IEEE Guide for Improving the Lightning Performance of Electric Power Overhead Distribution Lines

IEEE Power & Energy Society

Sponsored by the Transmission and Distribution Committee

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IEEE Guide for Improving the Lightning Performance of Electric Power Overhead Distribution Lines

Sponsor

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Abstract: Factors that contribute to lightning-caused faults on the line insulation of overhead distribution lines and suggested improvements to existing and new constructions are identified in this guide.

Keywords: direct-stroke protection, distribution, flashover, ground conductivity, IEEE 1410, induced over-voltage, insulation, lightning, overhead line, surge impedance
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Introduction

Lightning is a major cause of faults on typical overhead distribution lines. These faults may cause momentary or permanent interruptions on distribution circuits. Power-quality concerns have created more interest in lightning, and improved lightning protection of overhead distribution lines against faults is being considered as a way of reducing the number of momentary interruptions and voltage sags.

Lightning usually causes temporary faults on overhead distribution lines. If the fault is cleared by a breaker or a recloser, the circuit may be successfully reclosed. In the past, this was acceptable—but now with the proliferation of sensitive loads, momentary interruptions are a major concern.

Lightning may also cause permanent faults. Five to ten percent of lightning-caused faults are thought to cause permanent damage to equipment. Temporary faults may also cause permanent interruptions if the fault is cleared by a one-shot protective device, such as a fuse.

Estimates of the lightning performance of distribution lines contain many uncertainties. Some of the basics such as lightning intensity measured by ground flash density (GFD), or estimating the number of direct strokes to a distribution line may have significant errors. Often, rough estimates or generally accepted practices are just as effective as detailed calculations. This guide is intended to provide estimates of lightning-caused faults that are linked to physical variables such as the line height, the presence of parallel neutral or overhead groundwires (OHGW), the intervals between ground electrodes and/or surge protective devices, the proximity of the line to nearby objects and the characteristics of the soil.

Another goal of this guide is to provide revised estimates of lightning-caused faults showing the effectiveness of various improvement options. Estimates using this guide may be used to compare improved lightning protection with other methods of improving system reliability and power quality such as tree trimming programs or improved protection schemes such as the use of additional reclosers or sectionalizers. This guide should also be beneficial in evaluating design standards.

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Contents

1. Overview .................................................................................................................................................. 1
   1.1 Scope ................................................................................................................................................ 1
   1.2 Purpose ............................................................................................................................................... 2

2. Normative references ............................................................................................................................... 2

3. Definitions ............................................................................................................................................... 2

4. Lightning parameters ............................................................................................................................... 4
   4.1 Lightning incidence ........................................................................................................................... 4
   4.2 Electrical characteristics of lightning .............................................................................................. 9

5. Lightning performance of overhead distribution lines ............................................................................ 13
   5.1 Overvoltages from direct lightning flashes to unprotected phase conductors .................................. 13
   5.2 Overvoltages from lightning flashes to objects near the line ............................................................ 17
   5.3 Distribution line insulation level ...................................................................................................... 20
   5.4 CFO voltage of combined insulation .............................................................................................. 21
   5.5 Determining the CFO voltage of structures with series insulation .................................................. 22
   5.6 Practical considerations when increasing structure CFO ............................................................... 24
   5.7 Arc-quenching capability of wood .................................................................................................... 26
   5.8 Wood damage caused by lightning ................................................................................................. 26
   5.9 Limits to increased insulation strength for improved lightning performance ................................. 27

6. OHGW protection of distribution lines .................................................................................................. 27
   6.1 Shielding angle .................................................................................................................................. 28
   6.2 Insulation requirements ..................................................................................................................... 29
   6.3 Effect of grounding and insulation level ............................................................................................ 29
   6.4 Distribution underbuild ..................................................................................................................... 30
   6.5 Overhead groundwires and arresters ................................................................................................. 30

7. Arrester protection of distribution lines ................................................................................................. 31
   7.1 Arrester lead length considerations ................................................................................................. 31
   7.2 Flashovers from nearby strokes ....................................................................................................... 31
   7.3 Flashovers from direct strokes ........................................................................................................ 32

8. Burial protection of distribution lines ..................................................................................................... 34
   8.1 Direct stroke damage to buried cables ............................................................................................. 34
   8.2 Induced currents and voltages in buried cables .............................................................................. 35

Annex A (informative) Examples of guide usage ....................................................................................... 36
   A.1 Example 1—15 kV wooden crossarm design .................................................................................... 36
   A.2 Example 2—35 kV distribution line with an OHGW ...................................................................... 37

Annex B (informative) Technical modeling and assumptions ...................................................................... 40
   B.1 Shielding .......................................................................................................................................... 40
   B.2 Induced-voltage flashovers ............................................................................................................. 40
   B.3 Shield-wire modeling for direct lightning ....................................................................................... 50
   B.4 Arrester spacing ............................................................................................................................... 52

Annex C (informative) Bibliography .......................................................................................................... 57
1. Overview

This design guide contains information on methods to improve the lightning performance of overhead distribution lines, and is written for the distribution-line designer. This guide recognizes that a perfect line design does not exist, and that a series of compromises are made in any distribution-line design. While some parameters such as voltage, routing, and capacity may be predetermined, other decisions are made at the discretion of the designer. The designer may exercise control over structure material and geometry, shielding (if any), amount of insulation, grounding, and placement of arresters. This guide will help the distribution-line designer optimize the line design in light of cost-benefit considerations.

1.1 Scope

This guide will identify factors that contribute to lightning-caused faults on overhead distribution lines and suggest improvements to existing and new constructions.

This guide is limited to the protection of distribution-line insulation for system voltages 69 kV and below. Equipment protection considerations are covered in IEEE Std C62.22™-2009.¹

¹ Information on references can be found in Clause 2.
1.2 Purpose

The purpose of this guide is to present alternatives for reducing lightning-caused flashovers on overhead distribution lines.

2. Normative references

The following referenced documents are indispensable for the application of this document (i.e., they must be understood and used, so each referenced document is cited in text and its relationship to this document is explained). For dated references, only the edition cited applies. For undated references, the latest edition of the referenced document (including any amendments or corrigenda) applies.


3. Definitions

For the purposes of this document, the following terms and definitions apply. The IEEE Standards Dictionary: Glossary of Terms & Definitions should be consulted for terms not defined in this clause.2

3.1 back flashover (lightning): A flashover of insulation resulting from a lightning stroke to part of a network or electric installation that is normally at ground potential.

3.2 basic impulse insulation level (BIL) (rated impulse withstand voltage) (surge arresters): The crest value of a standard lightning impulse for which the insulation exhibits a 90% probability of withstand (or a 10% probability of failure) under specified conditions.

3.3 cloud-to-ground lightning flash: A lightning discharge to ground consisting of a first return stroke that may be followed by subsequent strokes and other impulsive or continuing currents.

3.4 critical impulse flashover voltage (CFO) (insulators): The crest value of the impulse wave that, under specified conditions, causes flashover through the surrounding medium on 50% of the applications.

3.5 direct flash: A lightning flash with one or more return strokes terminating directly to any part of a network or electric installation.

3.6 direct stroke: A lightning stroke directly to any part of a network or electric installation.

3.7 distribution line: Electric power lines which distribute power from a main source substation to consumers, usually at a voltage of 34.5 kV or less, but possibly at a phase-to-phase voltage of up to 69 kV.

3.8 flashover (general): A disruptive discharge through air around or over the surface of solid or liquid insulation, between parts of different potential or polarity, produced by the application of voltage wherein the breakdown path becomes sufficiently ionized to maintain an electrical arc.

3.9 ground electrode: A conductor or group of conductors in intimate contact with the ground for the purpose of providing a connection with the ground.

---

3.10 **ground flash density (GFD)** ($N_g$): The average number of lightning flashes per unit area per unit time at a particular location.

3.11 **guy insulator**: An insulating element, possibly with elongated form and transverse holes or slots, for the purpose of insulating two sections of a guy or provide insulation between structure and anchor and also to provide protection in case of broken wires.

3.12 **guy wire**: A stranded cable used for a semi-flexible tension support between a pole or structure and the anchor rod, or between structures.

3.13 **induced voltage (lightning strokes)**: The voltage induced on a network or electric installation by a nearby stroke.

3.14 **lightning first return stroke**: A lightning discharge to ground initiated when the tip of a downward stepped leader meets an upward leader from the earth.

3.15 **lightning flash**: The complete lightning discharge, most often composed of one or more leaders from a cloud followed by one or more return strokes.

3.16 **lightning subsequent stroke**: A lightning discharge that may follow a path already established by a first stroke.

3.17 **lightning outage**: A power outage following a lightning flashover that results in system fault current, thereby necessitating the operation of a switching device to clear the fault.

3.18 **line lightning performance**: The performance of a line expressed as the annual number of lightning flashovers on a circuit kilometer or tower-line kilometer basis.

3.19 **metal-oxide surge arrester (MOSA)**: A surge arrester utilizing valve elements fabricated from non-linear resistance metal-oxide materials.

3.20 **nearby stroke**: A lightning stroke that does not terminate directly on any part of a network but induces a significant overvoltage in it.

3.21 **overhead groundwire (OHGW)**: Grounded wire or wires placed above phase conductors for the purpose of intercepting direct strokes in order to protect the phase conductors from the direct strokes. They may be grounded directly or indirectly through short gaps. An OHGW also functions as a shield wire.

3.22 **shielding angle**: The angle between the vertical line through the overhead ground wire and a line connecting the overhead ground wire with the shielded conductor.

3.23 **shield wire**: Grounded wire(s) placed near the phase conductors for the purposes of: a) Reducing the incidence of direct lightning strokes to phase conductors, b) Reducing induced voltages from external electromagnetic fields, c) Lowering the self-surge impedance of an overhead groundwire (OHGW) system, or d) Raising the mutual surge impedance of an OHGW system to the protected phase conductors. A grounded neutral beneath a phase conductor functions as a shield wire (functions b,c,d) but not as an OHGW (function a).

3.24 **spark gap**: Any short-air space between two conductors electrically insulated from or remotely electrically connected to each other.

3.25 **surge arrester**: A protective device for limiting surge voltages on equipment by diverting surge current and returning the device to its original status. It is capable of repeating these functions as specified.

NOTE—The term arrester as used in this guide is understood to mean surge arrester.
4. Lightning parameters

4.1 Lightning incidence

Lightning occurs during rainstorms, snowstorms, and other natural phenomena. However, in most areas, rainstorms are the primary source of lightning. Storms produce intracloud, cloud-to-cloud, and cloud-to-ground lightning. Intracloud lightning is the most frequent, but cloud-to-ground lightning affects overhead distribution lines. During a storm, power interruptions are caused by wind and lightning. Interruptions caused by wind, trees, and damaged equipment are sometimes assumed to be caused by lightning, which will make the number of lightning-caused interruptions appear artificially high.

The reliability of a distribution line is dependent on its exposure to lightning. To determine exposure, the distribution-line designer needs to know the ground flash density (GFD), defined as the number of flashes per unit area per unit time. The preferred measure of GFD is \( N_g \), the number of cloud-to-ground flashes per square km per year. This GFD may be estimated in several ways, as follows.

4.1.1 Statistical considerations

Lightning and lightning-caused interruption rates have considerable year-to-year variation, see Darveniza [B37], MacGorman et al. [B69]. The historical standard deviation for yearly measurements of lightning activity ranges from 20% to 50% of the mean. Estimates of GFD for a small region such as 10 km \( \times \) 10 km have a larger standard deviation of about 30% to 50% from the mean. Larger regions such as 500 km \( \times \) 500 km have a smaller standard deviation of 20% to 25% from the mean. In areas with lower levels of lightning activity, the relative standard deviation is higher.

With such large standard deviations, it takes many years of data to estimate a mean value accurately. This is especially true when using ground-flash data for a localized region or estimating lightning-caused interruption rates on a distribution line from outage data.

Estimates of average GFD may also be obtained directly from lightning-detection network data or from lightning flash counters. If enough years of data are present, this has the advantage of identifying regional variations. A minimum grid size that provides at least 400 registrations in each cell over the selected observation period shall be used to allow meaningful comparison of adjacent areas.

4.1.2 Ground flash density from thunder data

The ground flash density \( N_g \) for temperate areas may be estimated from \( T_d \), the keraunic level, using Equation (1) from Anderson et al. [B6]:

\[
N_g = 0.04 T_d^{1.25}
\]  

(1)

where

- \( N_g \) is the ground flash density in flashes per km\(^2\) per year
- \( T_d \) is the number of days with thunder per year

Torres et al. [B29] noted that this expression has unacceptably large errors in tropical areas, recommending the alternative expressions for Equation (1):
Another estimate of GFD may be obtained from thunderstorm hour records (MacGorman et al [B69]), as shown by Equation (2):

\[ N_g = 0.054 T_h^{1.11} \tag{2} \]

where

- \( N_g \) is the ground flash density in flashes per km\(^2\) per year
- \( T_h \) is the number of thunderstorm hours per year

With the uncertainty in the choice of appropriate expression, the poor statistical quality of observations in regions with limited lightning (a region with \( T_d = 5 \) will require 80 years of observations to meet the criterion of 400 observations) and the ready availability of better alternatives, the use of thunder data to predict distribution line lightning performance should be discontinued.

### 4.1.3 Ground flash density from lightning optical transient density

In most areas of the world, an indication of lightning activity may be obtained from observations of lightning optical transients, see Christian et al. [B24]. Satellite-based sensors respond to all types of lightning with relatively uniform coverage in all areas. With sufficient averaging, optical transient density data in Figure 1 provide better estimates of ground flash density than thunder observations, which have a wide range of relations between ground flash density and thunderstorm-hours or days. There are also regional variations in the ratio of ground flashes to total flashes, see Boccippio et al. [B14], but a median value of 0.33 ground flashes to total flashes is recommended for both tropical and temperate regions.

