point. This lingering question is addressed in our response to the fourth and last comment after the present one.

Our intention in making the remark on available fault current in the second paragraph of this section was not to contest the successful European experience cited by our colleagues, but to alert our readers at large to the importance of considering that requirement. The point that mains follow-current quenching capability is not trivial was confirmed in a comment by one of the reviewers of our forthcoming paper, “Gapped Arresters

Revisited” (scheduled for presentation at the IEEE-PES Winter 1998 Meeting and later publication in *IEEE Transactions*).

Section VII 4

We are aware of the work conducted in the IEC Technical Committee 81, the responsible body for development of the IEC 61662 and IEC 61312 publications. We are also aware of some discomfort among other parties concerning the stipulations from that body which might result in less than fully cost-effective solutions to the question of real necessity for protection against worst-case scenarios. The footnote offered in support of our introductory remark applies here also. There is a long and successful history of application of surge-protective devices based on a postulated 8/20 μs surge current waveform, using the appropriate values of amplitudes. For that reason, we included in our paper as alternate postulate the 8/20 μs waveform. From the point of view of IEC TC 81, their recommendations might be considered normative and thus non-negotiable, but protection measures in the various countries are typically determined — if at all — by bodies that promulgate codes based on a consensus drawn from experience based not exclusively on TC 81 recommendations. Therefore, the use of the term “not permissible” appears somewhat strong in the context of voluntary or even regulatory practices.

In conclusion, we appreciate the opportunity to present more detailed background information on our computations and underlying postulates, thanks to the discussion contributed by our colleagues.

1. Long ago, my mentor, Frank Fisher, taught me this concept which I recite in the following terms, well worth repeating in the present context: “The criterion of validity of an environment standard is not so much how closely it duplicates reality but rather how well equipment designed in accordance with this standard perform in the field. If equipment designed in accordance with the standard perform well in the field, while equipment ignoring the standard do not perform well, the chances that the standard be a good standard are pretty good.”

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**THE
DILEMMA
OF
SURGE
PROTECTION
VS.
OVERVOLTAGE
SCENARIOS**

The Dilemma of Surge Protection vs. Overvoltage Scenarios: Implications for Low-Voltage Surge-Protective Devices

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Significance:

Part 2 Development of standards

Part 7 Mitigation techniques

The application of surge-protective devices (SPDs) in low-voltage AC power circuits, in particular metal-oxide varistors (MOVs) has been influenced by the perception that low-limiting voltage is a desirable characteristic. Unfortunately, this low limiting voltage – intended for surge protection – makes the devices more susceptible to fail under conditions of extended temporary overvoltage (TOV).

Like any electronic component, SPDs will fail if overstressed beyond reasonable limits, and this is not a cause for rejecting their application, but a cause for concern on ensuring that the failure mode – rare as it might be – will be acceptable.

This acceptability must also take into consideration the effect of the available fault current that the power system can deliver at the point of connection of the SPD. This point needs to be more clearly and specifically stated in emerging standards on SPD applications.

The Dilemma of Surge Protection vs. Overvoltage Scenarios: Implications for Low-Voltage Surge-Protective Devices

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Abstract - The application of surge-protective devices in low-voltage systems faces the dilemma of providing effective limiting against surges while not attempting to limit the temporary overvoltages that do occur in a power system. The paper illustrates this dilemma with specific scenarios and presents recommendations for reconciling these two conditions through adequate design and more explicit standards.

I. INTRODUCTION

The concept of "Whole-House Surge Protection" has become a popular subject of discussion and has in fact been implemented by several utilities in North America. In this approach, the utility will install a surge-protective device (SPD) at the service entrance of the customer and provide additional plug-in SPDs. These additional SPDs are presumed to be well-coordinated with the service-entrance SPD, and are installed within the customer premises, presumably at the point of connection of so-called "sensitive appliances" such as home entertainment, computers, and sophisticated kitchen appliances.

The main purpose of these SPDs is to protect sensitive equipment against surges, a mission that they can accomplish quite well. However, the failure mode of these devices under temporary overvoltages (TOVs) that might be expected under abnormal but possible conditions of the power system has become cause of some concern for utilities and their customers.

An SPD should not be expected to protect downstream equipment in the case of a TOV and then return to normal operation, as it does by definition for surge protection. The dilemma for SPD designers is whether to select a maximum continuous operating voltage (MCOV) high enough to survive common TOVs — but at the price of diminished surge protection — or to select surge protection with a lower MCOV — and then accept failure of the SPD for infrequent but possible TOVs. In any case, one should expect that if a TOV at any level would cause the SPD to fail, that failure mode should be acceptable.

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This dilemma of surge protection versus overvoltage scenarios has been created by the industry's obsession with providing very low clamping voltages for surge mitigation (Martzloff & Leedy, 1989[1]). And now, the need to ensure coordination of the "cascade" of the service-entrance SPD and the plug-in SPDs has exacerbated this situation. The issue of cascade coordination has already been debated at length in the literature (Martzloff & Lai, 1991 [2]); (Stonely & Stringfellow, 1991 [3]); (Hostfet et al., 1992 [4]); (Rousseau & Perche, 1995 [5]). Further debate or exhaustive references to the many papers on that subject is not our purpose. It is mentioned here only as a contributing factor to the dilemma, but a factor that cannot be ignored in a complete assessment.

II. SURGE PROTECTIVE DEVICES FOR LOW-VOLTAGE SYSTEMS

The introduction of metal-oxide varistors (MOVs) in the seventies was a timely innovation, concurrent with the increasing use of semiconductors in consumer products. While these semiconductors opened new opportunities, their relatively low tolerance for surges created a strong demand for better surge-protective devices. Unfortunately, market competition encouraged a downward "auction" that led to attributing high value to low clamping voltages, a situation unwittingly encouraged by the listing of "transient suppression levels" stipulated in UL Standard 1449 [6]. The list begins at 330 V for SPDs intended for 120 V circuits, although there is good evidence that most consumer loads do not need such a low level of protection (Anderson & Bowes, 1990 [7]); (Smith & Standler, 1992 [8]).

The generic structure of typical low-voltage residential power systems is shown in Figure 1 for the case of a detached home. Underground service has similar characteristics. This system extends from the outdoor line-side of the service drop all the way through the premises wiring, including plug-in type SPDs. Figure 1 also shows the various locations where an SPD can be installed.

Typically, there are six locations. The first three: ① at the outdoor weather-head, ② at the service entrance, and ③ on the line side of the main disconnect are within the scope of IEEE Std C62.34 [9]. The next three are within the scope of ongoing IEEE project P62.62 [10]: ④ at the load side of the panel disconnect, ⑤ at a permanently wired receptacle, and ⑥ as a plug-in device. Locations ③ through ⑥ are within the premises wiring and therefore under the control of the end-user, while locations ① and ② are under the control of the utility.

III. FAILURE MODES UNDER SURGE CONDITIONS

For a correctly applied SPD, failure under surge conditions should be a very rare occurrence. Nevertheless, one can enumerate the following failure scenarios in a field application — including misapplications:

1. A single, large, and not anticipated impinging surge exceeds the capability of the SPD. An example of this situation can be the presence of switched capacitor banks;
2. A succession of surges, such as multiple lightning strokes, exceeds the capability of the SPD. This situation has been identified for distribution arresters (Darveniza, 1997 [13]) and might also occur for low-voltage SPDs;
3. A thermal runaway is launched in an SPD exposed to high ambient temperatures at the time when a surge (within specifications for normal ambient) occurs;
4. In the questionable scenario of an alleged “degraded” SPD (Stringfellow, 1992 [14]), a thermal runaway is launched by the heat generated during a within-specifications surge.

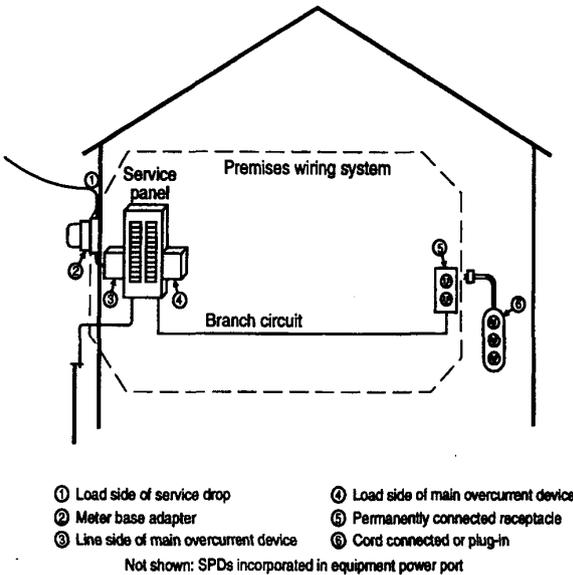


Figure 1 - Possible SPD locations for a residential building

An emerging requirement in standards for application of the low-voltage SPDs under development at the International Electrotechnical Commission (IEC 61643-1 [11]) as well as at the IEEE (P62.62 [10]) is the provision of a “disconnect” intended to disconnect a failing SPD. Failure under conditions exceeding the SPD capability is recognized as unavoidable, but the consequences of such failure are made acceptable thanks to the action of the disconnecter.

Unfortunately, some ambiguity has crept in the interpretation of this requirement. In some cases it has been interpreted as only disconnecting the failed SPD component from the power system, but leaving the load energized — and without further surge protection (Martzloff, 1998 [12]). If the disconnecter is of this latter type, the SPDs components of an SPD package will fail under TOV conditions, presumably in a safe manner, but then allow the TOV to be applied to the downstream load. This is undesirable for the typical user who values equipment protection above continuity of operation. Finally, there is the worst case, as reported in many anecdotal instances, where the failure mode of SPDs under TOV conditions has not been graceful, to say the least. These instances, while not very frequent, have led to new testing requirements for failure modes from the Underwriters Laboratories in the updated Second Edition of their UL 1449 [6] Standard for low-voltage surge-protective devices (“TVSS” in the industry jargon).

Given this unsettled situation, it will be useful to review the scenarios that can lead to failure of an SPD component in an SPD package, in particular under TOV conditions. For the sake of completeness, we will describe first some failure scenarios under surge conditions, as they have some bearing on the disconnecter design.

For all these scenarios, the ultimate failure mode depends on the fault current that the power system can deliver at the point of connection of the SPD. For moderate fault currents, such as on branch circuits, the disconnecter can generally provide protection. The fuse design can still be a challenge: carry the load current, carry the specified surge current, but melt in case of a power-frequency fault current resulting from failure of the SPD component. For very high available fault currents, such as that prevailing at some service entrances close to a large distribution transformer, successful clearing may be a greater challenge. Furthermore, coordination of overcurrent protection is more difficult, compared to branch circuits inside the building where the wiring impedance and the rating of circuit breakers in the panel can ensure proper coordination.

IV. FAILURE MODES UNDER TOV CONDITIONS

Three major types of TOV-induced failures can be identified for low-voltage SPDs:

Moderate TOVs associated with power system faults, such as a line-to-earth fault in a three-phase system, creating a 1.73 times normal line voltage in the other phases. Ferroresonance can also produce moderate but significant overvoltages.

Extreme TOVs associated with the commingling scenario (accidental fall of conductors of a higher voltage upon conductors of a lesser voltage). No conventional, varistor-only SPD can be expected to survive such a scenario.

Double voltage TOVs associated with the loss of neutral in a single-phase, three-conductor, earthed center-tap system such as the 120/240 V service typical of North American systems.

Depending on the philosophy of the system designer, in particular the utility for the case of a service-entrance SPD, survival or expected but acceptable failure can be stipulated for the loss-of-neutral scenario.

a) Moderate TOVs: System Faults

Among abnormal conditions that can produce temporary overvoltages, we give two examples of incidents resulting in overvoltages not exceeding twice the normal voltage. Some SPDs based on the misconception that a very low clamping voltage is desirable might not survive such moderate TOVs.

Single-phase faults to earth on a three-phase system produce a shift in the unfaulted phases. The severity of the voltage rise depends on the fault location, the system impedance, and the earthing practices. For a TT power system in steady-state, the neutral will by symmetry be at the same potential as earth, as in Figure 2 (a). However, if an earth fault occurs on one phase, it will cause a shift of potential as the system attempts to maintain balance. The neutral will be elevated and a corresponding shift will be experienced by the other two phases. In the worst case of a completely isolated system with a bolted fault to earth on one of the phases, we could have the situation depicted in Figure 2 (b). This shows a neutral which has been elevated to one per unit, causing the unfaulted phases to drift up to 1.73 per unit with respect to earth.

For three-phase systems with an artificial neutral through an earthing transformer or a finite resistance, the earth impedance is high. This arrangement limits the fault current during a single-line-to-earth fault, but allows enough for fault detection using overcurrent relays. It also inserts some impedance between the system neutral and the actual earth so that the voltage on the unfaulted phases will shift toward somewhat less than line-line value as shown in Figure 2 (c).