For areas without ground-based lightning location systems or lightning flash counters, the recommended estimate of ground flash density is:

\[ N_g = N_f / 3 \tag{3} \]

where

- \( N_g \) is the ground flash density in flashes per km\(^2\) per year
- \( N_f \) is the total (cloud + ground) density of optical flashes per km\(^2\) per year obtained from Figure 1a through Figure 1c.
Figure 1 a—Total (cloud + ground) lightning activity ($N_o$, optical flashes per km$^2$ year$^{-1}$) for Asia and Australia, adapted from Christian et al. [B24]
Figure 1b—Total (cloud + ground) lightning activity ($N_t$, optical flashes per km² year⁻¹) for North and South America, adapted from Christian et al. [B24]
Figure 1c—Total (cloud + ground) lightning activity ($N_t$, optical flashes per km$^2$ year$^{-1}$) for Africa and Eurasia, adapted from Christian et al. [B24]

4.1.4 Ground flash density from lightning location networks

A more detailed depiction of lightning activity may be obtained from lightning ground flash density (GFD) maps, which are created from information obtained via present-day lightning-detection networks or lightning flash counter networks that have been operated in the past. A sample GFD map of the United States from a lightning location network is shown in Figure 2.
Lightning location networks and lightning flash counter arrays have been deployed in North America and other parts of the world. With enough experience, these networks provide detailed GFD maps with much greater detail and accuracy than has been available with thunder or optical transient data. Location systems also provide measured quantities that are more useful and detailed than keraunic data. In addition to providing the GFD, lightning location networks may also provide the date, time, location, number of strokes, polarity, peak radiated electromagnetic field and the related estimate of stroke peak current.

In many areas of the world, these systems have accumulated sufficient data to satisfy design purposes in grid areas as fine as 20 km × 20 km. GFD maps are currently being used for distribution-line design, estimating lightning-caused flashovers, and for many other types of lightning analysis.

4.2 Electrical characteristics of lightning

4.2.1 Waveshape parameters

The impressed surge current is considered to be a current source. Direct flashes to unprotected conductors cause overvoltages that have the same waveshape as the stroke current. Figure 3 describes the typical concave lightning current waveshape with the use of the listed parameters.

Figure 2 — GFD map of contiguous USA (reprinted with permission of Vaisala) [B78]
<table>
<thead>
<tr>
<th>Parameter in Figure 3</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>$I_{10}$</td>
<td>10% intercept along the stroke current waveshape</td>
</tr>
<tr>
<td>$I_{30}$</td>
<td>30% intercept along the stroke current waveshape</td>
</tr>
<tr>
<td>$I_{90}$</td>
<td>90% intercept along the stroke current waveshape</td>
</tr>
<tr>
<td>$I_{100}$ = $I_t$</td>
<td>Initial peak of current</td>
</tr>
<tr>
<td>$I_F$</td>
<td>Final peak of current</td>
</tr>
<tr>
<td>$T_{10/90}$</td>
<td>time between $I_{10}$ and $I_{90}$ intercepts on the wavefront</td>
</tr>
<tr>
<td>$T_{30/90}$</td>
<td>time between $I_{30}$ and $I_{90}$ intercepts on the wavefront</td>
</tr>
<tr>
<td>$S_{10}$</td>
<td>instantaneous rate-of-rise of current at $I_{10}$</td>
</tr>
<tr>
<td>$S_{30/90}$</td>
<td>average steepness (through $I_{30}$ and $I_{90}$ intercepts)</td>
</tr>
<tr>
<td>$S_{90/90}$</td>
<td>average steepness (through $I_{30}$ and $I_{90}$ intercepts)</td>
</tr>
<tr>
<td>$S_m$</td>
<td>Maximum rate-of-rise of current along wavefront, typically at $I_{90}$</td>
</tr>
<tr>
<td>$t_{d10/90}$</td>
<td>Equivalent linear wavefront duration derived from $I_F / S_{10/90}$</td>
</tr>
<tr>
<td>$t_{d30/90}$</td>
<td>Equivalent linear wavefront duration derived from $I_F / S_{30/90}$</td>
</tr>
<tr>
<td>$t_m$</td>
<td>Equivalent linear waveform duration derived from $I_F / S_m$</td>
</tr>
<tr>
<td>$Q_I$</td>
<td>impulse charge (time integral of current) in stroke current waveshape</td>
</tr>
</tbody>
</table>

**Figure 3—Description of lightning current waveform parameters [B27]**

From a simplified circuit viewpoint, overvoltages from nearby lightning are inductively coupled to the current waveform. This means that the peak overvoltage magnitude is related to the maximum steepness $S_m$ and the duration of the overvoltage is related to $T_{10/90}$.

**4.2.2 Log-normal statistical distribution**

From the comprehensive summary presented by CIGRE Working Group 33.01 [B27] and supplemented by transmission line observations in Japan by Takami and Okabe [B110], log-normal distributions of lightning parameters are assumed. The general equation for the log-normal probability density function for any particular parameter $x$ is given by Equation (4):
\[ f(x) = \frac{1}{\beta \cdot \sqrt{2 \cdot \pi}} \cdot \exp \left( -\frac{z^2}{2} \right) \]
\[ z = \frac{\ln(x / M)}{\beta} \]

where

\( f(x) \) is the probability density
\( M \) is the median value of \( x \)
\( \beta \) is the logarithmic standard deviation (to base \( e \))

### 4.2.3 Parameters of negative downward strokes

Lightning flashes consist of a first stroke and may have one or more subsequent strokes, following the same path and terminating at the same location on the line. First strokes have higher peak currents and subsequent strokes have faster rate of current rise as shown in Table 1.

The values of \( M \) and \( \beta \) in Equation (4) for the most relevant lightning parameters used in calculation of distribution line outage rates are reported in Table 1, based on CIGRÉ Working Group 33.01 [B27].

**Table 1 — Recommended lightning current parameters (CIGRÉ Working Group 33.01 [B27])**

<table>
<thead>
<tr>
<th>Parameters of log-normal distribution for negative downward flashes</th>
<th>First stroke</th>
<th>Subsequent stroke</th>
</tr>
</thead>
<tbody>
<tr>
<td>Parameter</td>
<td>( M, ) Median</td>
<td>( \beta, ) logarithmic standard deviation</td>
</tr>
<tr>
<td><strong>FRONT TIME (( \mu \text{s} ))</strong></td>
<td></td>
<td></td>
</tr>
<tr>
<td>( t_{d10/90} = T_{10/90} / 0.8 )</td>
<td>5.63</td>
<td>0.576</td>
</tr>
<tr>
<td>( t_{d30/90} = T_{30/90} / 0.6 )</td>
<td>3.83</td>
<td>0.553</td>
</tr>
<tr>
<td>( t_m = I_F / S_m )</td>
<td>1.28</td>
<td>0.611</td>
</tr>
<tr>
<td><strong>STEEPNESS (kA/( \mu \text{s} ))</strong></td>
<td></td>
<td></td>
</tr>
<tr>
<td>( S_m, ) Maximum</td>
<td>24.3</td>
<td>0.599</td>
</tr>
<tr>
<td>( S_{10}, ) at 10%</td>
<td>2.6</td>
<td>0.921</td>
</tr>
<tr>
<td>( S_{10/90}, ) 10-90%</td>
<td>5.0</td>
<td>0.645</td>
</tr>
<tr>
<td>( S_{30/90}, ) 30-90%</td>
<td>7.2</td>
<td>0.622</td>
</tr>
<tr>
<td><strong>CREST CURRENT (kA)</strong></td>
<td></td>
<td></td>
</tr>
<tr>
<td>( I_i, ) initial</td>
<td>27.7</td>
<td>0.461</td>
</tr>
<tr>
<td>( I_F, ) final</td>
<td>31.1</td>
<td>0.484</td>
</tr>
<tr>
<td>Ratio, ( I_i/I_F )</td>
<td>0.9</td>
<td>0.230</td>
</tr>
<tr>
<td><strong>OTHER RELEVANT PARAMETERS</strong></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Tail Time to Half Value ( t_n ) (( \mu \text{s} ))</td>
<td>77.5</td>
<td>0.577</td>
</tr>
<tr>
<td>Number of strokes per flash</td>
<td>1</td>
<td>0</td>
</tr>
<tr>
<td>Stroke Charge, ( Q_i ) (Coulomb)</td>
<td>4.65</td>
<td>0.882</td>
</tr>
<tr>
<td>( \int P , dt ) (kA s)</td>
<td>0.057</td>
<td>1.373</td>
</tr>
<tr>
<td>Inter-stroke interval (ms)</td>
<td>—</td>
<td>—</td>
</tr>
</tbody>
</table>
For the sake of handling the probabilistic distribution in a simple way, the following expression is adopted (Anderson [B4]) for the probability of a first-stroke current peak value $I_o$ that exceeds a value $i_o$:

$$P \left( I_o \geq i_o \right) = \frac{1}{1 + \left( \frac{i_o}{31} \right)^{1.8}} \quad (5)$$

where

- $P(I_o \geq i_o)$ is the probability that the first return stroke has a peak current $I_o$ that exceeds $i_o$
- $i_o$ is the prospective first return stroke peak current (kA)

When the median first return stroke peak current of 31.1 kA from Table 1 is used for $i_o$ in Equation (5), the probability that the lightning-peak current $I_o$ exceeds the median is about 0.5. Equation (5) applies to values of $I_o$ lower than 200 kA. This is currently under review by Borghetti et al. [B17] and [B20]. Recent lightning detection network measurements in North America indicate the possibility of lower median current values, see Cummins et al. [B35], which may be the result of misclassification of subsequent strokes as first strokes in remote measurement systems. The distribution of subsequent-stroke peak current values is approximated by (IEEE Std 1243-1997):

$$P \left( I_o \geq i_o \right) = \frac{1}{1 + \left( \frac{i_o}{12} \right)^{1.7}} \quad (6)$$

where

- $P(I_o \geq i_o)$ is the probability that a subsequent return stroke has a peak current $I_o$ that exceeds $i_o$
- $i_o$ is the prospective subsequent return stroke peak current (kA)

When the median subsequent return stroke peak current of 12.3 kA from Table 1 is used for $i_o$ in Equation (6), the probability that the subsequent-stroke peak current $I_o$ that exceeds the median is about 0.5.

Generally, shielding failures are caused by flashes with low first return-stroke currents with corresponding small striking distance [IEEE Std 1243-1997]. The probability of first-stroke peak current that is less $i_o$ is $(1 - P(I_o \geq i_o))$, using Equation (5). These weak first-stroke currents are likely to be followed by one or more subsequent strokes with higher peak current, based on Equation (6). This means that almost all shielding failures result in insulation flashovers, no matter how low the peak current of the first stroke or how small the striking distance.

### 4.2.4 Amplitude and frequency dependence

There is a high correlation between the maximum steepness $S_m$ and the peak current $I_F$ in Table 1, meaning that the same waveshape in Figure 3 may be used even though the peak amplitude of current may vary over a factor of 100:1 from 2 kA to 200 kA. This correlation may be exploited by using the equivalent linear front time of 1.28 $\mu$s for first strokes and 0.31 $\mu$s for subsequent strokes in evaluation of distribution line lightning performance. An equivalent linear front time of 2.0 $\mu$s, used in IEEE Std 1243-1997 is appropriate for the large-amplitude first stroke events that tend to cause backflashovers on transmission lines with higher levels of insulation strength and overhead groundwires.
While the lightning impulse has broad frequency content, many calculation methods for electromagnetic coupling rely on frequency-domain analysis. In cases where a single frequency is used for estimates, it shall be obtained by the sine wave that gives the same peak amplitude $I_f$ and maximum derivative $S_m$. For first strokes with median 31.1 kA and 24.3 kA/$\mu$s, this frequency is 124 kHz. For subsequent strokes with median 12.3 kA and 39.9 kA/$\mu$s, the frequency is 516 kHz.

5. Lightning performance of overhead distribution lines

Lightning may account for many power interruptions in distribution lines. Lightning may cause flashovers from:

a) Direct flashes to the line

b) Induced voltages from nearby flashes

Clause 5 describes how to estimate the number of direct and induced flashovers for distribution circuits.

5.1 Overvoltages from direct lightning flashes to unprotected phase conductors

Direct lightning flashes to unprotected power distribution phase conductors cause insulation flashovers in the great majority of the cases. For example, a stroke of as little as 10 kA would produce an overvoltage of around 2000 kV, far in excess of the insulation levels of overhead distribution lines operating up to 69 kV.

5.1.1 Lightning incidence and structure height

Lightning may have a significant effect on a line’s reliability, especially if the poles are higher than the surrounding terrain. More flashes are collected by taller structures. The flash collection rate $N$, in open ground (no significant trees or building nearby), is estimated by Equation (7), see Eriksson [B49]:

$$
N = N_g \left(28h^b + b \right) / 10
$$

(7)

where

$N$ is the flash collection rate (flashes/100 km/yr)

$N_g$ is the ground flash density from Equation (3) or Figure 2 (flashes per km$^2$ per year)

$h$ is the height of the uppermost conductor at the pole (m)

$b$ is the structure width (m)

For most distribution lines, the structure width factor $b$ is negligible ($b \approx 0$) compared to the attractive width.

From Equation (7), if the pole height is increased by 20%, the flash rate to the overhead distribution line would increase by 12%. Also, expressing the attractive radius of the line as a protective angle to level ground, this angle ranges from 77 to 82°.
5.1.2 Lightning interception from nearby structures and trees

The exposure of the distribution line to lightning depends on how much adjacent structures protrude above the surrounding terrain. Structures located along the top of mountains, ridges, or hills will be more likely targets for lightning flashes than those shielded by natural features. Trees and buildings may also play a major role in the lightning performance of distribution lines in level terrain. Trees and buildings may intercept many lightning flashes which otherwise would have struck a line.

The influence of nearby objects on the number of direct flashes to a distribution line is expressed using a shielding factor, $S_f$ is defined as the per-unit portion of the distribution line shielded by nearby objects. The number of strikes to the line is then:

$$N_x = N(1 - S_f)$$

where

- $N_x$ is the number of flashes collected by the sheltered line (flashes/100 km/yr)
- $N$ is the flash collection rate to the line in open terrain from Equation (7) (flashes/100 km/yr)
- $S_f$ is the environmental shielding factor, ranging from 0 to 1

A shielding factor of $S_f=0.0$ means the distribution line is in open terrain with no shielding provided by nearby objects. A factor of $S_f=1.0$ means the distribution line is completely shielded from direct flashes. As explained in 5.2, however, this does not imply that the line is protected from all lightning effects.

Figure 4 gives a means for approximating the shielding factors for objects of various heights for a 10 m tall distribution line. The objects are assumed to be in a uniform row parallel to the distribution line and located on one side of it. This could represent a continuous row of trees or buildings paralleling the distribution line.

![Figure 4 — Shielding factors $S_f$ due to nearby objects of different heights for a 10 m tall distribution line](image-url)
Figure 4 may also be used for objects on both sides of the distribution line if the shielding factors for the left and right sides are summed (if the sum of the shielding factors is greater than one, then the total shielding factor is equal to one). As an example, consider a 10 m tall overhead distribution line with the following rows of buildings on each side:

a) A 7.5 m tall row of buildings, 30 m from the left side of the distribution line \( (S_{f_{\text{left}}} = 0.23) \)

b) A 15 m tall row of trees, 40 m from the right side of the distribution line \( (S_{f_{\text{right}}} = 0.4) \)

If the GFD is 1 flash/km²/yr, the number of direct flashes to the overhead distribution line in open ground would be 11.15 flashes/100 km/yr from Equation (7). With the rows of buildings and trees, the number of direct flashes would reduce to, as shown in Equation (9):

\[
N_S = N\left[1 - (S_{f_{\text{left}}} + S_{f_{\text{right}}})\right] \\
= (11.15 \text{ flashes/100 km/yr})\left[1 - (0.23 + 0.4)\right] \\
= 4.12 \text{ flashes/100 km/yr}
\]

where

- \( N_S \) is the number of flashes collected by the sheltered line (flashes/100 km/yr)
- \( N \) is the flash collection rate to the line in open terrain from Equation (7) (flashes/100 km/yr)
- \( S_{f_{\text{left}}} \) and \( S_{f_{\text{right}}} \) are the environmental shielding factors from Figure 4 on each side of the line

### 5.1.3 Lightning flashovers from direct strokes

#### 5.1.3.1 Flashover rate from direct flashes on unprotected phases

Both corona and imperfect soil effects play relatively minor roles in the surge impedance of the phase conductor, leading to changes of less than 30% in this value. Unless the distribution-line insulation is protected with an OHGW or arresters, more than 99% of all direct lightning flashes will cause distribution line flashovers regardless of insulation level, conductor spacing, or grounding. Therefore, to estimate the number of flashovers due to direct lightning flashes use Equation (7) for a distribution line in open ground, or Equation (8) and Equation (9) for a partially-shielded line.

#### 5.1.3.2 Low-frequency, low current surge impedance of phase conductor

The surge impedance of a single wire over ground, fed from one end, is calculated from:

\[
Z_0 = 60 \ln\left(\frac{2000 \cdot h}{r}\right)
\]

where

- \( Z_0 \) is the surge impedance (Ω)
- \( h \) is the height of the conductor over ground (m)
- \( r \) is the radius of the conductor (mm)
The surge impedance of a conductor fed from the middle is half of this value, and the lightning current will split equally into each path over ground. An unprotected phase conductor of radius $r=10$ mm and height $h=10$ m over perfect ground has an impedance of $456 \, \Omega$ from one end or $228 \, \Omega$ in the normal case for a direct stroke terminating somewhere along the line. This low-current, high-frequency value over perfectly conducting ground is altered under high-current lightning surge conditions with fast rates of rise over imperfect soil of finite conductivity.

5.1.3.3 Effects of corona at high voltage

Using Equation (5) for a first stroke peak current with $i_o=4.4$ kA gives the probability that this current will be exceeded as 99%. Using Equation (10) with $h=10$ m and $r=10$ mm, the surge impedance $Z_o$ is $456 \, \Omega$. Assuming the current travels in two directions away from the source, the surge impedance is $Z_o/2$ or $228 \, \Omega$. The product of the impressed first-stroke current $i_o$ and the phase conductor surge impedance suggests a peak voltage that exceeds 1 MV about 99% of the time. With this magnitude of voltage on the conductor, its effective radius will increase from corona effects, thereby increasing the capacitance of the conductor and lowering its surge impedance. IEEE Std 1243-1997 provides a method for evaluating this effect. If the radius of the corona envelope envelops adjacent conductors, these will have nearly the same voltage as the stricken phase from common-mode coupling and the differential-mode stress on phase-to-phase insulation will be further reduced. However, if the corona envelope radius exceeds the phase-to-phase or phase-to-neutral spacing, there is a greater chance of midspan flashovers on distribution lines. Midspan flashovers on distribution lines have been observed with automatic cameras and also documented using locations where covered conductors have melted after lightning flashover faults.

5.1.3.4 Effects of imperfect soil at high frequency

The surge impedance of the conductor over “lossy” ground of finite conductivity will increase with decreasing soil conductivity and with decreasing frequency. For greatest accuracy, the height $h$ in Equation (10) should be a complex number, see Gary [B51] and Darveniza [B43], based on both soil conductivity and frequency. The impedance over wide range of frequencies should be evaluated and an inverse Fourier transform should be performed to obtain a time-domain result. However, the effect of finite soil conductivity on surge impedance can be modeled with acceptable accuracy in lightning calculations by replacing the real height of the line $h$ in Equation (10) with a real value of effective height, see Darveniza [B43], given by:

$$h_{\text{eff}} = h + \frac{4.7}{\sqrt{\sigma}}$$

(11)

where

- $h_{\text{eff}}$ is an effective height to be used for calculating $Z_o$ in Equation (10) (m)
- $h$ is the height of the conductor over ground (m)
- $\sigma$ is the conductivity of the uniform, lossy ground beneath the conductor (mS/m)

For a realistic result, the earth conductivity at the main frequency of interest, 124 kHz for the first stroke, should include the frequency-dependent changes in both soil conductivity and permittivity. These effects tend to limit the conductivity at 124 kHz to a minimum of $\sigma \geq 1$ mS/m, even for areas of rock with extremely low conductivity.
5.1.3.5 Fault rate from direct flashes with arc quenching on unprotected phases

At least three forms of arc quenching are practical on distribution lines: series wood in the insulation path, active arcing horns consisting of a series gap and metal oxide resistor, and long-flashover path provided by covered conductor creeping discharge or other means. If none of the arc quenching methods are used, a flashover from a direct flash will cause a fault on the distribution circuit.

5.2 Overvoltages from lightning flashes to objects near the line

Experience and observations show that many of the lightning-related outages of low-insulation lines are due to lightning that flashes to the ground or to structures in proximity of the line. Most voltages induced on a distribution line by flashes that terminate near a line are less than 300 kV. The induced voltages tend to have short pulse width compared to the time to half value of a typical stroke. They tend to be unipolar especially for flashes beside the line. For lossy ground, the induced voltage waveshape depends on the position along the line the polarity may invert from one end of the line to the other.

The distribution system engineer can perform detailed evaluations of overvoltages from the induced lightning, which requires considerable experience and technical skill. Some distribution systems can be easily fitted with simple countermeasures, such as consistently high impulse strength at every structure to withstand the most severe induced overvoltage for the local soil conditions. Where an overvoltage level of 300 kV CFO is considered sufficient for lines in areas of high soil conductivity, an insulation level of 420 kV CFO may be more appropriate for areas of low soil conductivity with $\sigma = 1$ mS/m (millimho/meter).

The accurate calculation of induced voltages requires the availability of adequate models for the electromagnetic coupling between the lightning electromagnetic pulse (LEMP) that “illuminates” without directly terminating on the conductors of a multi-conductor line situated, in general, above lossy ground.

One of the simplest analytical formulas for calculating induced overvoltage peak magnitude was set out by Rusck [B100]. This analysis is restricted to a simple configuration, namely an infinitely long single-conductor overhead line above an ideal ground, excited by a LEMP generated by a step lightning current waveshape with propagation along a channel represented using a transmission line model, moving at a slow velocity $v$ relative to the speed of light $c$. The Rusck simplified formula serves as a calibration benchmark in numerical methods, but delivers misleading results in many cases. It predicts that the maximum overvoltage is:

$$U_m = 30 \left( 1 + \frac{v}{c} \frac{1}{\sqrt{2} - \left( \frac{v}{c} \right)^2} \left( \frac{h \cdot I_p}{d} \right) \right)$$

(12)

where

- $U_m$ is the maximum overvoltage at the location nearest the ground flash (kV)
- $v$ is the speed of propagation of the return stroke (m/s), typically $c/3$
- $c$ is the velocity of light, $3 \times 10^8$ m/s
- $I_p$ is the peak stroke current (kA)
- $h$ is the height of the conductor over ground (m)
- $d$ is the lateral distance from the horizontal line to the vertical lightning stroke ground termination (m)

One limitation in the Rusck simplified formula, related to imperfect ground effects, can be resolved efficiently with reasonable accuracy by artificially increasing the apparent height of the phase conductors over ground, using Equation (11), see Darveniza [B43].
Flashes may be collected by tall objects, and the height and distance of trees, buildings, light standards and other structures from the distribution line will influence its lightning performance. For tall structures, Equation (12) fails because $v=c$ in conductors and the Rusck model should not be used for this calculation. Baba and Rakov [B8] applies numerical methods for this problem and suggests that lightning flashes to tall objects such as wind turbines, located within 100 m of distribution lines, may induce 50% to 80% higher overvoltage than flashes to ground at the same location.

More elaborate models, see Nucci et al. [B79],[B80], [B81], and [B82], now allow for a sufficiently accurate treatment of realistic line configurations. Moreover, the presence of distribution transformers with the relevant surge protection devices, as well as the presence of surge arresters, grounded neutrals and other OHGWs along the line, should also be taken into account in order to predict the overall line response to illumination of electromagnetic fields from nearby lightning.

Induced-voltage calculations should be carried out in the following way:

- A prospective flash location, first and subsequent stroke peak magnitudes are established, for example using a Monte Carlo method in combination with the attractive radius expression in Equation (7).
- The lightning current where it contacts the ground at channel base, is assumed, for example using the waveshape in Figure 3 or a ramp with equivalent front time $t_m$ from Table 1.
- A lightning return-stroke model, which specifies the spatial-temporal distribution of the current along the channel is selected, as in Nucci et al. [B79]. Normally, the radiated field from this current dominates electrostatic or induction fields for the short times of interest in calculating induced overvoltage, see Uman et al. [B111].
- The electromagnetic field associated with the current is calculated everywhere along the line, taking into account if needed the effect of the soil conductivity, see Cooray [B34] and Rubinstein [B99]. Normally, the electric field is resolved into its vertical and horizontal components, but other combinations of field components are equivalent since they are all related by Maxwell’s equations.
- The computed electromagnetic field is used to calculate the induced transients using a field-to-transmission line coupling model based on the source terms, as in Agrawal et al. [B1].
- The response of the power system components to the induced transients is calculated using an EMTP-based modeling program that includes the possibility of computing line responses to external excitation electromagnetic fields.
- The calculation is repeated many times with different flash locations and peak stroke magnitudes to simulate a large number of years of service.
- The calculation results are normalized to the local ground flash density.

As for many other applications in power systems practice, these models call for an implementation into computer codes since, in general, they require a numerical integration of the relevant equations. Appendix B includes the recommended field-to-transmission line coupling model proposed by Agrawal et al. [B1] extended to the problem of interest, distribution line lightning performance, by Nucci and Rachidi [B83].

All models can be used to infer the lightning performance curves showing the number of flashover/100km/yr versus the CFO of the distribution line, and relevant statistical procedures for this are also described in Appendix B. In this section, however, reference is made only to the results obtained using the more general modeling approach described above. The LEMP calculation uses the simple and relatively accurate “Transmission Line” return-stroke model of Uman et al. [B111]. The coupling between the LEMP and the line conductors is calculated using the model by Agrawal et al. [B1].

Figure 5 presents the frequency of flashover as a function of the critical flashover (CFO) voltage of a 10 m high, infinitely long line consisting of a single conductor above a conducting ground. The values are normalized for a GFD of $N_g=1$ flash/km$^2$/yr and may be scaled linearly with respect to GFD.
Figure 5 — Number of induced-voltage flashovers versus distribution-line insulation level, from Borghetti et al. [B22]

NOTE—In Figure 5 ideal ground has infinite conductivity (zero resistivity); conductivity of 10 mS/m is equivalent to resistivity of 100 Ωm; 1 mS/m is equivalent to 1000 Ωm.