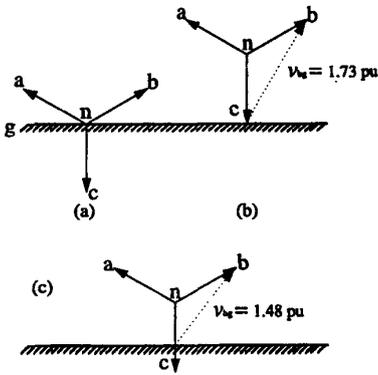
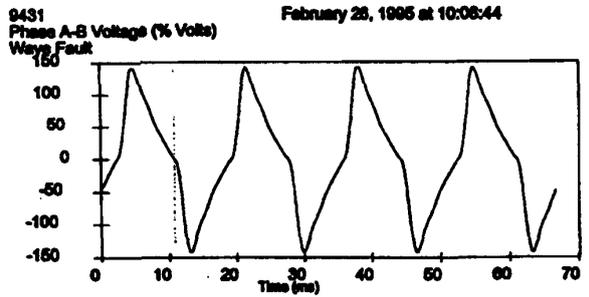


Figure 2 - Effect of a single phase-to-earth fault

Series resonances occur in a power system when a series circuit consisting of an inductance and a capacitance is excited at its natural frequency. As an example of this condition, Figure 3 shows a portion of a temporary overvoltage resulting from a power system switching incident (restoring power phase by phase after interruption [15]), which produced a sustained rms voltage exceeding 150% and lasting four seconds.



Source: EPRI Report [15]

Figure 3 - Temporary overvoltage caused by ferroresonance

b) Extreme TOVs: Commingling

In this real-life scenario, the low-voltage SPD connected on the secondary side of the distribution transformer, and normally energized at its rated voltage, is first brought to failure by the large overvoltage resulting from commingling conductors. In an overbuilt system, a collision of a vehicle with a pole, or breaks caused by icing, the conductors of the higher voltage distribution system or sub-transmission system can fall on the lower voltage distribution system. Such accidental contact injects an intruding voltage for a few cycles, until the higher voltage breaker clears the fault.

Figure 4 shows a simplified one-line diagram of the two medium-voltage systems (the intruding MV1 and the victim MV2) being accidentally commingled. In this figure, the bond between the two systems earth connections is shown in dotted line to present the generic case of commingling. In an overbuilt system with common neutrals, a solid bond exists where the dotted line is shown in the diagram. In the scenario of a simple crossing of two systems (not overbuilt along the right of way), or delta systems, a solid bond might not be present, and the fault current from MV1 will involve the earthing impedances shown in the diagram. In that case, the intruding voltage V_i might be less than the system voltage MV1 but still enough to precipitate failure of an SPD on the secondary.

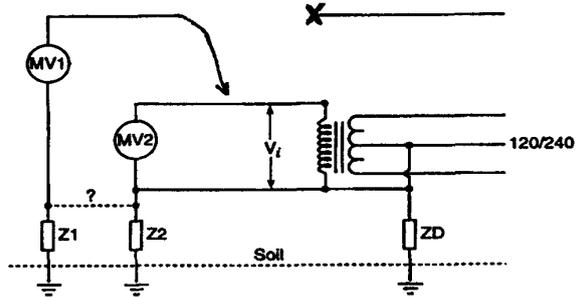


Figure 4 - Commingling overvoltage applied to the distribution transformer primary and reflected on the LV side

c) Double-voltage TOV (Loss of neutral)

There are many situations where loss of neutral can occur. Some can be of a transient nature, such as a loose connection, while some might be permanent until repaired, such as a mechanical break or corrosion of the neutral conductor. While the latter might not linguistically fall under the label of 'temporary', the consequences are the same. In a three-wire, single-phase system typical of North American practice, this condition has been observed many times.

Figure 5 shows a system where one side of the supply (L1) is lightly loaded, while the other side (L2) is heavily loaded. Under normal conditions, the two sides remain at normal voltage. Should the neutral connection be lost, then the voltage at mid-point is determined by the ratio of impedances on the two sides: the L1 side experiences an overvoltage that can approach twice normal. Any SPD connected to this side will then be exposed to the twice-normal voltage, with an available current determined by the impedance Z2. This current, by the very design of the circuit, will not be interrupted by the overcurrent protection and is available to generate substantial heat in the SPD that has failed as the result of the overvoltage. A disconnecter designed to clear larger fault currents, such as those occurring if the SPD fails while the neutral is connected, might not be capable of clearing the limited current. Other schemes are then necessary to ensure an acceptable failure mode, such as a thermally-activated disconnect.

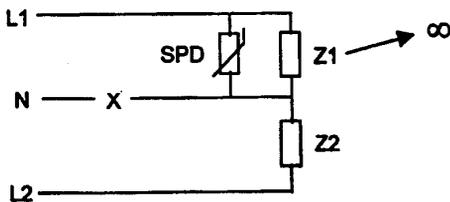


Figure 5 - Three-wire, single-phase system where a broken or intermittent neutral connection creates a voltage imbalance

V. AVAILABLE FAULT CURRENT

As described in the preceding paragraphs, several scenarios can produce failure of the SPD, ultimately offering a very low (but not zero) impedance to the flow of the fault current that the power system can deliver. For each scenario, the available fault current at the point of connection of the SPD will have a very significant effect on the failure mode.

The significance of available fault current is recognized in standards, but the value that should be considered is generally left undefined. The following statements can be found in published standards.

1996 National Electrical Code, 230-65, Available Short-Circuit Current

"Service equipment shall be suitable for the short-circuit current available at the supply terminals."

IEEE C62.34-1996, Standard for Performance of Low-Voltage Surge-Protective Devices (Secondary Arresters), Article 7.6

"If the manufacturer claims a fault current withstand rating, then that rating shall be verified."

IEC 61643-1 (FDIS November 1997) Surge protective devices connected to low-voltage power distribution systems - Part 1: Performance requirements and testing methods, Article 6.2.11

"The SPD shall be able to carry the power short-circuit current until it is interrupted either by the SPD itself, by an internal or external overcurrent disconnecter, or by the backup overcurrent protection."

It is noteworthy that none of these documents specify a value for the available short circuit, but rather leave it to the discretion of the manufacturer, while imposing criteria of acceptability after the failure. The NEC tersely requires the device to be "suitable," without elaboration. The two standards applicable to a service-entrance SPD — where the available fault current can be quite high — acknowledge the possibility of failure and significance of the available fault current, but do not stipulate specific values.

In an attempt to obtain information on what levels of fault currents should be considered, an informal survey was conducted among a few utility engineers. It turns out that very few utilities limit the available fault current at the service point of residential customers but many people are under the misconception, as cited below, that all residential service load centers and breakers are limited to an available fault current of 10 kA and therefore they assume that actual available fault current must be less than 10 kA.

Interestingly, none of the individuals consulted on this issue could cite a standard or even a document with the status of a consensus guide that does stipulate a current value. Two perceptions seem to prevail among the individuals consulted, as quoted below:

- 1. Based on the observation that breakers for typical residential service panels (up to 200 A) generally have an interrupting capacity of 10 kA, it would be logical that the available fault current at the service entrance would be in the same order of magnitude.*
- 2. Based on the reality that a service entrance connected close to a large distribution transformer (such as a garden apartment or high-rise) will have available fault currents in excess of 10 kA, it would be prudent to review the specifics of the situation.*

Furthermore, these standards do not require that the SPD application data state a limit of acceptable fault current.

VI. EMERGING STANDARDS

a) SPDs installed downstream from the service entrance

In recognition of the issues raised by failure modes of the SPDs within the scope of UL Standard 1449 — downstream of the main disconnect — the second edition of this standard [6] now requires demonstration of an acceptable behavior in the failure mode of SPDs exposed to various overvoltage scenarios. Specifically, among the many tests required by UL, three tests address the issue:

Temporary overvoltage with high available current - This test is stipulated under article 37.2 of UL 1449, calling for extended exposure to 125% of normal line voltage with either an acceptable temperature equilibrium being attained or until an internal disconnect device operates. This test will demonstrate capability of sustaining moderate overvoltages, primarily providing a margin against high system voltage, but not the higher levels of temporary overvoltages covered in the next test. The available fault current specified for this test is defined as a function of the ampere rating of the service over a range of 200 A to 25 000 A.

Full phase voltage with high available fault current - This test is stipulated under article 37.3 of UL 1449, calling for exposure to the “full phase voltage” as shown on Figure 2(b) of this paper. The same criteria as above apply, namely acceptable temperature equilibrium or operation of an internal disconnect.

The available fault current specified for this test is defined as a function of the ampere rating of the service over a range of 200 A to 25 000 A.

Overvoltage with limited current - This test is stipulated under article 37.4 of UL 1449, with overvoltage values presumably corresponding to a loss of neutral scenario, and the associated low values of current supplied by the connected load, as in Figure 5.

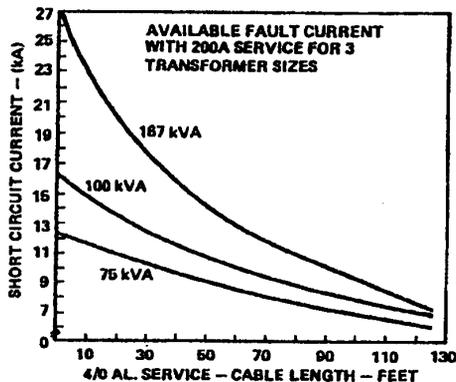
It is noteworthy that in the stipulations of these three tests, the emotionally charged word “failure” is not used. Instead, a list of unacceptable conditions is given, including emission of flame, molten metal, flaming particles, charring of adjacent material, ignition of enclosure, or creation of openings leaving live parts accessible.

b) SPDs installed upstream from the service entrance

For SPDs connected upstream from the service panel, the recently published IEEE Standard C62.34 does describes a loss-of-neutral scenario with limited current, similar to the UL 37.4 test. However, a demonstration test is not mandated, as the consensus development process settled for a weaker statement: “if the manufacturer claims a loss of neutral withstand capability ... then that capability shall be verified ...” Thus, the inference might be made that if no claim for loss-of-neutral withstand capability is made, no demonstration test is required. However, an additional paragraph in the standard does mention “...must fail in an acceptable manner.”

From this brief overview of emerging standards, it appears that on the low side of available fault current, both the UL and the IEEE standards have recognized the issues of acceptable failure modes associated with temporary (or quasi-permanent) overvoltages. On the other hand, at the high side of available fault currents, it seems that insufficient recognition of the issue still prevails, as discussed in the preceding section on the significance of the level of available fault currents.

Nevertheless, Figure 6, offered as background information in a tutorial addressing considerations on the revenue meter environment (Ward, 1980 [16]), shows typical values of fault current as a function of distribution transformer size and length of service drop. That figure clearly shows values in excess of 10 kA, but somehow this information has not been fully recognized by the SPD community.



Source: (Ward, 1980 [16])

Figure 6 - Fault current amplitude as a function of transformer rating and length and size of service drop

VII. POSSIBLE SOLUTIONS

Among possible solutions, two approaches may be considered: making the SPD less sensitive to TOVs, and, in any case, ensure that if failure is unavoidable under extreme stress, it will be in an acceptable mode.

The obvious way to desensitize SPDs to TOVs is to design them with a higher MCOV. However, as the higher MCOV in a varistor-only SPD means a higher surge-limiting voltage, there is a limit beyond which such an SPD becomes useless (op cit., [2-5]).

A possible solution may be in reviving the concept of a gapped arrester for the upstream SPD of a “whole house” scheme (Mansoor et al., 1998 [17]). There, the initial let-through associated with the gap volt-time response can easily be mitigated by the downstream SPD, while the gap prevents the SPD from becoming involved with moderate TOVs.

Of course, for the (rare) commingling scenario, little can be done but to ensure a graceful failure. This condition should be an implicit requirement, but, as discussed in the section on available fault current, the implications of such a requirement apparently have not been recognized by all interested parties. Even among the community of SPD engineers, there has been some reluctance to accept the concept that *temporary overvoltages* should be addressed in documents discussing the *surge* environment.

VIII. CONCLUSIONS

1. The dilemma of providing a suitable surge protection of load equipment by means of surge-protective devices, while ensuring acceptable response of these surge-protective devices to unavoidable temporary overvoltages raises several application issues that demand attention.
2. "Acceptable response" can be interpreted either as survival of the SPD (a challenge to the coordination of cascades) or as accepting failure, but within well-defined conditions of the failure mode (a challenge for applications where high available fault currents prevail).
3. Coordinating a cascade of surge-protective devices can be solved by providing a gapped arrester at the service entrance, which will coordinate with the de facto situation of low limiting voltage SPDs inside the building.
4. The need for a service-entrance arrester to withstand the scenario of lost neutral can be satisfied by a gapped arrester having sufficient maximum continuous operating voltage capability.
5. Emerging standards for low-voltage SPDS have given new recognition to the importance of taking into consideration temporary overvoltages in the design of SPDs.
6. Notwithstanding conclusion (5), SPD application standards as well as performance and test standards should be more explicit in defining how to deal with the issues raised by available fault current in case of unavoidable SPD failure.

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**THE FALLACY
OF
MONITORING
SURGE
VOLTAGES:

SPDS AND
PCS GALORE!**

The Fallacy of Monitoring Surge Voltages: SPDs and PCs Galore!

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Significance:

Part 2 Development of standards

Part 5 Monitoring instruments

Recent projects of monitoring Power Quality in AC systems have focused on voltage surges rather than current surges. The predictable results of such monitoring – low apparent surge voltages – which in fact only monitors whatever limiting voltage is allowed by the proliferating SPDs and PCs, do not reflect the surge activity, now confined to surge currents flowing into the “attractive” paths of the SPDs and the capacitors included in the switch-mode power supplies of PCs (and other electronic appliances).

At standard-writing times, questions have emerged as to why mention “large” surges when monitoring shows only low voltages. With proper perspective, it becomes apparent that the proliferation of these voltage-limiting, surge-absorbing SPDs and PCs are the explanation. While voltage surges might now no longer be a threat, the possibility of substantial current surges is indeed a threat to equipment.

The Fallacy of Monitoring Surge Voltages: SPDs and PCs Galore!

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Abstract

To support the recommendation of shifting transient monitoring from voltage surges to current surges, the paper presents experimental results as well as numerical modeling results demonstrating two mechanisms causing an apparent decrease of surge activity in low-voltage ac power circuits. The first mechanism is the proliferation of surge-protective devices, a situation which is by now well recognized. The second, which should also have been recognized, apparently escaped scrutiny so far: the proliferation of electronic appliances containing a switch-mode power supply that effectively places large surge-absorbing capacitors across the ac power systems.

1. Introduction

This paper is unabashedly tutorial, and some of the themes presented here might be quite familiar to some of our readers. However, we have observed that these concepts, which could almost be characterized as obvious when given the benefit of hindsight, are still not widely recognized. Therefore, we will present them to this forum, and illustrate their validity by experimental measurements and numerical modeling. The three major themes of this presentation are:

1. There is a logical explanation for the apparent decrease in the level of voltage surges reported in recent power quality surveys: the proliferation of surge-mitigating devices.
2. The present practice of recording voltage surges can lead to erroneous concepts on surge protection, which in turn can lead to equipment failures.
3. There is an unfulfilled need to develop and deploy power quality monitors that can characterize the energy-delivery capability of a surge event.

The first theme will be introduced by a historical perspective showing how *voltage* surges became the focal point of monitoring surges, even before the term “power quality” was coined. Experimental measurements and numerical modeling will be described to illustrate the effect of the proliferation of surge-protective devices (SPDs) and of new electronic appliances that serve respectively as intended and unintended surge-mitigating devices. The next two themes will be only briefly discussed because, once the first is accepted, these two follow quite logically.

¹ Electricity Division, Electronics and Electrical Engineering Laboratory, Technology Administration, U.S. Department of Commerce.
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2. Historical Perspective

The proliferation of SPDs in low-voltage ac power circuits has been recognized as one of the root causes of the apparent decrease of the surge levels recorded in recent power quality surveys. The change in the occurrence of surges began to be recognized [Dorr, 1995]¹ and explanations were offered attributing the phenomenon to the emerging proliferation of SPDs in low-voltage ac power circuits [Martzloff, 1996]. It is noteworthy that for many people, the term “surge” is equivalent to “transient overvoltage” to the point that the phenomenon has generally been recorded by instruments acting as voltmeters, and the term was without much scrutiny accepted in general as meaning a voltage surge.

For instance, in the bi-lingual publications of the International Electrotechnical Commission (IEC), the French text which parallels the English text uses the term “surtension” (meaning overvoltage), because there is a mind set, reinforced by the lack of a neutral term in French that could be applied to either or both voltage surges and current surges. In the United States, the Underwriters Laboratories perpetuates this narrow perspective by calling SPDs “Transient *Voltage* Surge Suppressors” (italics ours) [UL Std 1449, 1996], although both recent IEC and IEEE definitions of SPDs introduce and emphasize the concept of *current surges* as well as *voltage surges*.

Now less recognized but significant, a similar cause for the apparent decrease of voltage surge levels is becoming important as more and more electronic appliances depend on a switch-mode power supply with a rectifier-capacitor DC link. Through the rectifier, the DC link capacitor which is typically in the order of 200 μF to 500 μF , offers a low-impedance path to current surges impinging on the power port of these appliances. With many such appliances connected in an end-user installation, the effect is that of quite a large capacitor being connected across the ac mains. Small wonder then that even large *surge currents* (for instance, with the capability of delivering currents of 3 kA, 8/20 μs) [ANSI/IEEE C62.41-1995] can no longer raise the voltage across the mains to the high values sometimes reported in earlier surveys of *surge voltage* occurrences.

Another historical mind-set has been to recognize the origin of surges only as a voltage event while in fact it can be either a true induced-voltage event, or the end-result of the injection of a surge current somewhere in the power system. Typical induced-voltage surges are associated with the electromagnetic coupling into the power circuits of the field created by a nearby (but not direct) lightning flash. Such voltage surges, which can develop substantial voltages in high-impedance circuit loops [Martzloff et al., 1995] can easily be mitigated with relatively small SPDs because their energy-delivery capability is relatively small [IEC document 64/1034/CD, 1998]. In contrast, current surges are produced either by the dispersion of the current associated with a lightning flash when a direct strike injects current at some point of the power system, in close vicinity or at a more remote point of the power distribution system [Mansoor et al., 1998]. Another source of current surges is switching surges involving the injection of residual energy into parts of the power system. The energy-delivery capability of these current surges can be substantial, and be a threat to the survival of improperly sized SPDs. Note in passing the use of the term “energy-delivery capability” and not “energy in the surge.” Some of our readers are by now familiar with that theme [Lindes et al., 1997] — perhaps even tired of seeing it repeated — but the sad truth is that usage of the term “surge energy” is still rampant.

¹ Citations appearing in the text as [Author, date] are listed in alphabetical order in Section 7, Bibliography.

The significance of making the distinction between recording current surges versus recording voltage surges is very important for equipment designers. A decision to provide only modest surge withstand capability for an SPD incorporated at the power port of the equipment might be made because the contemporary surveys reveal few and moderate (voltage) surges. When combined with the misconception that “the lower the clamping voltage, the better” [Martzloff et al., 1989], the result can be disastrous. We have in our laboratory ‘morgue’ two examples of such mass-produced devices incorporating an inappropriate SPD that led to early mortality of the product. Without identifying the culprit — perhaps a harsh word for a designer who was lulled into this position by referring to misleading reports on surge activity — but to illustrate the situation, we can name the two products: a compact fluorescent bulb, and a remotely-controlled ceiling fan. To recite a recurrent theme in our tutorial presentations (“Transients Are Here to Stay”), voltage surges might appear to have faded away, but current surges are still here, ready to destroy a small SPD incorporated in a design based on the misperception of fading voltage surges, fostered by recording only voltage surges.

3. Experimental Measurements

To illustrate the effect of nonlinear SPDs as well as linear capacitors connected across the mains, the Power Electronics Applications Center (PEAC) “Upside-Down House” [Key et al., 1994] was used to inject surges into the service entrance of the Upside Down House with various combinations of SPDs and/or personal computer (PC) power supplies connected at the end of two branch circuits, one 9-m long, the other 36-m long. In Figure 1, currents in the branches and voltages at the nodes are identified respectively as I_s , I_9 , I_{36} , and V_0 , V_9 , V_{36} . The charging voltage setting of the Combination Wave surge generator was kept constant to provide a 2 kV, 1.2/50 μ s open-circuit voltage (OCV). In a second series of experiments, the setting was increased to 4 kV. This second series, not reported here in detail because of limited space, confirmed the expected nonlinear response of varistors and the linear response for capacitors only.

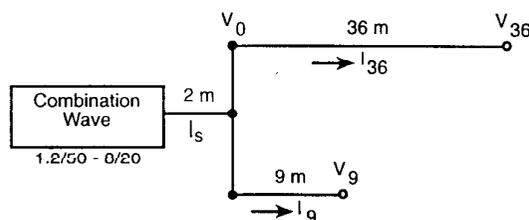


Figure 1 - Upside-Down House branch circuits

3.1 Metal-oxide varistors only

Given the known and predictable behavior of multiple SPDs — often reported in the literature to the point that bibliographic citations would take several lines to list all of them — this part of the experiment was performed only to provide a baseline. Metal-oxide varistors (MOVs) rated 150 V, 20 mm diameter, were used for this experiment. To record all interesting currents and voltages, two shots are necessary when using the 4-channel digital signal analyzer; therefore each oscillogram in the figures contains the trace of V_0 to serve as a common reference (Figure 2).

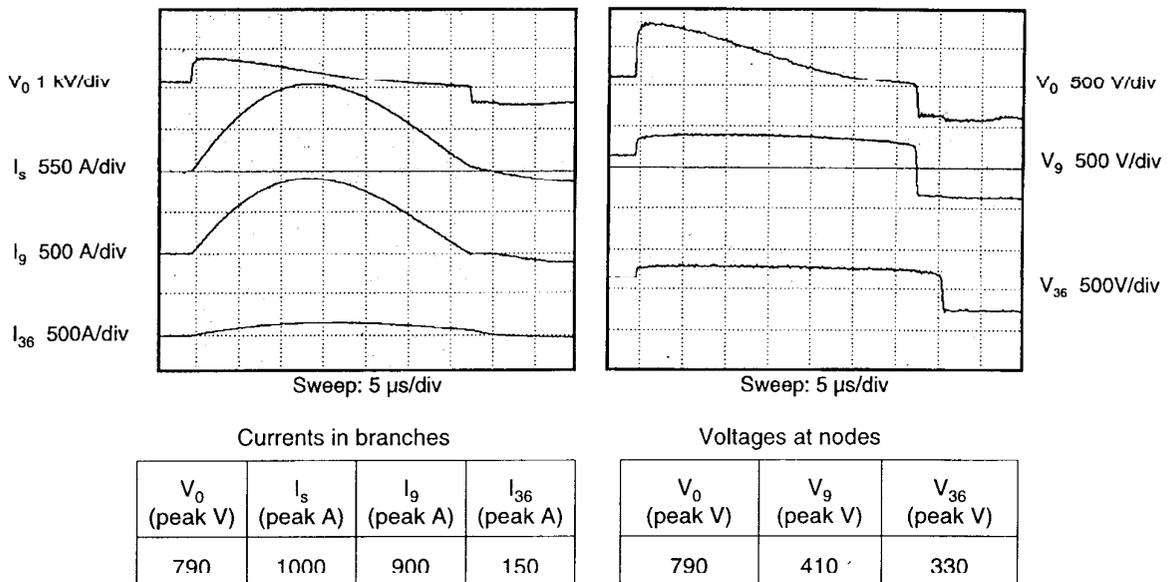


Figure 2 - Typical recordings¹ made during the first experiment – SPDs only.

In Figure 2, the current traces (left oscillogram) show the unequal sharing between the two MOVs, reflecting the difference in the inductance of the two branch circuits. Because the peaks of the two branch-circuit currents are not simultaneous (the current in the longer branch circuit takes longer to build up) their sum seems to exceed the peak of the injected current, I_s .

The voltage traces (right oscillogram) show how the voltage at the service entrance, V_0 , is mitigated from the 2 kV open-circuit voltage supplied by the generator that would propagate without attenuation in the absence of a surge-mitigating device [Martzloff et al., 1986]. This voltage V_0 is the sum of the clamping performed by the varistor at the node V_g , and the inductive voltage drop in the 9-m long connection. Note that this inductive voltage is additive during the rise of the current I_g , and subtractive during the fall, hence the apparent “overshoot” in the trace of V_0 , compared to the flat-top trace of the varistor at V_g . The voltages at V_g and V_{36} are the typical clamping voltages of the MOVs corresponding to the current they carry.

In a subsequent experiment with a 4 kV open-circuit voltage setting of the generator (twice the value of Figure 2, but not reported here in detail because of limited space), the voltages across the varistors, predictably, were not substantially increased. However, the substantially increased current in the 9-m long branch circuit (from 1000 A to 2800 A, resulting from the nonlinear response of the varistor) produced an increased inductive effect to the point that the voltage at the service entrance was raised to 1300 V from the 790 V recorded for the case of Figure 2. Table 1, at the end of this section, presents a summary of the peak values recorded in the various combinations of components, branch circuits, and amplitudes of the injected surge.

¹ The experimental values shown in Figures 2, 3, and 4, and in Table 1 were recorded with a digital signal analyzer. They have been rounded off to the nearest ten to convey a simpler set of numbers, uncluttered by a precise last digit. Uncertainty in these measurements is not an issue here as it does not affect the general conclusions.

3.2 Capacitors only

In a second set of experiments, less easily predictable would be the behavior of the still all-linear circuit involving the capacitors of a PC power supply (440 μF each in this experiment) when receiving a surge originating from the complex RLC wave-shaping network of a Combination Wave surge generator (Figure 3).

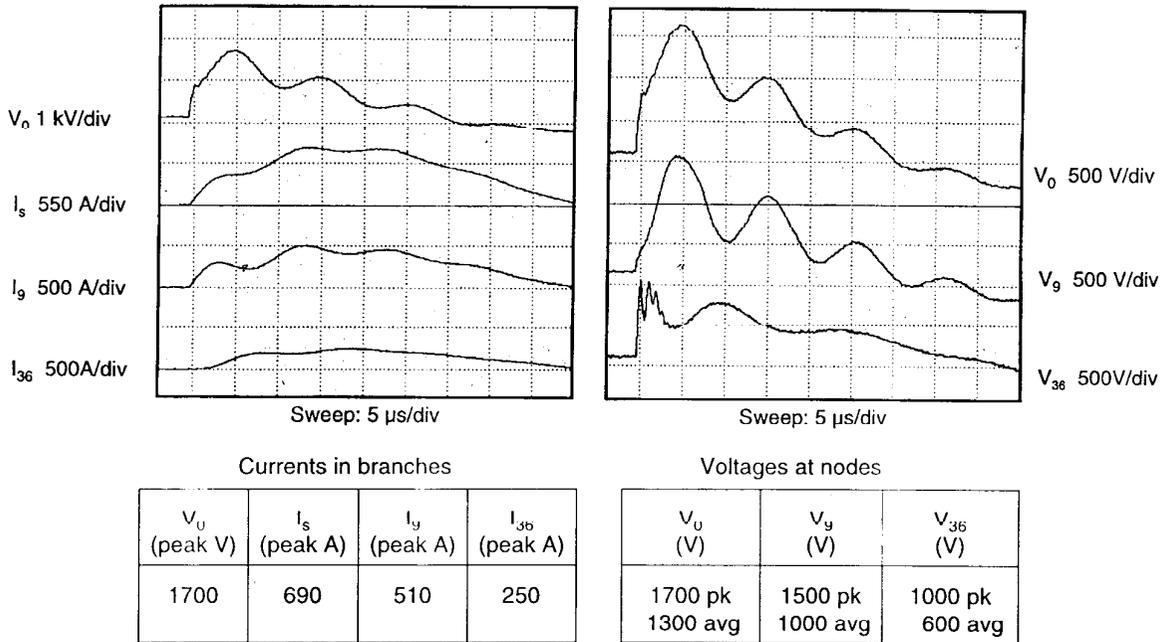


Figure 3 - Typical recordings made during the second experiment – Capacitors only

In Figure 3, the voltage at the service entrance, V_o , is mitigated from the 2 kV open-circuit voltage supplied by the generator. However, because of the interaction between the RLC components in the wave-shaping network of the Combination Wave generator on the one hand, and the capacitances of the PC power supplies and inductances of the branch circuits on the other hand, this voltage rings around an average voltage level of 1300 V, reaching a peak of 1700 V. To reflect this situation, the tabulation of the voltages in the figure shows two lines, peak and average. Thus, the mitigation effect is degraded by the ringing. Nevertheless, one can expect that as more PCs would be added, the ringing frequency would become lower and the voltage peaks lower.