The Monte Carlo method used to obtain the results of Figure 5 is described in Appendix B. The parameter in Figure 5 shows three values of ground conductivity \( \sigma \), namely infinite (ideal ground), 10 mS/m and 1 mS/m. When evaluating lightning induced voltages, see Rachidi et al. [B91] and CIGRÉ C4.401 [B30] the finite value of the ground conductivity on the one hand increases the transient propagation losses in the line but, on the other hand, has also an influence on the LEMP propagation. While the former effect tends to decrease the surges propagating along the line, the latter tends—in general—to enhance the amplitude of the induced voltages. It is this second effect that, overall, produces amplitudes of induced voltages larger than those obtained for the case of an ideal ground, see Ishii et al. [B65], Rachidi et al. [B91] and CIGRÉ C4.401 [B30].

As a point of reference, a 10 m tall distribution line in open ground with GFD = 1 flash/km²/yr will have approximately 11 direct flashes/100 km/yr, using Equation (7), each resulting in a flashover. In open ground, induced voltages will be a problem for lines characterized by low insulation levels and/or above a poor conducting ground. For example, for the case of an overhead line above a perfectly conducting ground, the number of induced-voltage flashovers will exceed the number of direct-stroke flashovers for an un-grounded circuit only if the CFO is less than 75 kV (from Figure 5). However, if the ground conductivity is poor (e.g., \( \sigma = 1 \) mS/m), the CFO for which the number of induced-voltage flashovers will exceed the number of direct-stroke flashovers is less than 140 kV (from Figure 5).

The results shown in Figure 5 are for a distribution line in open ground with no nearby trees or buildings. The number of induced flashovers depends also on the presence of nearby objects which may shield the line from direct flashes. This may increase the induced-voltage flashovers because there are more nearby strokes. Therefore, in shielded areas, induced-voltage flashovers are more of a concern.

More modeling details relevant to the results shown in Figure 5 are reported in the Annex B.
A grounded neutral wire or OHGW will reduce the voltage across the insulation by a factor that depends on the spacing between adjacent groundings, on the grounding impedance and on the proximity of the grounded conductor to the phase conductors. For this analysis, a shielding factor formula has been obtained from a formula found in Rusck [B100] by assuming the grounded neutral or shielding wire as a non-illuminated conductor and with continuous grounding connections. This factor is typically between 0.6 and 0.9. The adoption of such a shielding factor gives quite accurate results only for short spacing values between two adjacent groundings, e.g. 30 m and for ideal ground conductivity. For a more accurate calculation, the effect of the presence of a grounded conductor should be dealt with by considering it in the same way as the other conductors of the multi-wire lines, as in Rachidi et al. [B91], and by taking into account the actual spacing between adjacent groundings, as in Paolone et al. [B84]. The distance between adjacent ground terminations, rather than the value of resistance achieved at each pole, is the parameter that has the largest influence on the protective efficiency because the pole resistances are typically much less than the surge impedance of the neutral or overhead groundwire.

Note that grounded circuits, i.e., circuits with a grounded neutral wire or OHGW, are generally expected to have fewer flashovers for a given CFO because the grounded conductor reduces the voltage stress across the insulation via its electromagnetic shielding effect. However, the presence of the grounded conductor results in two possible flashover paths, namely a) from phase-to-ground path and b) from phase-to-grounded wire. This second path is characterized in general by a lower CFO value, which means that, eventually, the line could experience an overall CFO reduction. This may be adequately appraised by means of available computer tools, see Yokoyama [B114] and Borghetti et al. [B22].

The proximity of surge arresters can mitigate the effects of lightning-induced voltages. The results of Borghetti et al. [B22] and Paolone et al. [B84] show that a significant improvement of the lightning performance of the considered distribution line can be obtained by reducing the spacing between the surge arresters below few hundreds of meters. Again, for a precise evaluation of the need to protect every phase with a surge arrester and of the optimal arresters spacing, the use of available software is needed along with the knowledge of the non-linear characteristics of the protection devices.

Clearly, a refined evaluation of the indirect lightning performance should take into account the real topology and geometry of distribution networks, which are in general constituted by short lines terminated to power system components (e.g., loads and transformers), in general protected by surge arresters. To this purpose, adequate EMTP-based computational tools are available, see Nucci and Rachidi [B83].

In common with IEEE Std 1243-1997 and most published literature, the reported results have been expressed in terms of “flashover/100 km/year.” This measure is meaningful for transmission systems characterized by the presence of long line sections with uniform construction. This measure may be less helpful for distribution systems having irregular features, such as a mix of three-phase feeders with single-phase laterals with different framing. It is suggested that results of distribution system lightning outage rates be presented by making reference to the system of specific interest, namely to its topology and configuration (i.e., number and location of surge arresters, presence of shielding wires, etc.), and simply providing the results in terms of number of flashover per year.

### 5.3 Distribution line insulation level

This design guide is an attempt to assist the distribution system design engineer to optimize the lightning insulation capabilities of overhead distribution lines. Most overhead construction utilizes more than one type of insulating material for lightning protection.

The more common insulating components used in overhead distribution line construction are porcelain, air, wood, polymer, and fiberglass. Each element has its own insulation strength. When the insulating materials are used in series, the resulting insulation level is not the summation of those levels associated with the individual components, but is somewhat less than that value.
The following factors affect the lightning flashover levels of distribution lines and make it difficult to estimate the total insulation level:

a) Atmospheric conditions including air density, humidity, rainfall, and atmospheric contamination
b) Polarity and the rate of rise of the voltage
c) Physical factors such as insulator shape, shape of metal hardware, and insulator configuration (mounted vertically, horizontally, or at some angle)

The effect of wood in the lightning discharge path on the insulation strength may be quite variable. Improvement depends primarily upon surface moisture content and to a lesser degree on the physical dimensions of the wood and the ratio of wood path to dry arc distance.

Even though the design engineer may be more familiar with the basic impulse insulation level (BIL) of a given combination of insulating materials, the results of this guide are given in terms of the CFO of these combinations. The CFO is defined as the voltage level at which statistically there is a 50% chance of flashover and a 50% chance of withstand. This strength value is defined in laboratory tests, and tends to have a narrow Gaussian distribution, compared to the wide log-normal distributions of stress based on Table 1. If a Gaussian distribution of flashover strength is assumed, then any specific probability of withstand may be statistically calculated from the CFO value and the standard deviation.

As the laboratory data became available, various methods were studied in an attempt to develop a procedure for use in determining the expected CFO of a given combination of insulating components. The “insulation-strength-added” approach may be the most practical.

This method was adopted from a similar procedure used earlier in transmission line design but has been expanded in its application to multiple insulating components used in distribution-line construction. It utilizes the CFO of the basic or primary insulation element and adds to that value the increase in CFO offered by an added component (keeping in mind that the added insulation strength is always less than that of the single added element).

5.4 CFO voltage of combined insulation

From the earliest times, electrical engineers have been constructing distribution lines using wooden crossarms and poles in series with basic insulators to increase the lightning impulse strength of the distribution line insulation. In the early 1930s, a number of papers presented the results obtained when insulators were tested in combination with wood. A question arose as to how much lightning impulse strength the wood added to the primary insulation of the porcelain insulator. A partial answer came through research in many laboratories, and some results were published in the 1940s and 1950s, see Clayton and Shankle [B31]. A general summary of previous works on CFO was presented in the 1950 AIEE Committee Report [B2] and an extended report [B3] in 1956. These results were developed mostly for transmission lines, but the high impulse insulation levels are now relevant to distribution-line construction as a countermeasure for induced over-voltage flashovers.

More recently, research continued on multi-dielectric combinations used in electrical power systems. These investigations were concerned with distribution and transmission lines and the withstand level of the wood when subjected to lightning, switching, and steep-front impulses, see Darveniza et al. [B36], Guerrieri et al. [B56], Jacob et al. [B66] and [B67], Pigini et al. [B89], Ross and Grzybowski [B97], Shwendi [B106], Shwendi and El-Kieb [B107]. Polymer insulators and fiberglass crossarms have been introduced to distribution lines starting in the 1970s, see Cherney et al. [B25], Elrod and Menzel [B47], Grzybowski and Jacob [B57] and Shwendi [B106] and Shwendi and El-Kieb [B107]. From the point of view of lightning performance, polymer insulators tend to deliver the same external impulse flashover strength as ceramic insulators of the same dimensions.
5.5 Determining the CFO voltage of structures with series insulation

Studies have indicated that 1 m of wood or fiberglass adds approximately 330 kV to 500 kV to the lightning impulse strength of the total insulation, see Grzybowski et al. [B57], [B58], and [B59]. For longer lengths, the impulse insulation strength of the wooden or fiberglass crossarm and insulator combination is determined mainly by the wooden or fiberglass crossarm alone. The nominal ac voltage insulation is obtained by the insulator alone, and the wooden or fiberglass crossarm is considered only as additional insulation for lightning overvoltage.

When the lightning surge path to the ground does not include a wooden or fiberglass crossarm but involves two or more types of insulators in series, the CFO of the combination is not obtained by merely adding the individual CFOs of the components. The CFOs of these combined insulations are controlled by a number of different factors, each of which requires individual analysis. Today, there are many different combinations and configurations in use by the operating companies.

The extended CFO-added method may be used to estimate the total CFO of a distribution structure by:

a) Determining the contribution of each additional insulation component to the total CFO of the combination

b) Estimating the total CFO of the combination knowing the CFO of the insulation components

This may be done using either tables or curves that display the experimental data available, and utilizing these data to relate the effect of one insulating material added to another. This procedure relies on the CFO characteristic data of the basic insulation and an additional set of composite data given as the CFO voltage added by a specific component.

In configurations where two components are involved, the CFO of the combination is much lower than the sum of the individual CFOs. The insulator is considered the primary or basic insulation. The CFO obtained for configurations consisting of two components is calculated as the CFO of the basic component plus the added CFO of the second component.

The total calculated CFO voltage for two components is:

\[ CFO_T = CFO_{ins} + CFO_{add,sec} \]  \hspace{1cm} (13)

For three or more components:

\[ CFO_T = CFO_{ins} + CFO_{add,sec} + CFO_{add,third} + \ldots + CFO_{add,nth} \]  \hspace{1cm} (14)

where

- \( CFO_T \) is the critical flashover voltage of the multiple forms of insulation in series
- \( CFO_{ins} \) is the critical flashover voltage of the primary insulation
- \( CFO_{add,sec} \) is the additional CFO added by the second component
- \( CFO_{add,third} \) is the additional CFO added by the third component
- \( CFO_{add,nth} \) is the additional CFO added by the \( n^{th} \) component
### Table 2—Critical Flashover Voltage of Primary and CFO-Added Components

<table>
<thead>
<tr>
<th>Description</th>
<th>Type</th>
<th>$CFO_{ins}$ (kV)</th>
<th>Description</th>
<th>$CFO_{add,sec}$ (kV/m)</th>
<th>Description</th>
<th>$CFO_{add,third}$ (kV/m)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Polymer Insulator</td>
<td>15 kV to 35 kV</td>
<td></td>
<td>Wood pole</td>
<td>210</td>
<td>Fiberglass pole</td>
<td>410</td>
</tr>
<tr>
<td>Ceramic Pin-Type Insulator</td>
<td>ANSI 55-4</td>
<td>105</td>
<td>Wood pole</td>
<td>235</td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>ANSI 55-5</td>
<td>120</td>
<td>Wood crossarm</td>
<td>250</td>
<td>Fiberglass pole</td>
<td>400</td>
</tr>
<tr>
<td></td>
<td>ANSI 55-6</td>
<td>140</td>
<td>Fiberglass crossarm</td>
<td>250</td>
<td>Fiberglass standoff</td>
<td>315</td>
</tr>
<tr>
<td>Vertical Ceramic Insulator String</td>
<td>1x102mm</td>
<td>75</td>
<td>Wood pole</td>
<td>90</td>
<td>Fiberglass standoff</td>
<td>315</td>
</tr>
<tr>
<td></td>
<td>2x102mm</td>
<td>165</td>
<td>Wood crossarm</td>
<td>160</td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>3x102 mm (4&quot;)</td>
<td>250</td>
<td>Fiberglass crossarm</td>
<td>250</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Horizontal Ceramic Insulator String</td>
<td>1x102mm</td>
<td>75</td>
<td>Wood pole</td>
<td>90</td>
<td>Fiberglass standoff</td>
<td>295</td>
</tr>
<tr>
<td></td>
<td>2x102mm</td>
<td>165</td>
<td>Wood crossarm</td>
<td>295</td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>3x102 mm (4&quot;)</td>
<td>250</td>
<td>Fiberglass crossarm</td>
<td>250</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Description</td>
<td>Type</td>
<td>$CFO_{ins}$ (kV/m)</td>
<td>Description</td>
<td>$CFO_{add,sec}$ (kV/m)</td>
<td>Description</td>
<td>$CFO_{add,third}$ (kV/m)</td>
</tr>
<tr>
<td>Wood</td>
<td>Pole</td>
<td>330</td>
<td>Wood Pole</td>
<td>65</td>
<td>Fiberglass Standoff:</td>
<td>200</td>
</tr>
<tr>
<td></td>
<td>Crossarm</td>
<td>360</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Fiberglass</td>
<td>Pole</td>
<td>470</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>Standoff</td>
<td>500</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Air</td>
<td></td>
<td>600</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

**NOTE 1**—All values are CFO levels obtained in standard wet tests.

**NOTE 2**—Values are the minimum of the negative and positive polarity values.

**NOTE 3**—Insulators are shown as examples only. Refer to manufacturer’s data for more exact values.

The values given in Table 2 refer to wet conditions which are recommended for estimating CFO. For CFO values under dry conditions obtained from manufacturers or from laboratory impulse tests, multiply the dry CFO values by 0.8±0.1 to obtain an estimate the CFO for wet conditions. For components not given in Table 2, the total CFO may be estimated by reductions for the second and third components as:

\[
CFO_{\text{add,sec}} = 0.45 \times CFO_{\text{ins}} \\
CFO_{\text{add,third}} = 0.2 \times CFO_{\text{ins}}
\]

where
- $CFO_{\text{add,sec}}$ is the additional CFO added by the second component
- $CFO_{\text{add,third}}$ is the additional CFO added by the third component
- $CFO_{\text{ins}}$ is the critical flashover voltage of the primary insulation
Use of the extended CFO-added method and the tables given in this guide will usually give answers within a ±20% error. Variation in pole humidity with outdoor exposure, and to a lesser extent the chemical treatment, affect the resistivity of wood and also influence the wood pole and crossarm insulation strength. Estimates that are more accurate can be obtained with the following methods:

a) Perform laboratory impulse tests of the structure in question under wet conditions. This method will give the most accurate results.

b) Perform impulse tests of the combined under dry conditions, and multiply the CFO value by 0.8 to estimate the wet condition CFO.

c) Use more detailed component CFOs given in Jacob et al. [B66] and [B67] or Shwehdi [B106].

d) Refer to other test results of distribution structures, as in Armstrong et al. [B7] or Darveniza et al. [B37] and [B38].