3.3 Capacitor and MOV

In a third set of experiments, a capacitor was connected at the end of the 9-m branch circuit and an MOV was connected at the end of the 36-m branch circuit. Interest in this particular configuration was motivated by the desire to show how an MOV would mitigate the ringing that was observed in the preceding experiment at the end of the 36-m line (V_{36} in Figure 3).

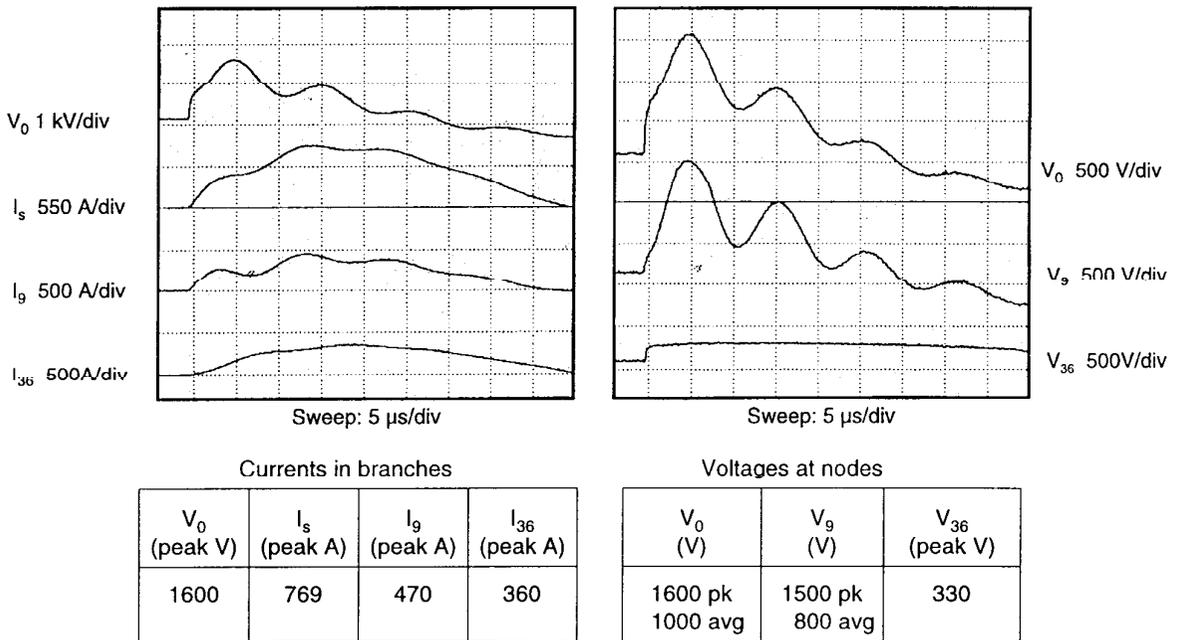


Figure 4 - Typical recordings made during the third experiment – capacitor and MOV.

Experiments 2 and 3 (Figures 3 and 4) do show decreasing levels of voltage surges at various points of the Upside Down House, compared to the open-circuit conditions. However, the interactions between the capacitors of the generator RLC wave-shaping network on the one hand, and the PC capacitors on the other hand, make detailed interpretation of the waveforms tedious and beyond the scope of our illustrative examples. Numerical modeling with a current source, as reported in the following section, avoids this interaction and provides further evidence on the “PCs Galore” effect. For readers interested in the details, Table 1 documents the results concerning peak voltages noted from the oscillograms kept on file but not reproduced here. Readers interested primarily in the big picture may skip a close examination of this table.

Table 1 - Summary of node voltages for component combinations

Detailed experiment description number	Device Combination		Voltage for 2 kV OCV (peak V)			Voltage for 1 kV OCV (peak V)		
	9 m	36 m	V_0	V_g	V_{36}	V_0	V_g	V_{36}
	Open	Open	2500	2600 *	3300 *	Not done (linear)		
		MOV	1700	330	1900 *	1300	400	370
		Capacitor	1800	1900 *	1300	Not done (linear)		
3.1 - MOVs only	MOV	Open	860	400	1300 *	1400	400	2000 *
		MOV	790	410	330	1300	390	400
		Capacitor	720	420	700	1300	390	990
3.3 - Capacitor and MOV	Capacitor	Open	2000	1600	2100 *	Not done(linear)		
		MOV	1600	1500	370	Capacitor failed, end test		
		Capacitor	1700	1550	1100	Not done (linear)		
3.2 - Capacitors only								

* Ringing at the open-ended line is the cause of this voltage being greater than V_0 .

4. Numerical Modeling

Our previous experience with modeling cascaded SPDs [Lai et al., 1993] and the behavior of the Upside Down House with installed SPDs [Martzloff et al., 1995] can be readily applied to the prediction of the behavior of the three circuits subjected to the experimental measurements, with an imposed current source, free from the unavoidable interactions that occurred in the preceding experimental measurements. This approach yields the best of the two methods: a computation that has been well demonstrated as suitable for modeling nonlinear SPDs, and the freedom to impose any fixed waveform — what reality imposes on a residence is not the surge from an impedance-limited surge generator — combined with the possibility to model many branch circuits and many combinations of SPDs and/or capacitors.

4.1 Modeling combinations of loads and branch circuit lengths

In a series of modeling runs similar to the combinations of the experimental measurements, the model used the circuit of Figure 5. A current source feeds a fixed current surge via a common service drop to the panel bus of the service entrance, where three branch circuits made of 2-mm diameter conductors (#12 AWG) take off, with length of respectively 5 m, 10 m, and 20 m. The choice of these lengths was based on curiosity about the effect of the long distance connection (and thus an effect that might be delayed or degraded) to the immediate occurrence of an overvoltage at the end of shorter branch circuits. The currents in the circuit are identified in Figure 5 respectively as I_s , I_0 , I_5 , I_{10} , and I_{20} , with the node voltages at the service bus and branch circuit ends respectively as V_0 , V_5 , V_{10} , and V_{20} .

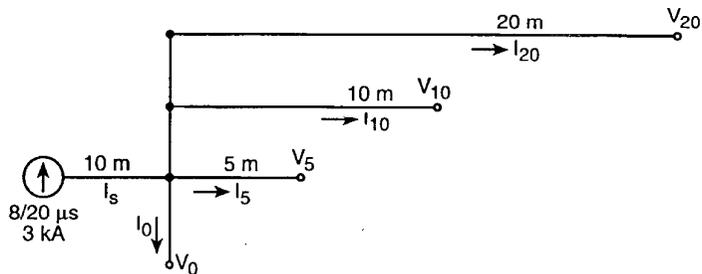


Figure 5 - Service drop and branch circuits for modeling combinations

As in our previous modeling applications, the EMTP program [EPRI, 1989] was used. To avoid computational artifacts, a finite $10 \text{ k}\Omega$ resistance was always postulated at the ends of all branch circuits, whether these were left in “open” or “loaded” condition. The loads that were modeled included combinations of SPDs, capacitors, and 100Ω resistors. The SPDs were all 20-mm diameter, 130-V rated MOVs. The capacitors included a capacitance of $440 \mu\text{F}$ and a series resistance (“ESR”) of 0.25Ω to represent an electrolytic capacitor. Initial conditions for the models stipulated an initial charge on the capacitor to represent the normal condition of the DC link. The 100Ω resistor was selected as a typical value for a 150-W power-consuming appliance connected at the end of the branch circuit.

Just to illustrate the point of a readily predictable behavior of a purely linear circuit, Figure 6 shows the voltages and currents for the case of 100 Ω loads confronted with a current source. Compared with the 100 Ω load at the end of the branch circuits, the series impedance has a very small effect and the impinging surge current I_s divides almost equally ($3000 \text{ A} / 4 = 750 \text{ A}$) in the nearly identical four branches I_0 , I_5 , I_{10} , and I_{20} (I_0 being the current in the service entrance ‘branch’, with a length of 0). Thus, the voltage developed by this 750 A current across 100 Ω would simply be expected to be 75 000 V (seventy five thousand volts). We say “would be expected” because, of course, the insulation level of a real-world low-voltage insulation cannot withstand such a voltage. The result of this theoretical case is another illustration of the theme “More Begets Less,” according to which a high-amplitude, steep-front surge cannot propagate in branch circuits because a flashover will occur at the origin [Mansoor et al., 1998].

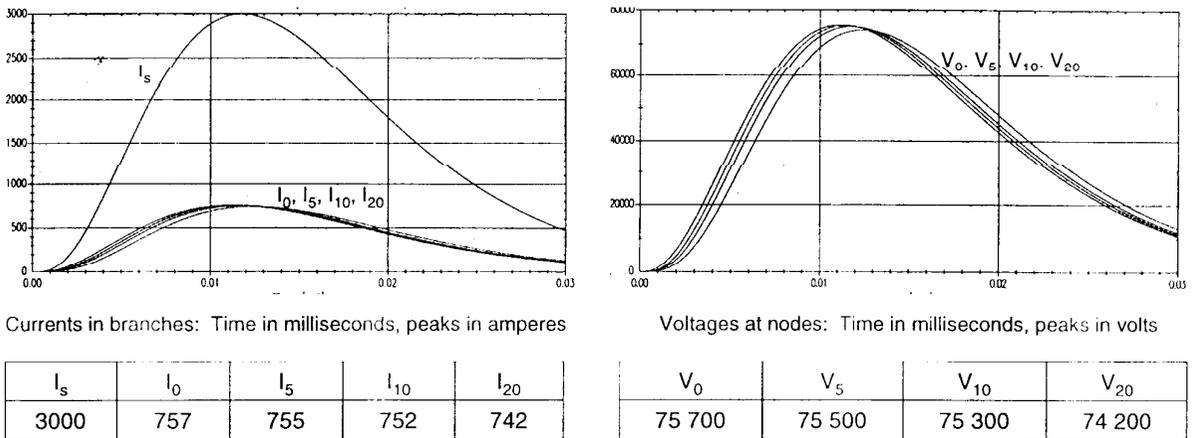
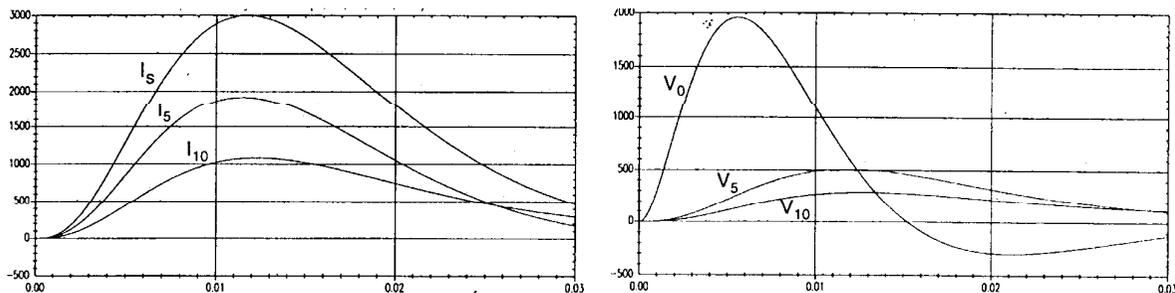


Figure 6 - Currents and voltages for the baseline case of 100 Ω loads without SPDs

Figure 7 shows the example of two PCs, one at the end of the 5-m branch circuit, the other at the end of the 10-m branch circuit: the surge current is shared (unequally) between the two capacitors, with a corresponding decrease of the voltage level at each DC link, and also a reduction of the voltage at the open end (unprotected) of the 20-m branch circuit. This example shows the beginning of the “PCs Galore !” effect. Several intermediate and further steps were modeled by adding combinations of surge-mitigating devices to build our case file and confirm the expected effects, but we will spare the reader from a tedious recitation.

The results shown in Figure 6 call for several comments:

- The unequal division of the currents I_5 and I_{10} reflects the effect of the larger impedance of the 10-m branch circuit, compared to the 5-m branch circuit.
- Substantial mitigation is obtained at the nodes V_5 and V_{10} , as a result of the filter-like action of the line inductance and the capacitance of the PC power supply.
- The voltage surge developed at the node V_0 propagates, unabated, to the end of the open-circuited 20-m branch circuit, a reminder that the notion of voltage surges being attenuated as they propagate in building wiring [IEC Report 664, 1980] was incorrect.