5.6 Practical considerations when increasing structure CFO

Equipment and support hardware on distribution structures may severely reduce CFO below the 300 kV level recommended for mitigating most induced overvoltage flashovers. The reduction in CFO at “weak-link” structures may greatly increase flashovers from induced voltages as shown in Figure 5. Several situations are described below.

5.6.1 Guy wires

Guy wires may be a major factor in reducing a structure’s CFO. For mechanical advantage, guy wires are generally attached high on the pole in the general vicinity of the principal insulating elements. Because guy wires provide an electrical path to the ground, their presence will generally reduce the configuration’s CFO. The small porcelain guy-strain insulators that are often used provide very little in the way of extra insulation (generally less than 30 kV of the CFO).

A fiberglass-strain insulator in series with a guy wire may be used to gain considerable insulation strength. A 50 cm fiberglass-strain insulator has a CFO of approximately 250 kV.

5.6.2 Fuse cut-outs

The mounting of fuse cutouts is a prime example of unprotected equipment which may lower a pole’s CFO. For 15 kV class systems, a fuse cut-out may have a 95 kV BIL. Depending on how the cutout is mounted, it may reduce the CFO of the entire structure to approximately 95 kV (approximately, because the BIL of any insulating system at a low probability of failure is always less than the CFO of that system at a 50% probability of failure).

On wooden poles, the problem of fuse cut-outs may usually be improved by arranging the cutout so that the attachment bracket is mounted on the pole away from any grounded conductors (guy wires, ground wires, and neutral wires). This is also a concern for switches and other pieces of equipment not protected by arresters.

5.6.3 Height of neutral wire

On any given line, the neutral wire height may vary depending on equipment connected. On wooden poles, the closer the neutral wire is to the phase wires, the lower the CFO.
5.6.4 Electrically conducting structural materials

The use of concrete and steel structures on overhead distribution lines greatly reduces the CFO compared to unbonded wood or fiberglass structures. Metal crossarms and metal hardware are also being used on wooden pole structures. If such hardware is grounded, the effect may be the same as that of an all-metal structure. On such structures, the total CFO is supplied by the insulator, and higher CFO insulators should be used to compensate for the loss of wooden insulation. Obviously, trade-offs should be made between lightning performance and other considerations such as mechanical design or economics. It is important to realize that trade-offs exist. The designer should be aware of the negative effects that metal hardware and electrically conducting poles may have on lightning performance and attempt to minimize those effects. On wooden pole and crossarm designs, wooden or fiberglass brackets may be used to maintain good insulation levels.

5.6.5 Spark gaps and insulator bonding

Bonding of insulator hardware is sometimes done to minimize the risk of lightning-caused damage to wooden poles or crossarms, or it is done to prevent pole-top fires. Spark gaps are also used to minimize the risk of lightning damage to wooden material. The use of spark gaps was a practice suggested by Rural Utility Service distribution specifications in the USA in 1983 but it is no longer a suggested practice, see USDA [B112]. In some parts of the world, spark gaps are used instead of, or in series combination with, metal oxide arresters for equipment protection, and the silicon carbide arresters remaining in service also have series gaps.

Spark gaps and insulator bonds will greatly reduce a structure’s CFO. If possible, spark gaps, insulator bonds, and pole-protection assemblies should not be used to prevent wood damage. Better solutions for damage to wood and pole fires are local insulator-wood bonds at the base of the insulator as discussed in 5.8.

5.6.6 Multiple circuits on same pole

Multiple circuits on a pole often cause reduced insulation. Tighter phase clearances and less wood length in series usually reduce insulation levels. This is especially true for distribution circuits built underneath transmission circuits on wooden poles. Transmission circuits will often have an OHGW with a ground lead at each pole. The ground lead may cause reduced insulation. This may be improved by moving the ground lead away from the pole with fiberglass spacers.

5.6.7 Spacer-cable circuits

Spacer-cable circuits are overhead-distribution circuits with very close spacing among conductors. Covered wire and spacers (15 cm – 40 cm) hung from a messenger wire provide support and insulating capability. A spacer-cable configuration will have a fixed CFO, generally in the range of 150 kV to 200 kV. Because of its relatively low insulation level, its lightning performance may be lower than a more traditional open design, see Powell et al. [B90]. There is little that may be done to increase the CFO of a spacer-cable design.

A spacer-cable design has the advantage of a messenger wire which tends to act as an OHGW. This may reduce some direct-stroke flashovers. Back flashovers will likely occur because of the low insulation level. Improved grounding can improve lightning performance if the soil conductivity is high and the separation between grounds is less than 30 m.

Spacer-cable circuits are usually installed in areas where tree contacts are a reliability issue. They can also be installed under existing open-wire circuits to save space. In either case, the spacer cable is generally protected from direct lightning flashes. Also, induced flashover rate is reduced because similar voltage is induced on each of the closely-spaced conductors when illuminated by electromagnetic (EM) fields from nearby lightning.
5.7 Arc-quenching capability of wood

Wood poles and crossarms have shown the capability to quench the lightning-caused arc, raising the ac arc reignition voltage to levels that minimize the risk of power-frequency faults, see Armstrong et al. [B7] and Darveniza et al. [B37] and [B38].

The arc-quenching capabilities of wood are predominantly a function of the instantaneous power-frequency voltage across the arc at the instant of the lightning-caused flashover. If the voltage is near a zero crossing, the arc is much more likely to extinguish without causing a fault. If the nominal voltage along the wooden crossarm is maintained below a certain level, the chance of a fault developing may be greatly reduced.

If multiple flashovers occur, arc quenching is much less likely (see Figure 6). Most distribution lines will suffer multiple flashovers from each direct stroke. On distribution structures that have RMS voltage gradients across wood greater than 10 kV/m of wood, arc quenching may not provide significant benefit. For example, a 13.2 kV distribution line with 0.5 m of wood between the phase insulator and the neutral wire has an RMS voltage gradient across the wood of 13.2 kV/ (√3 × 0.5 m) = 15.2 kV/m. For this voltage, if wood spacing of 1 m is achieved among all phase conductors and all grounded objects on the pole, then arc quenching may become a significant factor. This may be readily achieved on circuits with high insulation levels and long distances of wood. For this guide, a conservative assumption is made that all flashovers cause faults.

![Figure 6](image_url)

**Figure 6**—Probability of a power arc due to a lightning flashover over a wet wooden crossarm, from Darveniza et al. [B38]

5.8 Wood damage caused by lightning

Service experience indicates that damage to poles or crossarms due to lightning is relatively rare, see Darveniza [B37]. Nevertheless, in high-lightning areas it may be a concern under certain conditions. The probability of damage due to lightning depends on many factors, especially the moisture content and aging of the wood. Damage and shattering occurs when the breakdown is internal to the wood rather than along the surface of the wood. If the wood is green, it is more likely to breakdown internally.
If historical records show that wood damage is a problem, the wood may be protected by bonding the insulators. However, this approach short circuits the insulation capability provided by the wood. A better solution may be to use surface electrodes fitted near the insulator pin. This may include wire-wraps, bands, or other metal extensions attached near the insulator in the likely direction of flashover. This encourages breakdown near the surface rather than internally.

Preventative measures for lightning damage to wood will also reduce the likelihood of pole-top fires, which are the result of leakage-current arcs at metal-to-wood interfaces, see Darveniza [B37] and Ross [B97]. Local bonding, using wire bands or wraps, will bridge the location where fires are most likely to start at poor metal-to-wood contacts. This is preferable to completely bonding the insulators.

5.9 Limits to increased insulation strength for improved lightning performance

Clause 5 has shown that overhead lines can be protected from induced stroke overvoltages with the proper use of CFO levels of 300 kV or more, depending on local soil conductivity and line height, and that it can be practical to achieve this level on every distribution structure by taking advantage of series wood or fiberglass insulation strength. The more difficult question treated in Clause 6 and Clause 7 is that of protecting overhead lines from direct strokes. Direct stroke protection with overhead groundwires (OHGW) is effective on transmission lines because the product of stroke current and local tower resistance is less than the insulator CFO in the majority of cases. With reduced CFO of lines with system voltage less than 69 kV, OHGW are less effective or ineffective. Instead, the basic philosophies for protecting overhead distribution lines from direct lightning strokes consider that:

— The use of overhead ground (shield) wires reduces the risk of direct strokes to phase conductors, but increases the risk of back flashovers. OHGW may reduce the overall risk of lightning outages if soil conductivity is high. The desired value of ground resistance is dependent on line CFO, and generally each pole resistance has to be less than CFO (kV)/(15 kA) to be about 25% effective against backflashovers from direct strokes. It is generally not practical to achieve such low values of pole resistance in most areas.

— The use of arresters on all phases of a three-phase system and applied on the same pole can reduce the rate of flashover from direct strokes if the arresters are properly selected. Spacing of the arresters should be no more than one or two pole spans due the fast rise-times of direct lightning strokes. Flashover failure rates on the order of 20% should be expected for direct flashes, even with the protective margins associated with heavy duty arresters.

— The combined use of arresters and overhead groundwires provides complementary protection of distribution lines. The overhead groundwires divert most of the lightning energy away from phase conductors and connected equipment, and the arresters limit peak insulator voltages and reduce back flashover rates more effectively than improved grounding at every pole. While this hybrid design is considered effective, flashovers will still occur but to a much lesser degree.

A final factor to be considered is that of the effective use of limited capital resources. Generally, utilities may derive better improvements in customer service, measured by the momentary average interruption frequency index (MAIFI), by improving lightning performance on the circuits that affect the largest number of customers. The best value tends to be in eliminating weak-link structures on central distribution feeders in the higher voltage classes that have a naturally high CFO, compared to re-insulating single phase lateral lines that serve only a few customers.

6. OHGW protection of distribution lines

OHGW are grounded conductors placed above the phase conductors to intercept lightning strokes that would otherwise terminate on the phases. Lightning current is diverted to ground through pole ground leads near the flash termination. Effectiveness of OHGW requires that they be grounded at every pole.
Lightning-surge current flowing through the pole ground impedance causes a potential rise, resulting in a large voltage difference between the ground lead and the phase conductors. The voltage difference may cause a back flashover across the insulation from the ground lead to one of the phase conductors.

The back flashover phenomenon is a substantial constraint to OHGW effectiveness in distribution-line applications. OHGW may provide effective protection only if:

a) Good insulation design practices are used to provide sufficient CFO between the ground downlead and the phase conductors

b) Low pole ground resistances are obtained

Figure 5 may be used to estimate the number of induced flashovers for an OHGW design. For three-wire distribution circuits, adding an OHGW will reduce the number of induced flashovers. Since the OHGW is grounded, it will suppress the voltages on the phase conductors through surge impedance coupling. The closer the phase wires are to the OHGW, the better the coupling, and the smaller the induced voltages will be (although this may reduce the CFO, as discussed in 6.3). Note that adding a grounded wire below the phase conductors will have approximately the same mutual coupling effect as an OHGW above the phases.

On a four-wire, multigrounded system, replacing the underbuilt neutral wire with an OHGW will not significantly reduce the number of induced flashovers because there will be little change in the mutual surge impedance that establishes the voltage coupling coefficient. However, having both an OHGW and a neutral wire will improve performance to some degree because the coupling coefficient will be higher.

The cost of including an OHGW in a distribution-line design may be substantial. In addition to the cost of the conductor, pole grounds, and additional insulation, the pole height must be greater to support the OHGW such that there is a sufficient shielding angle between the OHGW and the outer phase conductors. The greater structure height attracts more direct flashes, and this slightly offsets some of the flashover rate reduction provided by the shielding. Despite the cost and design difficulties, OHGW have been used on distribution lines by some utilities with great success.

6.1 Shielding angle

A shielding angle (as shown by Figure 7) of 45° or less is recommended so that most lightning flashes will terminate on the OHGW rather than on the phase conductors. This guideline is only valid for lines less than 15 m tall with conductor spacing less than 2 m. Taller lines require smaller shielding angles.