Currents in branches: Time in milliseconds, peaks in amperes

Voltages at nodes: Time in milliseconds, peaks in volts

I_s	I_0	I_5	I_{10}	I_{20}
3000	0	1920	1080	0

V_0	V_5	V_{10}	V_{20}
1960	507	290	1960

Figure 7 - Currents and voltages for the case of two PCs without SPDs

The voltage results are especially worth noting, in the context of what a power quality monitor would report in such an installation, depending upon its point of connection.

- From the (impossible) high voltages of the 100 Ω baseline (no SPDs), the voltage that would be recorded by a monitor installed at the service entrance is now reduced to “only” 1960 V.
- A **voltage-only monitor** installed at the point of use of the power (typical selection of point of installation in many surveys), namely the two receptacles at 5 m and 10 m feeding the PCs, would report respectively 510 V and 290 V, **creating the illusion (fallacy) that there is no significant surge activity** at these points.
- In reality a current surge I_5 of nearly 2000 A is carried by the PC rectifier into the capacitor, via a line fuse. Such a high current — **undetected by a voltage-only monitor** — could very well be fatal for the rectifier or the input fuse, or for the capacitor of the PC power port. Such failures were found in post-mortems of equipment recently performed at PEAC. That scenario is also what occurred in the experimental test of Section 3 for a 4 kV open-circuit voltage and capacitor-MOV combination (shown in next-to-last row of Table 1).

4.2 Modeling PCs Galore

As a grand finale for illustrating our major theme, the case of an increasing number of PCs was modeled, with each of the PCs connected at the end of a dedicated branch circuit, with all branch circuits having the same length of 20 m, still with the 10-m long service drop driving a 3 kA current surge into the installation.

Table 2 shows the resulting voltages at the service entrance and at the point of connection of the PCs, as well as the imposed impinging current and the resulting currents in the individual power ports of the PCs. Indeed, the effect is linear with increasing numbers of PCs, and the resulting decrease in the voltages expected to be reported by a power quality monitor installed at the point of use is quite apparent.

Table 2 - Effect of an increasing number of connected PCs

Number of	Impinging current (A)	Current at PC port (A)	Voltage at service entrance (V)	Voltage at PC point of use (V)
1	3000	3000	5600	790
2	3000	1500	2800	400
5	3000	600	1200	160
10	3000	300	570	79

Inspection of the table shows that the effect is practically proportional to the number of PCs in use in the installation. While we have used the short acronym of PC in the title and preceding discussions, the proliferation that we observe is not limited to PCs, but includes many electronic appliances, such as home entertainment, heating and air conditioning with adjustable speed drive, that use a DC link with large energy-storage capacitor.

5. Action Items

The customary closure of a paper is to list conclusions. However, in this case we suggest action items for industry, rather than academic conclusions:

- The examples given here clearly show that the fallacy of little surge activity can be created by limiting power quality measurements to voltage surges. With undetected current surge activity, users of electronic appliances will be puzzled by unexplained failures in the face of reports of little voltage surge activity.
- We offer the explanation that these unexplained failures are likely to be associated with the (heretofore not characterized) surge current delivery capability of the environment. Such failures could have been avoided, had designers been better informed.
- With our leading theme now solidly established, questions arise about what to do about it. This matter requires the dedicated attention of both manufacturers and users of power quality monitoring instruments.
- Unfortunately, more than three years after beginning the crusade to overcome the fallacy of limiting surge measurements to voltage surges [Martzloff, 1995], there is no commercially available power quality monitor capable of characterizing the energy-delivery capability of a surge event.
- Worse yet, among the international delegates to an IEC working group chartered to develop a standard on measurement of power quality parameters (which is likely to dominate the design of future monitoring instruments), there are some claims being made that “a current surge is not a power quality parameter” and therefore it should not be included in the emerging standard prescribing measurements methods. This misconception needs to be corrected.
- Therefore, the crusade must be pursued with perseverance, and it is the goal and hope of the authors that sufficient recognition of the fallacy will eventually create a market demand for appropriate instruments, which forward-looking manufacturers may have developed or may be in the process of developing in anticipation of such recognition.

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**ON THE
DISPERSION
OF
LIGHTNING
CURRENT
AFTER A
DIRECT FLASH
TO A BUILDING**

On the Dispersion of Lightning Current After a Direct Flash to a Building

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Significance:

Part 2 Development of Standards – Reality checks
Part 4 Propagation and coupling of surges

Comparison between two simplified modeling studies of the dispersion and a documented case of the complexity of a direct flash to a residence.

Reservations on the justification of very high stress requirements for SPDs are expressed in a discussion, followed by a proposal to encourage more information sharing on the subject



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**ON THE DISPERSION OF LIGHTNING CURRENT
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Abstract: Simplifications often made when simulating the dispersion of lightning current can yield results that depart too much from the complex reality of a direct lightning flash to a building. The unpredictable occurrence of side-flashes increases even more the complexity. Such simulations, if taken at face value, might lead to unrealistic specifications for service-entrance surge-protective devices (SPDs). A real-world anecdote illustrates both the complexity and a case where an SPD with only modest ratings, compared with some present proposals, provided satisfactory protection on the power-port appliances of a residence.

Key Words: Lightning current dispersion, side-flashes, surge-protective devices

1. INTRODUCTION

Recent discussions among participants in the development of surge-protection standards have shown a lack of consensus on the possible scenarios concerning the dispersion of the lightning current when a direct flash to a building is involved. Skepticism has been expressed both on the simulation of available paths for the dispersion, and on the magnitudes and waveforms postulated for the resulting currents flowing in the conductive elements of the installation — especially the service entrance surge-protective devices (SPDs). Note that the skepticism does not aim at the parameters of the lightning flash itself, which have been accepted now for many years [1], but at the simulation conclusions concerning requirements for service-entrance SPDs with very high current-handling capability.

Another objection has been that the distinction between a building equipped with a lightning protection system (air terminals, down-conductors and earthing system), on the one hand, and a building without such a system, on the other hand, might be misleading.

Every building that contains electrical circuits (power or communications), electrically-conducting mechanical elements, metallic structures, etc., has a de facto lightning ‘protection’ system of intended or unintended air terminals and down-conductors — except that their connection to the earthing system might have unpredictable and unwanted side effects.

As an input toward developing consensus, this paper reviews in a first part two examples of simulations that have been performed by others, and in a second part relates a real-world anecdote of a corroborated case of a direct flash to a residence.

2. SIMULATING DISPERSION

2.1 Examples of simulations

A Joint Working Group of the International Electrotechnical Commission (IEC) has recently developed a Technical Report on surge protection [2]. This four-year effort involved the participation of five IEC Technical Committees interested in the subject. The data base considered by the group included, among many sources, two published papers, identified in the Bibliography of the report, authored independently by members of the group.

In both studies, a 10/350 μ s waveform was postulated, and a time-invariant earthing resistance and inductance were postulated. Currents in the available paths to earth and voltages at selected points of the systems were computed. For the purpose of this paper, three figures only are reproduced here for a qualitative glimpse on the results.

Figure 1, simplified from Ref [2], shows the nature of circuit components and configuration: two buildings and the distribution transformer linked by a cable in a linear arrangement. The point of strike is Building 1. The detailed numerical values, which are given in the referenced paper, are not significant for this comparison of the two studies.

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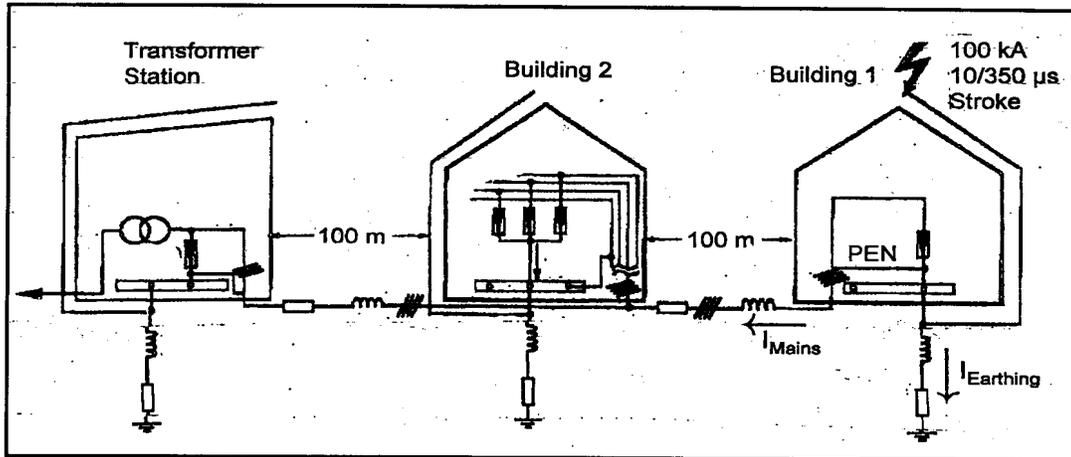


Figure 1 — Circuit components and configuration used in the Hasse simulation (Ref [2])

Figure 2 shows examples of the computed waveforms for currents at selected points of the Figure 1 circuit, from top to bottom : the postulated stroke current, the current *exiting* Building 1 via the power supply cable, the current in the earthing impedance of the building, and the current in the service-entrance SPDs of Building 2 resulting from the surge that is now, for Building 2, an *impinging* surge.

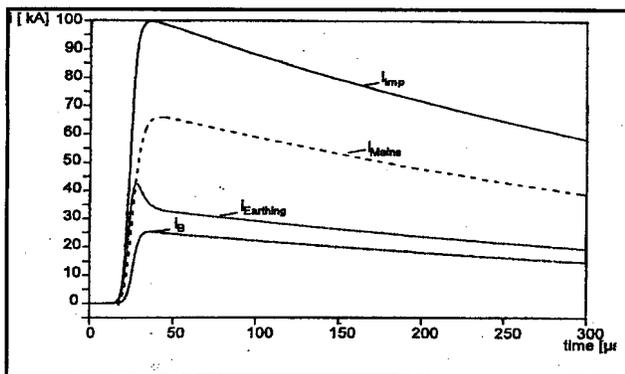


Figure 2 — Current dispersion for Figure 1

The numerical values are not significant, but the waveforms are. The current exiting the building (I_{Mains}) has the same waveform as that of the stroke. The earthing current (I_{earthing}) has an initial peak, due to the additional inductance of the power supply cable; in the long term, the inductive effect disappears, and the current division simply reflects relative values of the available earthing resistances.

In contrast with Figure 1 where the buildings are strung along a power supply cable, Figure 3, from Ref [3], shows a radial configuration of three buildings, each supplied by its own service drop, with all three connected directly to the terminals of the common distribution transformer. Varistor-type service-entrance SPDs are provided for each building. The transformer and each building have their own earthing electrode connection, represented by a fixed resistance and an inductance.

The radial service drops also consist of a resistance and an inductance, not drawn in the figure, but modeled in the computation according to the 20-m length of each radial drop. The point of strike of the flash is the earthing system of Building 1 (to which the neutral is bonded).

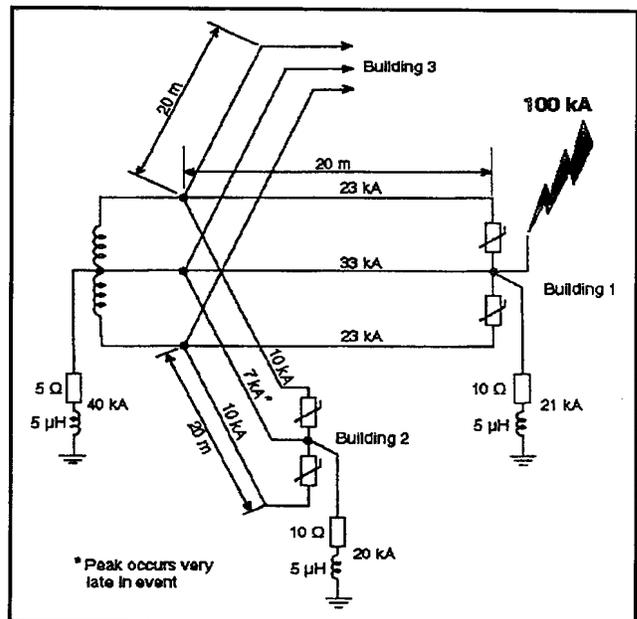


Figure 3 — Buildings configuration and resulting currents, according to Mansoor (Ref [3])

The significance of citing these two independent studies, separated by an ocean, by two different languages used by the authors, and by two different simulation programs, is that quite compatible conclusions were reached after exchanging information, first across the table during IEC working group meetings, then later on, via intensive e-mail messages, as a working relationship blossomed in spite of logistics barriers. This important point will be elaborated further in Section 7 of this paper.

With hindsight, it appears predictable that the initial current dispersion (the first ten microseconds) should be strongly influenced by the relative values of the postulated inductances, and the later dispersion by the relative values of the postulated (time-invariant) earthing resistances.

2.2 Involvement of service-entrance SPDs

The results of these simulations show that the stresses imposed on the service-entrance SPDs that become involved in dispersing the part of the lightning current toward remote earthing electrodes of the power distribution system will reflect the relative values of the earthing impedances. These stresses also vary with the postulated current waveform, ranging from the short 8/20 μ s long-used for designing SPDs, to the more recent proposed 10/350 μ s and finally to the seldom-considered continuing current within a multiple-stroke flash.