For more information on shielding with overhead groundwires, refer to IEEE Std 1243-1997 and its references. Most of the shielding angle curves are drawn for transmission circuits, starting with a critical current of 5 kA to cause a shielding failure flashover. It must be recognized that critical currents for distribution circuits would be lower, in the range of 2 kA to 3 kA accepted as the minimum lightning stroke current. This would act to reduce the required shielding angle. Lightning detection network measurements in North America indicate the possibility of lower median current values, see Cummins et al. [B35]. This would also reduce the required shielding angle for a target shielding failure flashover rate. The electrogeometric models that form the basis of shielding angle recommendations are also under continuous review.

In areas where existing distribution lines with a 45° shielding angle perform well, this practice may continue. A smaller shielding angle of 30° should be used for new construction or design standards and for improved power quality on existing lines.
6.2 Insulation requirements

OHGW effectiveness in distribution lines depends greatly on the insulation provided between the ground lead and the phase conductors. If the ground lead is in contact with the pole for its entire height, it is difficult to provide adequate insulation. On a wooden pole, it is usually necessary to isolate the ground lead from the pole in the vicinity of the phase insulators and crossarms. This may be accomplished with fiberglass rods, or standoffs mounted horizontally on the pole to hold the ground wire 30 cm – 60 cm away from the pole as shown in Figure A.2. The CFO from the ground lead to the closest phase is the most limiting value from several paths. Care should also be taken to insulate guy wires to obtain the necessary CFO.

A CFO in excess of 250 kV to 300 kV is necessary for the effectiveness of OHGW applications. By using ground lead standoffs, it is not difficult to achieve this insulation level on distribution lines.

6.3 Effect of grounding and insulation level

OHGW effectiveness is highly dependent on grounding. For effectiveness of an OHGW design, ground resistances must be less than 10 Ω if the CFO is less than 200 kV. If attention is given to insulation level and the CFO is 300 kV–350 kV, a ground resistance of 30 Ω will provide similar performance. The OHGW should be grounded at every pole. Figure 8 shows the direct-stroke performance and effect of grounding with an example computer simulation of an OHGW with CFOs of 175 kV and 350 kV. Triggered-lightning studies confirming computed behavior of grounding electrodes under actual lightning surge conditions are presented in Rakov et al. [B93].
6.4 Distribution underbuild

Distribution lines underbuilt on transmission structures may be especially susceptible to back flashovers. Greater structure heights and larger right-of-ways will draw more direct flashes to the structures. Care must be taken to maintain high insulation levels to avoid unnaturally high flashover rates.

In addition, the voltage stress developed to cause a back flashover is higher on the distribution circuit than on the transmission circuit. This occurs because the distribution conductors are further from the OHGW, and therefore, have a lower coupled voltage and a higher voltage across the insulation compared to any of the transmission conductors. The insulation strength on the distribution underbuild is also usually less than on the transmission circuit. The distribution conductors will back flashover first, and will then help the transmission circuit's performance by increased coupling to those conductors.

Care must be taken to maintain low ground resistance and high insulation levels to avoid unnaturally high flashover rates on the distribution circuits. Line arresters on every pole should also be considered for underbuilt circuits. These arresters can help even if installed on just one phase, by increasing the coupled voltage on the other phases.

6.5 Overhead groundwires and arresters

To eliminate a large fraction of possible flashovers, arresters on every pole and every phase may be used in conjunction with an OHGW. The arresters will protect the insulation from backflashover. The OHGW will divert most of the current to the ground, so the arresters are not subject to much energy input. The arresters make the OHGW design less dependent on insulation level and grounding. One limit to this approach is the increasing occurrence of midspan flashover when the phase-to-OHGW separation is less than the corona radius of the surge current on the OHGW. The design engineer must determine the minimum separation distance, using the calculation of corona radius in IEEE Std 1243-1997.
7. Arrester protection of distribution lines

Distribution arresters provide overvoltage protection for equipment insulation such as transformers and regulators. These arresters function as high impedances at normal operating voltages and become low impedances during lightning surge conditions. The arrester conducts surge current to the ground while limiting the voltage on the equipment to the sum of the discharge voltage of the arrester plus the inductive voltage developed by the discharge current in arrester line and ground leads.

Arresters may be used to protect distribution line insulation by reducing the occurrence of flashovers and circuit interruptions. Several different types of arresters, such as internally gapped silicon carbide, internally, externally or non-gapped metal-oxide, have been used over time. From the point of view of protection of distribution-line insulation, all perform in a similar manner. Differences in discharge voltage characteristics will cause only a small difference in the protection of insulation, since there is considerable margin. Several studies have investigated the effectiveness of different arrester spacing, see Paolone et al. [B84], [B85], and Short et al. [B104]. Triggered-lightning studies of the performance of arresters on distribution lines are presented in De la Rosa et al. [B46], Fernandez et al. [B53], [B54], Master et al. [B72], and Mata et al. [B73] and [B74]. Generally, no arrester can survive a direct flash on its own but charge sharing among parallel arresters is efficient, see Mata et al. [B73] and [B74], and this sharing can limit energy dissipation to reasonable levels.

For selection of arrester rating, refer to IEEE Std C62.22-2009 or the manufacturer’s guidelines. For equipment protection (especially underground cables), it is sometimes necessary to select an arrester with the lowest possible protective level. However, for line-insulation protection, this is not usually necessary because the arrester protective level is generally considerably lower than the line-insulation level.

When applying arresters for protection, the failure rate of the added arresters should be considered along with the line flashover improvement obtained by adding the arresters.

7.1 Arrester lead length considerations

Arrester leads that connect the distribution line and ground terminals of arresters to the equipment they protect contain a small amount of inherent inductance. This inductance causes L(di/dt) voltage drops to appear across the leads that conduct lightning surge currents. Any voltage drop across an arrester lead will add to the arrester discharge voltage. This will increase the voltage appearing across the device(s) protected by the arrester.

The effect of the line-lead length on the protection of the distribution-line insulation is not as significant as it is with equipment protection. For overhead equipment, the margin is generally very high. Also, line insulation level is generally much larger than standard equipment BIL. Of course, it is always good practice to keep arrester distribution line and ground leads as short and straight as possible. Refer to IEEE Std C62.22-2009 or more information on arrester lead lengths.

7.2 Flashovers from nearby strokes

Arresters may greatly reduce the flashover rate due to induced voltages from nearby strokes. Figure 9 shows results for an insulation CFO of 150 kV for an un-grounded circuit. Note that even relatively wide arrester spacing may reduce induced-voltage flashovers significantly (8 spans yields at least a 25% reduction). On many distribution circuits with frequent transformers, the arresters used to protect the transformers may provide significant protection from induced flashovers. The technical assumptions are described in Annex B.
Arresters may be even more effective at reducing induced flashovers if they are used to protect poles with poor insulation levels. These “weak links” may include cut-outs, dead-end poles, or crossover poles. Placing arresters on these poles may be more cost-effective than improving the insulation level.

### 7.3 Flashovers from direct strokes

Protecting against direct flashes is difficult because of the high surge currents, steep rates of rise, and large energy content in each lightning stroke. In theory, arresters may provide protection against direct strokes, but they must be used at very close intervals (virtually every pole). Figure 10 shows flashover estimates for various arrester spacings to protect against direct strokes (see the Annex B for details and assumptions). The analysis in Figure 10 assumes that the neutral wire is grounded at every pole. The high number of flashovers, according to Figure 10, may be misleading where the neutral wire is not grounded except at poles where arresters are applied to all phases, and the neutral-to-ground insulation level is high.

**Figure 9**—Arrester spacing for flashovers from induced overvoltages

Arresters may be even more effective at reducing induced flashovers if they are used to protect poles with poor insulation levels. These “weak links” may include cut-outs, dead-end poles, or crossover poles. Placing arresters on these poles may be more cost-effective than improving the insulation level.

**Figure 10**—Effectiveness of arrester spacing for direct stroke protection

NOTE—Span length is 75 m.
7.3.1 Top-phase arrester protection

If the top-phase conductor is situated such that it will intercept all lightning strokes, arresters may be applied to the top phase which makes it act like an OHGW. Upon being struck, the top-phase arrester will conduct the surge to ground. The circuit will be protected if the arrester ground resistance is low enough and the insulation on the unprotected phases is high enough. Like an OHGW, care should be taken to maintain high insulation level on the unprotected phases. The curves for an OHGW (see Figure 8) may be used to estimate the effectiveness of a top-phase arrester design. The arresters should be used on virtually every pole or tower to achieve optimum protection.

7.3.2 Arrester energy absorption capability

Distribution arresters are divided into three energy classifications in Table 3.

<table>
<thead>
<tr>
<th>Arrester Class</th>
<th>Block Diameter (mm)</th>
<th>Energy Rating (kV/kJ MCOV)</th>
<th>Energy Rating (J/cm³)</th>
<th>Failure Rate per Direct Stroke in Unshielded Line</th>
</tr>
</thead>
<tbody>
<tr>
<td>Light Duty</td>
<td>25</td>
<td>3.0</td>
<td>170–200</td>
<td>33%–100%</td>
</tr>
<tr>
<td>Normal Duty</td>
<td>32</td>
<td>4.8</td>
<td>170–200</td>
<td>17%–50%</td>
</tr>
<tr>
<td>Heavy Duty</td>
<td>40</td>
<td>6.7</td>
<td>170–200</td>
<td>12%–33%</td>
</tr>
</tbody>
</table>

Energy ratings in Table 3 are the manufacturers’ minimum values, established for switching surge discharge tests. In fact, most high-quality MOV blocks absorb more than 500 J/cm³ at destruction with constant I-t product, independent of applied surge duration from seconds to microseconds, see Ringler et al. [B95]. The failure rate estimates for arresters in Table 3 are for three-phase plus neutral lines without overhead groundwires, having arresters on all poles and every phase. For lines with arresters only on the equipment and/or line protection with two or more spans between arresters, the arrester failure rate per direct stroke can be considerably less. The arresters are, on average, a greater distance from the flash termination, and unprotected insulators between the termination and arrester usually flashover, diverting most of the current to ground and minimizing energy duty. Arrester energy from induced overvoltages or terminations on OHGW is also much lower than for termination directly on phases and there is the same consequence of reduced arrester failure rate from these cases.

Light duty arresters are normally used only in specialized cases to protect underground installations. The industry generally uses either the normal duty or heavy duty arrester for the protection of overhead distribution lines. The greater energy capability of heavy duty arresters improves their survival rate by 5%–15% compared to normal-duty arresters.

In exposed applications (e.g., a distribution line in the open without an OHGW), distribution-class metal-oxide arresters may suffer unacceptable failure rate due to direct flashes. A significant percentage of direct lightning flashes to arresters result in energy duty that exceeds the manufacturer’s published capability and the 4/10 μs discharge test wave, see McDermott et al [B76]. This is tempered by the facts that metal-oxide blocks may have appreciably more surge-energy absorption capability than the published rating, see Ringler et al. [B95]. Another failure mechanism of some metal-oxide arrester designs is the occurrence of flashovers around the blocks when the arrester is subjected to multiple-stroke events. Surface flashovers due to multiple strokes are much less likely for arresters without air spacings such as polymer-housed arresters, see Darveniza et al. [B40]. Several studies, both field and laboratory, have evaluated arrester performance due to both single-stroke and multiple-stroke events, for example Darveniza et al. [B41], [B42], Fernandez et al. [B52], Mata et al. [B73], [B74], and Schoene et al. [B102] and [B103].
The energy dissipated in an arrester during a direct flash is not normally the full energy of the flash. Some impulse charge, and most charge from the tail of wave and continuing currents, is shared with other arresters nearby or flows to ground through the distribution system itself. Studies with rocket-triggered lightning and interpretation of direct measurements on utilities have verified that high energy dissipation levels occur that generally exceed the rating of a single distribution arrester, but 75% of this energy came from continuing dc currents between strokes, see Barker et al. [B9]. It is also known that, despite the high probability of an isolated arrester failure for a direct flash, fewer failures than expected are reported due to adjacent arresters absorbing some of the energy. Arresters used in rural line protection on exposed feeders frequently see energy levels which cause failures.

The failing arrester tends to be located near the stroke, but is not always nearest to the termination on the exposed conductor. Normally, a single arrester will fail electrically, leaving the others intact, but multiple arrester failures from a single flash have been found in field tests.

When interpreted on a system basis, a high arrester failure rate in response to a direct stroke may not have significant system impact. Taking the case of a 50 km distribution circuit of 13.2 kV with a mix of three-phase main feeders and single-phase laterals, with arresters on all poles and all phases, there would be about 1000 poles and 2000 arresters. In open terrain, this line would receive about 20 flashes per year in an area with high ground flash density such as Florida. This would lead to failure of two to seven heavy-duty arresters per year. After considering environmental shielding, and the reduced incidence of lightning in moderate climates, the observed heavy-duty arrester failure rates of one or two every five years in New York State by Barker et al. [B9] can be reconciled with Table 3.

Arresters can fail for a variety of reasons, in addition to excessive energy duty from lightning. Failures can also occur when temporary overvoltage limits are exceeded, from long-term moisture ingress or from mechanical component failures. Overhead lines with arresters need to be patrolled periodically and maintained, otherwise lightning performance will degrade with time.

8. Burial protection of distribution lines

For completeness, it is reasonable to include the use of buried cable rather than overhead construction as a method to improve the lightning performance of an overhead distribution line. This generally trades off one set of reliability concerns, namely adverse weather (lightning, wind, and ice) and interference (vegetation and animal), with a different set of concerns related to aging of the cable dielectric, dig-ins and long time to find and repair faults. In most situations, the economic balance favors overhead construction.

Buried cables are not immune from lightning damage, either from direct stroke damage or from induced overvoltages.

8.1 Direct stroke damage to buried cables

In 1993, an experiment was conducted at Camp Blanding, Florida, to study the effects of lightning on underground power distribution systems. The cables were 15kV coaxial cables with polyethylene insulation between the center conductor and the outer concentric shield (neutral). One of the cables (Cable A) had an insulating jacket and was placed in PVC conduit, another one (Cable B) had an insulating jacket and was directly buried, and the third one (Cable C) had no jacket and was directly buried. The three cables were buried 5 m apart at a depth of 1 m. Thirty lightning flashes were triggered, and lightning current was injected into the ground directly above the cables. Barker and Short [B11], [B12], and [B13] reported the following results from the underground power cables experiment.
— After lightning attachment to ground, a substantial fraction of the lightning current flowed into the neutral conductor of the cable with 15%–25% of the total lightning current (measured at the rocket launcher) being detected 70 m in either direction from the strike point.

— The largest voltage measured between the center conductor and the concentric neutral of the cable was 17 kV, which is below the cable’s basic insulation level (BIL) rating.

— Voltages measured at the transformer secondary were up to 4 kV. These could pose a threat to residential appliances.

According to Barker and Short [B13], in the triggered-lightning tests lightning was attracted to the cable for strikes as far as 10 m on either side of the cable.

8.2 Induced currents and voltages in buried cables

Paolone et al. [B84] measured induced currents on the order of 100 A on sheaths of buried cables located 50-200 m away from triggered lightning sources, suggesting sheath-to-earth impulse potential of about 30 kV for 10-kA source currents. Calculation of induced currents has confirmed the field measurements by adapting the Agrawal et al. model [B1] for the magnetic field illumination below grade, see Petrache et al. [B85]. At present, the minimum cable impulse level to withstand induced overvoltages from nearby lightning should be at least 30 kV and the requirement may exceed 100 kV when all factors are considered.
Annex A

(informative)

Examples of guide usage

A.1 Example 1—15 kV wooden crossarm design

Problem: a utility is performing a review of its standard 15 kV class, three-wire distribution-line design (see Figure A.1). The utility is in a moderate lightning area with an optical flash density of 12 flashes per km² per year. Insulators are ANSI-class 55-4, porcelain pin-type insulators. It is assumed that the crossarm braces are conducting and steel insulator pins are used. Guy wires have porcelain-strain insulators (ANSI-class 54-4). The standard pole size is 12.2 m with a planting depth of 2 m. The goal is to estimate the lightning performance level of the current design and investigate improvements.

![Figure A.1—15 kV class wooden crossarm design](image)

**Insulation level.** The CFO for several possible flashover paths are shown in Table A.1.

**Table A.1—CFO calculations for several possible flashover paths for the 15 kV pole design in Figure A.1**

<table>
<thead>
<tr>
<th>From</th>
<th>To</th>
<th>Flashover path</th>
<th>Total CFO (kV)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Middle phase</td>
<td>Guy wire</td>
<td>Insulators (105 kV) to 0.2 m wooden pole (47 kV) to guy insulator (0 kV)</td>
<td>152</td>
</tr>
<tr>
<td>Outer phase</td>
<td>Guy wire</td>
<td>Insulator (105 kV) to 0.6 m wooden crossarm (150 kV) to 0.2 m wooden pole (13 kV) to guy insulator (0 kV)</td>
<td>268</td>
</tr>
<tr>
<td>Right phase</td>
<td>Middle phase</td>
<td>Insulator (105 kV) to 0.6 m wooden crossarm (150 kV) to second insulator (20 kV)</td>
<td>275</td>
</tr>
<tr>
<td>Right phase</td>
<td>Middle phase</td>
<td>Air (0.6m)</td>
<td>360</td>
</tr>
</tbody>
</table>

**Direct strokes.** The GFD may be estimated from the optical transient density $N_T$, from Equation (3)

\[ N_g = 12/3 = 4 \text{ flashes/km}^2/\text{yr} \]
The top conductor height is 10.2 m with a structure width of 2.24 m. From Equation (7), the number of
direct flashes in open ground is

\[ N = 4 \times (10.2)^{0.6} \times 2.24 / 10 = 46 \text{ flashes/100 km/yr} \]

Assuming a 0.75 shielding factor, and that all direct strokes will cause a flashover, the estimated number
of direct-stroke flashovers are:

Direct-stroke flashovers = 11.5 flashovers/100 km/yr

Induced flashovers. The number of induced flashovers in open ground may be estimated from Figure 5
using the lowest CFO path of 152 kV, the curve for moderate conductivity of \( \sigma = 10 \text{ mS/m} \), and scaling by
the GFD:

Induced flashovers (open ground) = (4)2 flashes/100 km/yr = 8 flashovers/100 km/yr

Because much of the distribution line is shielded (bordered by tall structures, e.g., \( S_f = 0.75 \)), larger magni-
tude strokes can terminate close to the line, without striking the distribution line directly. This will cause
more induced flashovers. The number of induced-voltage flashovers should be somewhere between the
number of induced flashovers in open ground (8 flashes/100 km/yr in this case) and the number of direct
flashes in open ground (46 flashes/100 km/yr in this case). As an estimate, we will assume that the induced-
voltage flashovers are two times the induced flashovers in open ground.

Induced flashovers = 16 flashovers/100 km/yr

All flashovers are assumed to cause faults, as shown by:

Total faults = direct + induced = 27.5 faults/100 km/yr

Improvement options to consider. It has been decided to consider changes that are relatively inexpensive
and easy to implement. Insulation changes to reduce induced-voltage flashovers are the primary
consideration with a goal of a 300 kV CFO.

a) Use 50 cm fiberglass guy-strain insulators. This will increase the middle phase-to-guy CFO to 310
kV [0.5 m fiberglass guy-strain insulator (250 kV) + insulator (0.45 \times 105 kV = 47 kV) + 0.2 m
wooden pole (0.2 m \times 65 kV/m = 13 kV)]. This virtually eliminates induced-voltage flashovers.
Note: because the fiberglass strain insulator has an individual CFO much higher than any of the
other elements, it is taken first, rather than the insulator.

b) Use wooden crossarm braces. This will add a significant amount of wood to the middle phase-to-
guy flashover path. The CFO along this path would be approximately 255 kV [insulator (105 kV) +
wooden crossarm (0.52 m \times 250 kV/m = 130 kV) + wooden pole (0.3 m \times 65 kV/m = 20 kV)]. This
reduces the number of induced-voltage flashovers to less than 0.8 flashovers/100 km/yr.

Other structure designs such as dead-end, angle, and crossover should also be examined. Improvement
options may then be cost-compared to the existing design and against the improvement in service reliability
and power quality.

A.2 Example 2—35 kV distribution line with an OHGW

Problem: a utility is considering using a shielded distribution-line design for its 35 kV four-wire multi-
grounded neutral circuits (see Figure A.2). The line will be built in an area with a shielding factor of 0.5
provided by nearby objects and a ground flash density of 6.7 flashes per km² per year. The design provides
a shielding angle of 24°. The phase insulators are ANSI-class 57-2, porcelain post-type insulators on steel
brackets. The OHGW is supported by an ANSI-class 55-5, porcelain pin-type insulator. The distribution
line uses 15.24 m wooden poles, and every pole is grounded with a ground resistance of 10 \( \Omega \) or less.
Table A.2—Insulation CFOs for 35-kV Line in Figure A.2

<table>
<thead>
<tr>
<th>From</th>
<th>To</th>
<th>Flashover path</th>
<th>Total CFO (kV)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Ground wire</td>
<td>A, B, C</td>
<td>Post insulator (with no ground-wire standoff)</td>
<td>180</td>
</tr>
<tr>
<td>Static</td>
<td>A, B</td>
<td>Post insulator (180 kV) to 0.91 m wooden pole (214 kV) to pin insulator (24 kV)</td>
<td>418</td>
</tr>
<tr>
<td>Static</td>
<td>C</td>
<td>Post insulator (180 kV) to 2.13 m wooden pole (501 kV) to pin insulator (24 kV)</td>
<td>705</td>
</tr>
<tr>
<td>A</td>
<td>B</td>
<td>First post insulator (180 kV) to second post insulator (81 kV)</td>
<td>261</td>
</tr>
<tr>
<td>A, B</td>
<td>C</td>
<td>First post insulator (180 kV) to 0.91 m wooden pole (214 kV) to second post insulator (36 kV)</td>
<td>430</td>
</tr>
<tr>
<td>Pole ground lead</td>
<td>C</td>
<td>Post insulator (180 kV) to standoff (145 kV)</td>
<td>325</td>
</tr>
<tr>
<td>Pole ground lead</td>
<td>A, B</td>
<td>Post insulator (180 kV) to 0.8 m wooden pole (188 kV) to standoff (92 kV)</td>
<td>460</td>
</tr>
<tr>
<td>Pole ground lead</td>
<td>A, B, C</td>
<td>0.75 m air (450 kV)</td>
<td>450</td>
</tr>
</tbody>
</table>

From the CFO calculations in Table A.2, it is obvious that the fiberglass ground-lead standoffs are needed. The pole ground lead wire is offset with a 0.46 m fiberglass standoff insulator, and it is attached to the pole 0.49 m below the bottom phase conductor. Without the standoffs, the CFO would be 180 kV, which would lead to induced-voltage flashovers, and the OHGW would not be effective at preventing direct stroke flashovers. Although the lowest CFO path is 261 kV, the paths of most concern are the phase-to-ground flashover paths, because the voltage for a stroke to the OHGW and induced voltages are phase-to-ground voltage stresses. The lowest phase-to-ground flashover path is 325 kV from phase C to the pole ground lead.
**Direct strokes.** The GFD is read out directly from a map such as Figure 2:

\[ N_g = 6.7 \text{ flashes/km}^2/\text{yr} \]

The overhead groundwire height is 13.1 m and the width of the phase conductors is 1.5 m. From Equation (7), the number of direct flashes in open ground is

\[ N = 6.7\left[(28 \times 13.1^{0.6} + 1.5)/10\right] = 88.8 \text{ flashes/100 km/yr} \]

The estimated number of flashes using a shielding factor of 0.5 is:

Direct flashes to the distribution line = 44.4 flashes/100 km/yr

Because the distribution line is grounded at every pole and the shielding angle is less than 45°, all flashes to the distribution line are assumed to strike the OHGW. The number of flashovers may be determined using the percentage of direct strokes causing flashover (4%) from Figure 8 with a ground resistance of 10 Ω for the 350 kV CFO curve and the distribution of first return stroke peak current from Table 1.

Direct-stroke flashovers = (44.4 flashes/100 km/yr)(0.04) = 1.8 flashovers/100 km/yr

**Induced flashovers.** With a CFO of 325 kV, exceeding the horizontal axis in Figure 5, the structure may be assumed to be immune from induced-voltage flashovers for soil of low conductivity that exceeds 10 mS/m. All flashovers are then due to direct strokes, and all flashovers are assumed to cause faults, as shown by

\[ \text{Total faults} = \text{direct} = 1.8 \text{ faults/100 km/yr} \]

**Improvement options to consider.** The design shown in Figure A.2 has very good flashover performance. One concern is that the design goal of 10 Ω grounding resistance may be difficult to achieve in practice. Figure 8 may be used to estimate the reduction in performance due to the footing resistance. For example, if the footing resistance is 50 Ω, the flashover rate will increase to 35% of direct strokes (15.3 faults/100 km/yr).

An improvement option to consider would be using fiberglass insulator brackets instead of the steel brackets specified. This would increase the phase-to-phase and phase-to-ground CFO.

When comparing this design to non-shielded designs, the increase in construction cost should be weighed against the mitigated cost of the power interruptions caused by flashovers.
Annex B

(informative)

Technical modeling and assumptions

B.1 Shielding

An electrogeometric model may be used to estimate the shielding factor for a specific portion of a distribution line. An electrogeometric model is based on the idea that a distribution line or other object has a certain attractive radius which increases with height, and also the attractive radius is dependent on the current magnitude in the first return stroke. Although several models have been proposed, the equation used for the calculation of the striking distances in this guide is the equation adopted by the IEEE Working Group on Estimating the Lightning Performance of Transmission lines [B62], given by Equation (B.1).

\[
\begin{align*}
    r_s &= 10 \cdot I_o^{0.65} \\
    r_g &= 0.9 \cdot r_s
\end{align*}
\]  

(B.1)

where

- \( r_s \) is the striking distance to the conductor (m)
- \( r_g \) is the striking distance to the ground (m)
- \( I_o \) is the peak of the first return stroke current (kA)

This electrogeometric model is used for the shielding-factor calculations shown in Figure 4 and for the induced-voltage flashover estimations (see B.2). The electrogeometric model may also be used to estimate the number of direct flashes to a distribution line. This is an alternate approach to the Eriksson formula given in Equation (5). This electrogeometric model gives results for direct flashes which are close to the Eriksson formula for line heights below 15 m. For larger distribution-line heights, the difference is much greater. Different line performances are estimated by adopting different lateral striking distance expressions. However, such a difference tends to decrease as the ground conductivity decreases, see Borghetti et al. [B17] and Guerrieri et al. [B56], because flashes that terminate on the ground near the line induce overvoltages that cause flashovers anyway.

B.2 Induced-voltage flashovers

The lightning performance of overhead lines is generally represented by means of curves reporting how many flashovers per year a distribution line may experience, as a function of their insulation levels, as those shown in Figure 5.