Performing these simulations was not a futile exercise but provided insight on the influence of significant parameters. However, among end-users, this complexity of postulates decreases the credibility of defining SPD requirements on the basis of simulations, giving a greater credibility to field experience of widely-used SPDs that have demonstrated satisfactory performance over many years.

3. SIDE FLASH

One event that contributes to the complexity and uncertainty of lightning current dispersion is the possible occurrence of a side-flash. A side-flash can establish unexpected paths to earth, with two consequences that extend beyond the consideration of service-entrance SPD stresses — the motivating concern for this paper.

- The side-flash itself can have hazardous consequences by acting as an igniter, as will be told in the anecdote of Section 4.
- The side-flash can cause currents to flow along conductive paths within the installation, thereby coupling transient overvoltages in the circuits of the installation, by common path or by induction.

Of course, the latter has the same end-result as what the bonding applied to avoid the side flash will produce, except for its unpredictability.

4. FROM SIMULATIONS TO REALITY

4.1 Setting the stage

To illustrate the credibility gap that separates reality from simplified representations, the following story should be narrated:

.... Once upon a time, in a far-away land (Upstate New York, U.S.A.) there lived an engineer who was recording surges, writing papers and presenting tutorials on surge protection, including the need for good bonding practices.

This engineer had bought a house from the previous owner who had lived many happy years there without any problem, so that our engineer made the (unwarranted) assumption that the house and its electrical wiring were in good order. The house was surrounded by several tall, mature trees so he thought that the cone of protection from the trees would benefit the house. Alas ! All-knowing Zeus recognized that this engineer needed to be taught a lesson on reality and thus sent a downward stepped leader toward the general area of the engineer's house ...

By now, dear readers, you have guessed that our mythical engineer is none but the author of this paper ... who will now offer this true story for your edification. First, the "where" : Figure 4 shows a simplified (here we go again ...) topology of the house, a two-story wood frame with basement and attic. Utilities (power, telephone, and cable TV), all entered, via overhead service drops, at the rear of the house, while water and sewer underground pipes were at the front of the house. The telephone system was not involved in the incident and therefore is not shown in the figure. The power installation included the usual revenue-meter (outside) and service panel (inside) with circuit breakers controlling a multitude of branch circuits. Only three are shown in the figure: lighting fixture in the attic, TV on the second floor, and a counter-top receptacle (via ground-fault interrupter) for the kettle (② in Figure 4) sitting on the enamelled cast-iron kitchen sink. (The significance of this detail will surface shortly.)

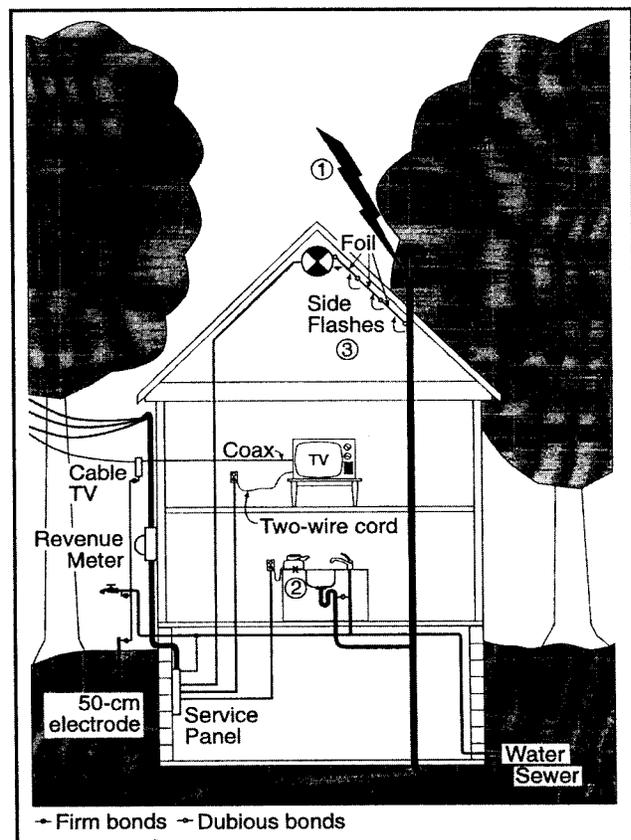


Figure 4 - Simplified configuration of anecdotal story

Water service and indoor piping were all copper, with a bond between the ground bus of the service panel and the nearest cold-water pipe. There was no visible grounding conductor to a (non-existent) made-electrode, but the perception existed that, given the vintage of the house (circa 1920), the water piping was sufficient, in addition to the multiple-grounded neutral of the power company.

Upon moving in the house, I had installed in the service panel a surge arrester (circa 1965 vintage) consisting of a silicon-carbide disk in series with a stamped-metal air gap. The cable TV service, as originally installed by the utility, only had a 50-cm long picket allegedly serving as "ground". Having been exposed to the concept of bonding, I had installed a bond between the picket and the nearby outdoor water faucet. A very passive — but soon to become active — part of the installation was the typical sewer system made of lead-bonded cast-iron pipe extending a to vent through the roof and connected to the street sewer, still with cast-iron pipes and thus offering the topology of a well-grounded air terminal, albeit below the peak of the roof. But I am getting ahead of myself, as I had never considered this vent as a lightning air terminal, since the house was surrounded by taller trees and thus "obviously" within their cone of protection.

4.2 Zeus' wrath

Now for the "When" and "How" : On the day when the tale unfolded, my wife (the corroborating eye-witness) and I were standing in the kitchen, listening to the approaching thunder and watching the big drops of rain just beginning to splash on the window. Then, a bright flash outside, with an immediate, deafening thunderclap, and also we both saw a small flash under the kettle. "That was a close one" we both said, whereupon I proceeded to check all appliances in the house. Several were inoperative, but a check of their branch circuit breakers revealed that they had tripped, and resetting them restored order. The only one that did not work was the old TV set, although there was no evidence of severe damage or burned smell, and we considered ourselves lucky — until a smell from the attic attracted my attention: the ceiling of the attic (which was covered by cellulose-base panels) was smoldering !

Fortunately — and not by accident — a handy fire extinguisher allowed me to quench the smoldering, while my wife called the fire department. To their credit, they were in front of the house within minutes. I told them that I believed that the fire extinguisher had done the job; nevertheless, one fireman proceeded to climb on a ladder to the attic window and hacked it away to let the smoke out, while another entered the house, pushing me aside, with a high power water hose in tow — which fortunately he did not turn on. After ripping several of the ceiling panels to verify that the fire indeed was out, the firemen left, with our emotional thanks and the applause of the neighbours gathered in front of the house.

4.3 The homeowner's epilogue

- One obsolescent TV receiver, which was not repaired, but catalysed the purchase of a new and upgraded set (missing the opportunity to do an extensive post-mortem as in the "Case of the Cozy Cabin" [4]).
- Several hundred dollars expended to repair the window destroyed by the firemen, install a splice on one attic rafter weakened by charring, and replacing the ripped panels.
- After recovering from the shock, a realization of how lucky to have been in the house at the time of the incident, and glad for the foresight of having a fire extinguisher on every floor of the house !

4.4 The engineer's epilogue

Such a traumatic experience called for an investigation of the incident. The first observation was that the previous owner had installed insulation between the attic rafters, stapling the aluminum foil of the bats to the rafters, but not overlapping them across the edges of the rafters. This arrangement, concealed by the panels, created several gaps along the 5-m distance separating the sewer vent pipe from the light fixture at the apex of the attic, but reducing the total gap to a few centimetres — an easy side-flash scenario, resulting in the ignition of the dust and surface fuzz of the rough-from-sawmill rafters.

The second observation, a few days after the incident, was to notice a small rust spot on the kitchen sink where the kettle usually sat: there was a small hole in the otherwise good-condition glaze, exposing the underlying cast iron: The flash seen under the kettle (②) was the cause of the enamel puncture; several kilovolts must have been required to break down the series-connected insulation of the heating element inside the kettle, and the porcelain glaze of the sink. The electronic ground-fault circuit breaker controlling the receptacle had to be reset, but it was not damaged, and subsequent use of the kettle did not cause it to trip, so we concluded that the brief breakdown of the insulation of the heating element was not a massive event.

The immediate action was to install a bond between the sewer vent pipe and all extraneous metal in the attic. The long-term effect on the engineer was a consciousness-raising on the issue of surge protection of multiple-port appliances, even though a bond had been provided between the incoming cable TV service and the power system [5]. At that time, the concept of the surge-reference equalizer [6] had not yet surfaced, and no commercial device was available to provide that function. In fact, the proliferation of plug-in surge-protective devices launched by the introduction of metal-oxide varistors had not yet occurred.

A casual inspection of the fist-sized surge arrester at the service panel showed no distress, an indication of adequate design for the rare scenario of a direct strike to a building [7].

This arrester used only a 30-mm diameter silicon carbide disk as varistor, which most likely would be destroyed by the high-energy lightning surges presently considered or recommended by some IEC committees. (Sorry, the house has changed owners and an exhaustive test on that particular surge arrester, desirable as it would be in retrospective, is not possible.)

The attic side-flash (③ in Figure 4) clearly indicated that the sewer vent pipe was the point of strike (①), raising the question of why the tall trees failed in their expected mission of establishing an effective cone of protection. Perhaps one explanation might be that during the initial part of the rainfall, the still-dry trees could not emit a successful competing upward streamer, compared to the well-grounded cast-iron pipe. Comments from lightning physics experts on this speculation would be welcome.

Thus, our engineer had learned his lesson, and lived happily without further incident for fifteen more years in the far-away land. However one cannot say 'lived happily ever after': After moving to a new home further South, one night a nearby lightning flash triggered a burglar alarm (which had to be pried open to silence the horn turned on by a failed semiconductor, at 02:00 am no less) and damaged a remote-control garage door opener: Zeus had still kept track of the battle-hardened surge-protection engineer, but that is another story ...

5. FROM REALITY TO SIMULATION

Among several investigations based on rocket-triggered lightning, the ongoing effort at Camp Blanding in Florida, U.S.A. is aimed at injecting a lightning current at specific points of the replica of a residential power system. Initial results (1997) were inconclusive because of instrumentation problems, but as these are progressively overcome, more definitive information becomes available. Actually, the most recent report [8] provides so many raw measurement results that an effort of synthesis will be necessary to gain a better understanding of the issues.

The major advantage of such systematic projects over a random recitation of anecdotes could be the possibility of going from a real-world configuration to a sufficiently detailed numerical representation of the circuit parameters. A cross-validation of the measurement results and of the simulation results would then significantly increase the credibility of both, and lead to realistic designs and ratings for SPDs.

The challenge, of course, will be to represent enough of the many, many parameters involved in the real world but not so many as to make the simulation model unmanageable. For instance, the real-world situation of the anecdote already simplified in Figure 4 — with the ill-defined bonds and side-flashes — would be difficult to turn into a manageable and credible simulation.

6. DISCUSSION

The simplified assumptions on lightning current dispersion illustrated in Section 2 have met with some skepticism among the North-American surge-protection community and perhaps others. Part of this skepticism is also based on the relatively rare occurrence of massive failures for secondary arresters (distribution transformer secondary terminals and residential service entrance) designed to withstand the "classical" 8/20 μ s or 4/10 μ s surges, at crest levels of a few to a few tens of kiloamperes. Furthermore, the two simulations cited in Section 2 were based on the assumption that earthing electrodes have a constant resistance during the flow of the lightning current, an assumption that is questioned on the basis of preliminary results of measurements made in Florida in connection with triggered lightning experiments [8].

In contrast with these simplified scenarios, the real-world anecdote would be a challenge for any numerical modelling but demonstrates evidence of substantial overvoltages developed in the installation (insulation puncture at the kettle) during the flow of this undetermined lightning current dispersion among the complex available paths to earth. The anecdote also offers an example of a surge arrester with modest current-handling capability surviving the scenario of a direct strike to a building.

A symptom of the incomplete consensus is the noticeable lack of a discussion of risk analysis in the report developed by the IEC Joint Working Group [2]. This topic was initially included in the document outline, raising high expectations, but, confronted with incompatible proposals, the group gave up on that initiative. The proposed methodologies ranged from elaborate and detailed mathematical formulae — which turned out to be using somewhat arbitrary postulates — to common-sense, almost intuitive considerations.

7. A PROPOSAL FOR THE DISPERSION OF LIGHTNING INFORMATION

In a 1963 freedom-seeking speech that still resonates today, the mantra "*I have a dream*" was coined. On a much more modest scale, the author has a dream of unfettered information-sharing on lightning. Having cited the preceding examples of developing, but still incomplete, consensus on the dispersion of lightning current, here is the proposal (or is it a challenge?): Hopefully helpful timely participation, on a world-wide basis via electronic mail could supplement — not compete with — the established routes for information sharing, at a much accelerated pace. We are still mostly in a mode of developing standards — a notoriously slow process — by volunteers or delegates often hampered by travel budgets, or of publishing peer-reviewed papers — unquestionably a wise process, but entailing long delays between generation and ultimate publication of the information.

This process of information dispersion might take one of the many forms by which the Internet has revolutionised information sharing. Should this paper be accepted for oral presentation at the Conference, the author would propose to make only a very brief summary of the paper itself — available to all in print — and make use of the scheduled presentation time for a cross-pollination of ideas among the attendees (much superior to the one-on-one poster process) on how to implement the proposal, bringing reality to the dream. Pessimists will point out hurdles such as the requirement of “previously unpublished information” for later acceptance of an archival paper reporting research on the subject, or the understandable modesty of researchers who want to be sure that the work is complete before publishing even preliminary results, and so forth. Optimists will find ways to by-pass these hurdles and broaden an early consensus.

8. CONCLUSIONS

- While there is no disagreement, or at least very little skepticism, on the specific parameters of the lightning discharge, consensus on the implications of lightning current dispersion for the rating of surge-protective devices has not yet been reached.
- Anecdotal information offered in many countries on their experience with service entrance surge-protective devices having moderate handling capability suggests that the proposed ratings for very high duty levels might be unnecessary and not cost-effective, unless a convincing risk analysis demonstrates otherwise.
- Information dispersion on these issues could be greatly enhanced by establishing an informal and time-sensitive world-wide site (in parallel, not in conflict with more formal procedures), which the author is prepared to undertake if encouraged and supported by colleagues in the lightning-protection community.

9. REFERENCES

9.1 General

Many publishing organizations, in their instructions for the peer-review process, raise the question “Are references adequate to show knowledge of work by others?” or words to that effect. While undoubtedly a valid question, the result is sometimes a lengthy recitation of up to several hundred citations, which seems an overkill.

Standard-writing organizations have evolved the concept of differentiating between, on the one hand “References” — a listing of documents that are made an integral part of the standard by a ritual introductory statement, and, on the other hand, *citations* — in the form of a “Bibliography” with or without annotations.

For this paper, “References” are limited to the strict minimum necessary to support a particular point being made. To illustrate where extensive listing of “references” might lead, a literature search was conducted with “lightning” as a leading key word, and next with one additional word. The results are listed below, showing the number of “hits” found for the period of just 1969-1999 — the accessible on-line data base did not include Benjamin Franklin’s seminal letters to the Royal Society on lightning protection of houses and the Purfleet munitions storage [9].

Lightning	15 791
Lightning + surge	2348
Lightning + current	3306
Lightning + damage	1130
Lightning + protection	6349
Lightning + arrester	1816
Lightning + earth + electrode	139

These numbers show that it would be unrealistic for a single researcher to examine in detail the contents of fifteen thousand papers. Injecting the concerted filtering and sharing action of today’s active researchers into a readily accessible data base — the author’s dream — would be a great improvement.

9.2 Specific references

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**ROLE AND
STRESS OF
SURGE-
PROTECTIVE
DEVICES
IN SHARING
LIGHTNING
CURRENT**

The role and stress of surge-protective devices in sharing lightning current

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Significance

Part 2 Development of standards – Reality checks

Part 4 Propagation and coupling – Numerical simulations

Most simulations performed to investigate the sharing (dispersion) of lightning current for the case of a direct flash to a building have focused on the role and stress of surge-protective devices (SPDs) installed at the service entrance of a building and their involvement in that part of the lightning current that exits the building via the the power supply connection to the energy supply.

The numerical simulations performed for this paper, based on a postulated waveform and amplitude suggested by current standards, include downstream SPDs, either incorporated in equipment or provided by the building occupant; the results show that a significant part of the exiting lightning current can involve those downstream SPDs with some likelihood that their surge withstand capability might be exceeded. Such a possibility then raises questions on the validity of the postulated amplitude in the face of the relatively rare occurrence of reported failures.

THE ROLE AND STRESS OF SURGE-PROTECTIVE DEVICES IN SHARING LIGHTNING CURRENT

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Abstract – This paper examines the sharing of lightning current associated with a direct flash to a building. This sharing involves not just those surge-protective devices (SPDs) that might be installed at the service entrance, but also all SPDs involved in the exit path of the lightning current. Such sharing might involve built-in SPDs of some equipment located close to the service entrance, but heretofore not included in numerical simulations performed by many researchers. From the numerical simulations reported in this paper, conclusions are offered that may influence the design and EMC testing of equipment, as well as the risk analysis associated with lightning protection.

I. BACKGROUND AND RATIONALE

This paper offers additional information to the body of knowledge accumulated on how the lightning current of a direct flash, injected into the earthing system of a building, is shared among the many available paths towards intended or opportunistic earthing electrodes.

Recent developments in the International Electro-technical Commission (IEC) and the Surge-Protective Devices (SPD) Committee of the Institute of Electronics and Electrical Engineers (IEEE) have focused on the role of SPDs connected at the service entrance of a building in the case of a direct lightning flash to the building. This scenario is described in IEC 61312-3 (2000) [9], IEEE PC62.41.1 [12] and PC62.41.2 [13].

Prior to this new focus, most of the considerations on SPD applications were based on the scenario of surges impinging upon the service entrance of a building as they come from sources external to the building. The new (additional) focus addresses the scenario of the earth-seeking lightning current as it is shared among the many possible paths to earth, including the deliberate and opportunistic exit paths of the building earthing system, services other than the power system connection and, mostly, the power supply connection.

Quite independently from these lightning protection considerations, the IEC Subcommittee SC77B had developed a series of documents on the electromagnetic compatibility of equipment, IEC 61000-4-5, Surge withstand capability [8] in particular. These documents were primarily concerned with immunity against typical disturbances, the rare case of a direct lightning flash to a building containing electronic equipment not included.

Increasing recognition of the need to include the scenario of a direct flash to a building – rare as it might be – has motivated the formation of an IEC Joint Task

Force TC81/SC77B for the purpose of considering surge stresses on equipment higher than those currently described in the IEC document 61000-4-5 on immunity testing [8].

The purpose of the paper is to examine in detail the sharing of lightning current, not just by the SPDs at the service entrance, but also by all SPDs that might be involved in the exit path of the lightning current. Such sharing might well involve SPDs incorporated in the equipment located close to the service entrance, but not always included in the numerical simulations that have been performed by many researchers (Altmaier et al., 1992) [1]; (Standler, 1992) [23]; (Rakotomalala, 1994) [20]; (Birkl et al., 1996) [3]; (Mansoor and Martzloff, 1998) [15]; (Mata et al., 2002) [19]. In its recent development of a Guide and a Recommended Practice on surges in low-voltage ac power circuits [13] the IEEE has refrained from identifying SPDs as being those that may be connected at the service entrance. Instead, it refers to "SPDs involved in the exit path" without reference to their point of installation.

Given the tendency of equipment manufacturers to include an SPD at the equipment power input port, the issue of "cascade coordination" arises. Several previous papers (Martzloff, 1980) [17]; (Goedde et al., 1990) [5]; (Lai and Martzloff, 1991) [14]; (Standler, 1991) [22]; (Hostfet et al., 1992) [7]; (Hasse et al., 1994) [6] have explored the concept of cascade coordination involving two or more SPDs connected on the same power supply but at some distance from each other.

The legitimate wish of the energy service providers to specify robust SPDs at the service entrance results in SPDs having a relatively high Maximum Continuous Operating Voltage (MCOV). On the other hand, some equipment manufacturers tend to select SPDs with a low MCOV under the misconception that lower is better (Martzloff and Leedy, 1989) [18]. This dichotomy can result in a situation where the low-MCOV SPDs included in equipment might well become involved in the "exit path" and thus become overstressed in the case of a direct flash to the building. This situation is made more complicated by the fact that commercial SPDs packages are assembled from typical distributors' supplies that can have an allowable tolerance band of $\pm 10\%$ on the voltage-limiting rating.

To explore the possibility and implications of a questionable coordination, numerical simulations were performed on a simplified model of a building featuring SPDs installed at the service entrance and SPDs that may be incorporated in equipment connected inside the building near the service entrance.

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II. NUMERICAL SIMULATIONS

II.1 Basic circuit

Figure 1 shows a simplified building power system that includes the key elements of this scenario: the building earthing system and all earthing electrodes, with the corresponding exit paths via the service-entrance SPDs and a built-in SPD provided at the power port of a typical item of electronic equipment. In this example, these SPDs are metal-oxide varistors (MOVs) with typical voltage ratings (150 V at the service entrance and 130 V in the equipment) selected for a 120/240 V residential power system. (The conclusions obtained for this type of power system will also be applicable to 240/400 V systems.)

Numerical analysis of the circuit behavior by EMTP [4] allows inclusion of the SPD characteristics as well as the significant R and L elements of the wiring, with injection of a stroke current of 100 kA 10/350 μ s at any selected point – the earthing system in this case. The selection of a 100 kA peak is consistent with the postulate made in many published simulations, but might be questioned on the basis of field experience and lightning detection statistics, as will be discussed later in this paper.

In Figure 1, the neutral is defined as part of a "multiple-grounded neutral" system (TN-C-S), with distributed R and L elements between its earthing electrode connections. The R and L values for the cables used in the numerical simulation, but not shown in the figure to avoid clutter, were selected to emulate the typical wire diameters used in low-voltage power distribution systems and building installations.

Previous studies (Birkel et al., 1996) [3]; (Mansoor and Martzloff, 1998) [15] have validated the intuitive expectation that the tail of the 10/350 μ s waveform often postulated for simulations will be shared among the available paths simply according to the relative values of resistance in the paths leading to the earthing electrodes. This fact is apparent in the results of Figure 2, for example at the 350 μ s time: when inductive effects have dwindled, the current I_H in the 10- Ω earthing resistance of the building is ten times smaller than the total current exiting the building [$I_N+I_{L1}+I_{L2}$] toward the power distribution system in which multiple earthing electrodes offer an effective earthing resistance of only 1 Ω . It is also worthy to note that this sharing is controlled by the *relative* values of the resistances, so that any earth conductivity differences associated with local conditions will wash out.

The combination of the service-entrance 150-V MOV on Line 2 and the 130-V MOV incorporated at the power port of the equipment constitutes a so-called "cascade". When two such cascaded SPDs are to be coordinated, a decoupling impedance must be provided between the two SPDs so that the voltage drop caused by the current flowing in the decoupling impedance – in this example the impedance of the 2,5 mm² diameter wires – and added to the limiting voltage of the 130-V MOV, will cause enough of the current to flow through the 150-V MOV to reduce stress on the 130-V MOV.

The simulation was performed for three values of the impedance (length) of the connection, i.e., 0,1 m, 1 m, and 10 m to assess the effect of this impedance for practical situations. Figure 3 shows the results for these three cases and Table 1 shows the resulting energy deposition in the respective MOVs.

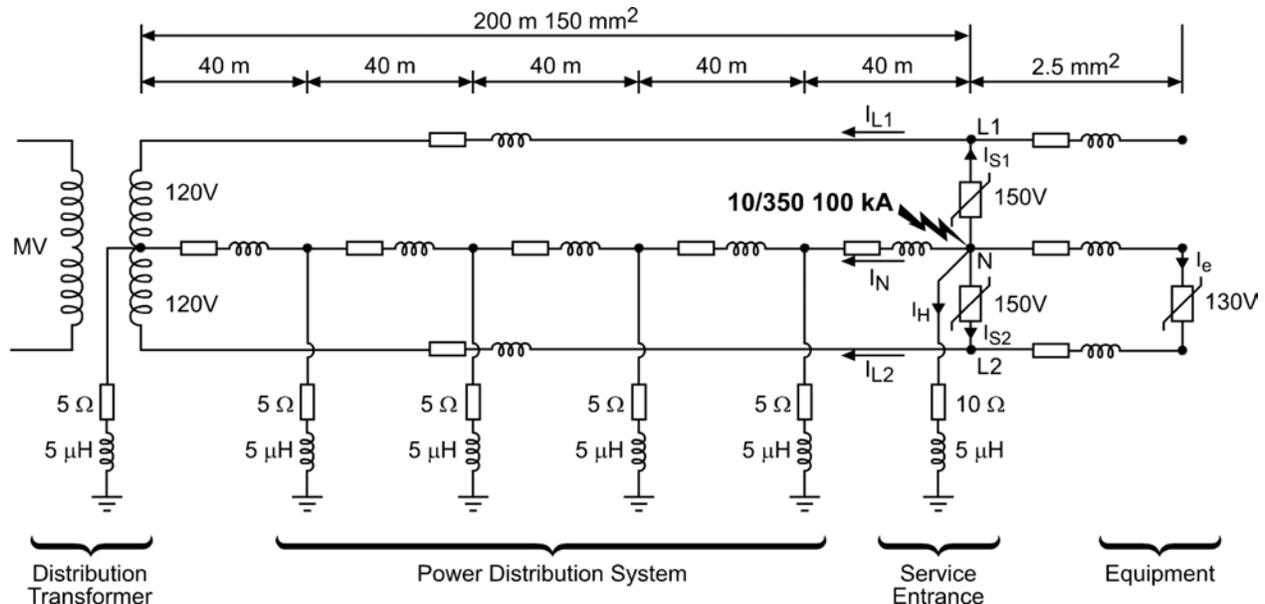
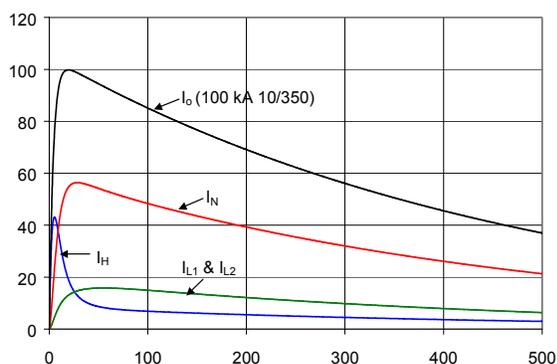


Figure 1 Simplified building schematic with service-entrance SPDs, one built-in equipment SPD, and multiple-grounded power distribution system in case of a direct lightning flash to the earthing system



Legend

I_o : 100 kA, 10/350 μ s stroke to the building earthing system
 I_N : current exiting via the neutral of the power supply
 I_{L1}, I_{L2} : current exiting via the two lines of the power supply
 I_H : current into the building earthing electrode(s)

Vertical scale: current in kA – Horizontal scale: time in μ s

Figure 2 – Sharing of the lightning current among available paths to earth electrodes

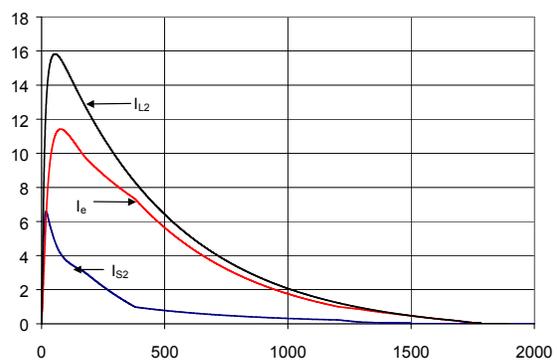
In the traces of Figure 3, the total current in Line 2 (sum of the two currents in the two MOVs) remains essentially unchanged for the three combinations, but the sharing of the current between the two MOVs is significantly affected.

Figure 3a, with only 0,1 m of separation, is not a practical example of connection of equipment that close to the service entrance – except perhaps an electronic residual current device incorporated in the service panel. The two other figures, 3b and 3c, show how the 130-V MOV that took the largest part of the current in the case of Figure 3a, now takes on less as separation length increases. An interesting situation develops as the current flowing in the 10-m line to the 130-V MOV stores energy that will cause a stretching of the current in the 130-V MOV long after the 150-V MOV current has decayed. This is significant because the total energy deposited in the MOVs is the criterion used for coordination, even though the current in the 130-V MOV could be lower than the current in the 150-V MOV. Table 1 shows how this energy sharing changes with the length of the decoupling connection, according to the integration of the varistor currents and voltages obtained from EMTP.

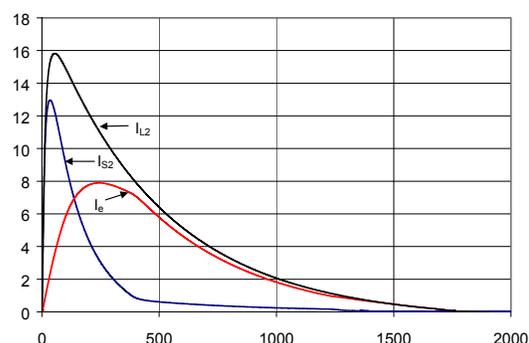
Table 1 – Sharing energy between MOVs for three different connection lengths

SPD	Energy deposition (joules)		
	0,1 m	1 m	10 m
150-V MOV	620	1090	2470
130-V MOV	2560	2030	890

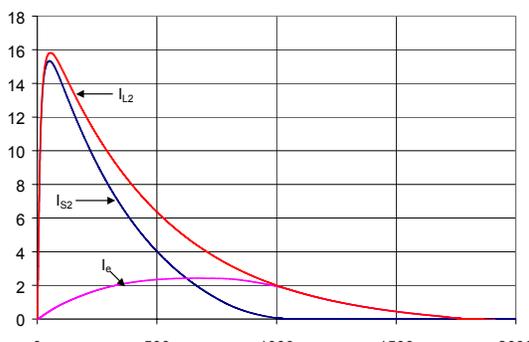
These energy levels might be acceptable for a 150-V MOV sized for service entrance duty, but the 890-joule deposition into the 130-V MOV incorporated in the equipment exceeds common-wisdom ratings for such



a) 0,1 m connection



b) 1 m connection



c) 10 m connection

Legend

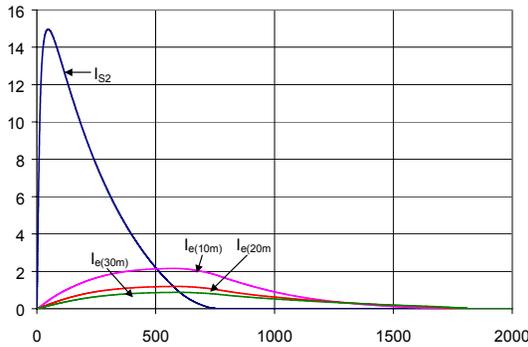
I_{L2} : current exiting via the power supply phase conductor
 I_{S2} : current into the service entrance SPD
 I_e : current into the equipment SPD

All vertical scales: current in kA
 All horizontal scales: time in μ s

Figure 3 – Sharing of lightning stroke current

devices. This finding then raises a question on the effectiveness of a cascade for the case of direct flash to the building. In an actual installation, there would be more than one piece of equipment, presumably each with a 130-V built-in MOV at the power port. One might expect that some sharing among these multiple SPDs would reduce the energy stress imposed on these devices.

To explore this situation, an additional simulation was performed for three branch circuits, respectively 10 m, 20 m, and 30 m, each of them supplying equipment incorporating a built-in 130-V MOV. Figure 4 shows the sharing of current among these three MOVs and the 150-V service entrance MOV, and Table 2 shows the energy deposition.



Legend

I_{s2} : current into the service entrance SPD
 I_e : currents in the three SPDs at end of 10, 20, and 30 m lines
 Vertical scale: current in kA – Horizontal scale: time in μ s

Figure 4 – Sharing of current among MOVs

Table 2 – Energy sharing among MOVs

Branch circuit length and energy deposition into three 130-V MOVs			Service entrance 150-V MOV
10 m	20 m	30 m	
620 J	370 J	280 J	1930 J

II.2 Effect of manufacturing tolerances on commercial-grade metal-oxide varistors

The simulations discussed so far were performed by postulating that both the 150-V MOV and the 130-V MOV had their measured voltage limiting at the nominal value as specified by typical manufacturer specifications. Such a postulate is of course difficult to ensure in the reality of commercial-grade devices. For instance, the nominal voltage-limiting value of MOVs rated 130 V rms is 200 V, with lower limit of 184 V and upper limit of 220 V. To check that aspect of the problem, an arbitrary lot of 300 devices rated 130 V rms was purchased from a distributor and the actual measured voltage-limiting value at 1 mA dc was determined in accordance with IEEE Std 62.33-1994 [11]. For this lot, the standard deviation (sigma) was found to be 8 V.

On the basis on these measurements and to give an indication of the significance of tolerance effects, the computations reported for Figure 3c (10 m separation) were repeated, still with a 150 V MOV at the service entrance, but with varistors at ± 1 sigma of the 130 V rms rating, that is, 122 V and 138 V rms. The results are shown in Table 3.

Table 3 Energy sharing for three values of the equipment built-in MOV (10 m separation)

Equipment MOV rating (V rms)	Energy deposited (J)	
	Equipment MOV	150-V service entrance MOV
122	915	2320
130	890	2890
138	750	2650

These results illustrate the significance of tolerances in a situation where the difference between the two SPDs of the cascade is not large, because of the de facto situation of low values of MCOV that the industry has unfortunately adopted. Of course, if tolerances were also taken into consideration for the service entrance MOV, the extremes of distributions for both MOV would make an effective coordination between a nominal 150-V MOV and a nominal 130-V MOV even more problematic.

II. 3 Nonlinearity of circuit elements

Most of the reported simulations, as cited above, have been performed with a conservative postulate of a 100 kA 10/350 lightning discharge. The median of the current peaks compiled in the seminal Berger et al. paper [2] is only 20 kA. Occasional reservations have been voiced on the validity of these data collected with technology dating back to the 1970's. A recent (July 2000) actual case history was communicated to the authors by a colleague for two major lightning storms recorded in the area of Tampa in Florida by means of the Lightning Detection System [24], during which over 30 000 flashes were detected in a period of less than 12 hours, with only one at the 150 kA level, and a median of 20 kA, confirming the Berger et al. data.

One could expect that the dispersion of the lightning current that results from the combined action of linear elements (resistance and inductance) with nonlinear components (MOVs) might produce a different sharing of the current as the decoupling element is linear but the SPDs are nonlinear. To explore this hypothesis, the computations for the case of Figure 4 and Table 2 were repeated, for peak currents of 100 kA (the original value of the computation), 50 kA, and 25 kA (about the median of the statistics). Table 4 shows the results of these computations. It is interesting to note that as the applied stroke is decreased 4 to 1 (from 100 to 25), the total energy deposited in the varistors is decreased by a factor of $3200/610 = 5.2$. This relative greater decrease is caused by the larger portion of the current exiting via the linear-path neutral, further relief for all the SPDs involved in the exit path.

Table 4 Nonlinear effects on current sharing

10/350 stroke (kA)	Branch circuit length and energy deposited into three 130-V MOVs			Energy into service entrance 150-MOV	Total energy in the MOVs
	10 m	20 m	30 m		
100	620 J	370 J	280 J	1930 J	3200 J
50	329 J	215 J	179 J	700 J	1423 J
25	170 J	120 J	90 J	230 J	610 J

III. DISCUSSION

We have made all these computations based on postulating that the insulation levels are sufficient to prevent a flashover that would drastically affect the continuing energy deposition in the downstream SPDs. We have not included the limits of energy handling of the devices, which of course should be compared with computed deposited energy levels in a practical case.

Another set of readings from the EMTP computations confirmed that the presence of SPDs at the critical points prevents such overvoltages from occurring (as long as the SPDs can carry the resulting currents)

Not surprisingly, the results of the simulation confirm that the sharing of the lightning current occurs in inverse ratio of the resistances leading to the earthing electrodes after the initial phase of the 10/350 μ s stroke. Likewise, one can expect that inductances will limit the current flow so that low-inductive paths, such as intended and opportunistic earth electrodes of the building itself, compared to the longer lines of the power supply, will carry a larger share of the total current during the initial phase of the current. This effect is clearly visible on the I_H of Figure 2, for the relatively slow rise time of 10 μ s of a first stroke. One may expect that for the subsequent strokes, or the flashes associated with triggered lightning experiments that have shorter rise times (Rakov et al., 2001) [21], this effect will be even more apparent.

An important finding – predictable on a qualitative basis but heretofore not quantified for the case of a direct lightning flash to buildings containing electronic equipment – concerns the cascade coordination of built-in SPDs in the equipment. From the simple examples presented, it appears that a cascade of a robust service-entrance SPD and a built-in SPD sized for limited energy-handling capability, according to the common-wisdom practice, might well be a delusion.

A solution to the difficult coordination could be to replace the all-MOV SPD at the service entrance with a combined series gap-varistor device (Mansoor et al., 1998) [16]. Such a device would also alleviate the concerns about the temporary overvoltage problems associated with MOV-only SPDs. Sparkover of the gap during the initial rise of the lightning current (when the coordination by means of the decoupling inductance occurs) will invite the remainder (continuing rise and tail) of the surge current exiting via SPDs to use the service entrance SPD rather than the simple and less robust built-in MOVs downstream.

Last but not least, the practical question remains open on the need to provide surge protection against worst cases – the combined worst case of a direct flash to the building and the high-level 100 kA stroke, which is only at the 4% probability, according to the Berger et al. data [2] and even lower in the yet-anecdotal case of the Tampa Bay lightning storm [24]. The nonlinearity effect presented in II.3 adds further credibility to the overall need to make reasonable risk assessments of cost-effectiveness before specifying high surge level requirements, both for the service entrance SPDs and for built-in SPDs in connected equipment.

IV. CONCLUSIONS

1. When accepting the postulate that the reference parameter of a direct lightning flash to a building should be a 10/350 μ s current with a peak of 100 kA, the numerical simulations performed for a simplified system with one surge-protective device installed at the service entrance, and one or more built-in SPD in downstream equipment indicate that the downstream SPD is very likely to be overstressed and fail, most likely catastrophically.

2. There are several possible explanations for the apparent contradiction between a prediction of downstream equipment failures based on this postulated lightning parameters, and equipment field experience that does not report such frequent failures, although of course anecdotes abound.

- The occurrence of a direct flash to a building can cause such extensive damage that a post-mortem for investigating the specifics of a prevailing ineffective coordination is not performed at that time and the issue is ignored.
- Enough uncontrolled clearance flashovers occur in the installation to provide significant relief for any at-risk SPDs incorporated in downstream equipment.
- In an installation where many built-in or plug-in SPDs are present, the sharing illustrated by Figure 4, combined with a low probability of a 100 kA stroke, might reduce the stress on downstream devices to a value within their capability. In particular, many commercial plug-in SPDs advertise capabilities of hundreds of joules, unlike the 20 joules of a single MOV, which might be provided at the input port of electronic equipment.
- Insufficient field failure data have been obtained, compiled, shared, and published to enable realistic assessment of frequency and severity of occurrences involving an unsuccessful cascade coordination.

3. It is impractical at this point to mandate high energy handling capability for built-in SPDs. Such a move might meet with strong objections from manufacturers whose products have satisfactory field experience, and a risk analysis might show it to be not cost-effective.

4. Economic and political realities related to the type and mission of the installations to be protected should be kept in mind. Clearly, mass-market applications such as cost-conscious consumers, in a framework of regulated or unregulated installations, are different from bottom-line-conscious industrial applications, and even more so in the case of national assets – be they cultural or military.

5. Another approach for manufacturers might be to avoid placing low MCOV varistors at the input port of their equipment. Rather, they should select an SPD with an MCOV and resulting surge-protective level as high as their equipment can inherently stand. This is a “selfish” approach which is mentioned here half-seriously, half-facetiously: there are enough low MCOV SPDs installed by users or included in other equipment in a typical system that those unfortunate low-MCOV devices will take up the stress, leaving unscathed the equipment wisely provided with high MCOV SPDs!

V. REFERENCES

Note: The citations that appear in the text are listed here by alphabetical order of the lead author, not by chronological order of appearance in the text.

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