Figure 5 in 5.2 replaces the corresponding one included in the 2004 version of this guide, which was obtained applying the Rusck simplified formula and the statistical procedure reported in this guide. As realistic line configurations cannot be represented with a simple single-conductor above a perfectly conducting ground, and the lightning current does not have a step lightning current waveform — as assumed by the Rusck formula— the above mentioned procedure may result, in several cases, in a poor estimation of the indirect lightning performance. Therefore, as mentioned earlier in 5.2, to overcome the limitations of the Rusck simplified model, the new version of Figure 5 has been obtained by applying a more general and accurate approach.
The newly adopted procedure will be described in detail in B.2.1. For sake of completeness, the original modeling assumptions in the previous versions of this Guide are reproduced in B.2.1 below along with the improved calculations. Since it makes an important difference and is now technically feasible using the lightning induced overvoltage [LIOV] code [B68], the indirect lightning performance calculation shall take into account the effect of the ground conductivity, which can significantly enhance the induced voltage amplitude. Also, the rise-time of the lightning current plays an important role in the problem of interest, and shall be taken into account.

Figure 5 of the previous versions of this Guide also included a curve showing the mitigation effect of a grounded conductor, calculated using a simplified ‘shielding factor’ formula proposed by Rusck [B100], which, as discussed later, provides an overestimation of the relevant mitigation effect on the phase conductor induced voltages.

B.2.1 Simplified procedure for the case of a line above ideal and lossy ground

This procedure, presented by the IEEE Working Group Report [B61], is based on the use of the so-called Rusck simplified formula for the calculation of the lightning-induced voltages and on the application of a statistical method proposed in Wagner and McCann [B113]. The statistical method has been improved by Chowdhuri [B26] in order to also take into account the statistical distribution of the lightning current rise-time, besides the peak-value one, as well as correlation factors between peak value and rise time.

The basic parameters considered in the procedure are the GFD $N_g$, the striking distance $r_s$, and the source term, which in this model, is the lightning-peak current $I_o$. Given the random nature of lightning, any calculation has to be kept within probabilistic bases and, as such, probabilistic distributions of the involved parameters have to be used. This Guide adopts the distribution described by Equation (4) for lightning-peak currents, assuming that it is not biased by the so-called ‘tower effect’, see Borghetti et al. [B20] and Rizk [B96], namely that it is the distribution for lightning-peak current at ground level.

The striking distance concept, which has to be considered here in order to determine the distance from the distribution line beyond where lightning will not strike the line, is that given in Equation (B.1).

According to the simplified Rusck formula [B100], the maximum voltage $V_{\text{max}}$ that is induced in a power line in the point closest to the stroke is given by:

$$
V_{\text{max}} = \frac{Z_0 I_o h}{y} \left( \frac{1}{y} + \frac{1}{c} \sqrt{2 - \left(\frac{y}{c}\right)^2} \right)
$$

(B.2)

where

- $Z_0 = 1/(4\pi \sqrt{\mu_0/\varepsilon_0})$ or $30 \Omega$
- $\mu_0$ and $\varepsilon_0$ are respectively the permeability and permittivity of free space (H/m, F/m)
- $I_o$ is the peak of the first return stroke current (kA)
- $h$ is the average height of the distribution line over the ground level (m)
- $y$ is the closest distance between the lightning stroke and the line (m)
- $v$ is the return-stroke velocity (m/s), typically $c/3$
- $c$ is the velocity of light in free space, $3 \times 10^8$ m/s

The value for $Z_0$ is $30 \Omega$, and the measured return stroke speed for natural lightning varies between $0.29 \times 10^8$ m/s and $2.4 \times 10^8$ m/s, see Idone and Orville [B60].
The simplified Rusck formula has been inferred by Rusck from the more general model he proposed in [B100] for the case of a step lightning current waveform and an infinitely-long single-conductor line above a perfectly conducting ground. These are unrealistic assumptions for most distribution lines.

As described in the IEEE Working Group Report [B61], the procedure is defined by the following steps in order to estimate the flashover frequency. The range of the lightning-peak current 1 kA to 200 kA is divided in intervals of 1 kA, and the probability of current peak to be within that interval is calculated from Equation (4). This is found as the difference between the probability for current to be equal or larger than the lower limit and the probability for current to reach or exceed the higher limit.

The maximum distance $y_{max}$ for every peak current interval at which lightning may produce an insulation flashover in the distribution line is then calculated. This is obtained by solving Equation (B.2) for $y$, by taking $I_0$ as the lower current limit of the interval, and taking $V_{max} = 1.5 \times CFO$. The 1.5 factor is an approximation which accounts for the turn-up in the insulation volt-time curve. This approximation is used for induced voltage, OHGW, and arrester spacing calculations. These voltages are assumed to have much shorter duration waveshapes than the standard 1.2/50 µs test wave.

The minimum distance $y_{min}$ for which lightning will not divert to the line, is calculated from Equation (B.3), as proposed in the IEEE Working Group Report [B61]. For this, $r_s$ and $r_g$ are calculated by taking the upper limit of the current interval.

\[
y_{min} = \sqrt{r_s^2 - (r_g - h)^2}
\]

(B.3)

where

- $y_{min}$ is the zone in Figure B.1 vulnerable to direct strokes (m)
- $r_s$ is the striking distance to the overhead line from Equation B.1 (m)
- $r_g$ is the striking distance to ground from Equation B.1 (m)
- $h$ is the average height of the distribution line over the ground level (m)

This is shown graphically in Figure B.1.
Figure B.1—Use of the electrogeometric model and the Rusck model for determining a direct stroke or induced-voltage flashover

For instance, following the described procedure, with CFO = 150 kV, for a current interval 49 kA to 50 kA, $y_{max}$ and $y_{min}$ result in 84.6 m and 72.5 m, respectively. In open ground, the three following scenarios may occur:

a) If the flash comes down between $y = 0$ and $y = y_{min} = 72.5$ m, strokes will terminate on the line
b) If the flash comes down between $y = y_{min} = 72.5$ m and $y = y_{max} = 84.6$ m, strokes will terminate on the ground and cause an induced-voltage flashover
c) Beyond $y = y_{max} = 84.6$ m, the flash will terminate on the ground and not cause a flashover

Finally, the number of induced voltage insulation flashovers per 100 km of distribution line per year, $F_p$, is obtained as the summation of the contributions from all intervals considered, as expressed by:

$$F_p = 2 \cdot N_g \cdot 0.1 \cdot \sum_{i=1}^{200} P_i \cdot (y_{i \text{ max}} - y_{i \text{ min}})$$  \hspace{1cm} (B.4)

where

$F_p$ is the number of insulation flashovers per 100 km of line per year
$N_g$ is the ground flash density (flashes per km$^2$ per year)
i is the prospective first return stroke peak current, incremented in intervals $\Delta i$ from 1 kA to 200 kA (kA)
$P_i$ is $P(i)-P(i+\Delta i)$ using Equation (5)
y_{i \text{ max}} is the zone in Figure B.1 vulnerable to flashover from direct strokes or induced voltage (m)
y_{i \text{ min}} is the zone in Figure B.1 vulnerable to direct strokes (m)
The effect of the presence of a grounded shield or neutral conductor was estimated in Figure 5 of previous version of the guide by using the following equation, again proposed in Rusck [B100]. This approach expresses the ratio of improvement $\eta$ as:

$$\eta = \frac{V''_{\text{max}}}{V'_{\text{max}}} = 1 - \frac{h_{\text{sw}}}{h} \frac{Z_{\text{sw-c}}}{Z_{\text{sw}} + 2R_g}$$

(B.5)

where

- $V''_{\text{max}}$ is lightning induced voltage on the line conductor in the presence of the shield wire (kV)
- $V'_{\text{max}}$ is the induced voltage on the conductor without the shield wire from Equation (B.2) (kV)
- $h_{\text{sw}}$ is the height of the shielding wire (m)
- $h$ is the height of the phase conductor (m)
- $Z_{\text{sw-c}}$ is the mutual surge impedance between the shielding wire and the line conductor
- $Z_{\text{sw}}$ is the surge impedance of the shielding wire ($\Omega$) from Equation (10)
- $R_g$ is the DC grounding resistance ($\Omega$)

This formula was obtained by assuming the grounded neutral or shielding wire as a non-illuminated conductor and with continuous grounding connections (see Rusck [B100]). The results shown in Figure 5 of the previous versions of this Guide refers to the case of $\eta = 0.75$, given by Short [B105]. They are reported, for convenience, also in Figure B.4 of this Guide, to be discussed next.

There is a simplified treatment of lossy ground effects on the lightning-induced voltage amplitude in Darveniza [B43]. Starting from the results presented in Guerrieri et al. [B56] and Nucci and Rachidi [B81], an extension of Rusck’s Equation B.2 is developed:

$$h_{\text{eff}} = h + 0.25 \cdot \sqrt{\rho}$$

$$V'_{\text{max}} = 28 \cdot I_o \cdot \frac{h_{\text{eff}}}{y}$$

(B.6)

where

- $h_{\text{eff}}$ is the effective height of the conductor (m)
- $\rho$ is the resistivity of the soil ($\Omega$m), noting that conductivity $\sigma$ (mS/m) = $1000/\rho$
- $h$ is the average height of the distribution line over the ground level (m)
- $I_o$ is the peak of the first return stroke current (kA)
- $y$ is the closest distance between the lightning stroke and the line (m)

Note that the statistical procedure to infer the lightning performance curves in presence of a lossy ground would be the same as the one above described for the case of ideal ground.
B.2.2 Monte Carlo procedure used for the case of a multi-conductor line above ideal and lossy ground

Detailed models for estimating the induced voltage have been derived, see Agrawal et al. [B1], De La Rosa [B46], Master and Uman [B70], and Nucci et al. [B80], [B81], and [B82]. Efforts have been made to formulate a complete model which takes into account soil effects on the peak amplitude and waveshape of the induced voltage. This procedure was used to obtain Figure 5, with the following specifics:

a) The application of the Monte Carlo method – which allows for a straightforward inclusion of the probabilistic distributions of the involved parameters along with their correlation factors

b) The use of more general and accurate models for the calculation of the voltages induced along a multi-conductor line, designed to take into account any waveshape of the lightning current and the finite ground conductivity in the calculation of both surge propagation and lightning electromagnetic field, see Cooray [B32] and [B34] and Rubinstein [B99]. Also, the importance of the treatment of multiple grounds on the shield (or neutral) conductor can be taken into account when appraising the relevant mitigation effects.

The procedure for obtaining the lightning performance of a distribution line is defined by the following steps from Borghetti et al. [B22].

B.2.2.1 Lightning induced overVoltage (LIOV) [B68] computer code

The above models – described fully in Nucci and Rachidi [B83] – have been implemented into a computer code (LIOV, lightning-induced overvoltage code). Starting from the waveform of the lightning current at the channel base, the LIOV code implements a return-stroke model, see Nucci et al. [B78] and Uman et al. [B1111], to infer the time and spatial distribution of the current along the channel. Then, the lightning electromagnetic pulse (LEMP) is calculated by using the Master and Uman equations [B70], along with the Cooray-Rubinstein formula [B32], [B34], and [B99] to take into account the effect of the ground on the propagating field. The electromagnetic coupling model by Agrawal et al. [B1], suitably extended to the case of a lossy ground (see Rachidi et al. [B91]), is then used to represent the coupling between the LEMP and the multi-conductor overhead line that finally allows the evaluation of the induced voltages along the line. The accuracy of the LIOV code has been verified by the comparison with experimental data, both on reduced scale models, see Paolone et al. [B84] and Piantini et al. [B88], and on a full scale experiment with triggered lightning, see Paolone et al. [B85].

B.2.2.2 Agrawal et al coupling model

The Agrawal et al coupling model [B1] for the problem of interest is described by the following equations:

\[
\frac{\partial}{\partial x} \left[ v_i^\gamma(x,t) \right] + \left[ \sum_{\gamma} L_{\gamma} \right] \frac{\partial}{\partial t} \left[ i_i(x,t) \right] + \left[ \sum_{\gamma} \left[ E_{\gamma}^\nu \right] \right] \frac{\partial}{\partial t} \left[ i_i(x,t) \right] = \left[ E_{i}^\nu(x,h_i,t) \right]
\]  
(B.7)

\[
\frac{\partial}{\partial x} \left[ i_i(x,t) \right] + \left[ C_{\nu}^\prime \right] \frac{\partial}{\partial t} \left[ v_i^\nu(x,t) \right] = 0
\]  
(B.8)

---

1 The LIOV code has been developed within the framework of an international research cooperation involving the University of Bologna, the Swiss Federal Institute of Technology (Lausanne) and the University of Rome ‘La Sapienza’. A freeware version of the LIOV code for both ideal and lossy ground for an overhead line can be downloaded at www.ieee.org/pes-lightning.
\[
[v_i(x,t)] = [\hat{v}_i(x,t)] + [\hat{v}'_i(x,t)] = [\hat{v}'_i(x,t)] - \int_0^{h_i} E'_e(x,z,t)dz
\]  
(B.9)

where

\[
E'_e(x,h_i,t)
\]

is the horizontal component of the incident electric field along the x axis at the conductor height \(h_i\).

\[
E'_e(x,z,t)
\]

is the vertical component of the incident electric field at a location \(x\) along the conductor at height \(z\), often approximated as \(h_i E'_e(x,0,t)\).

\[
\xi_{ij}'
\]

is the matrix of transient ground resistance

\[
[L'_{ij}]\text{ and }[C'_{ij}]
\]

are the external inductance and the capacitance matrices per unit length of the line.

\[
[i_i(x,t)]
\]

is the current vector

\(\otimes\) denotes the convolution product

\[
[v'_i(x,t)]
\]

is the scattered voltage vector

\[
v'_i(x,t)
\]

is the excitation voltage vector

\[
v'_i(x,t)
\]

is the total voltage vector

The boundary conditions for the scattered voltage \(v'_i(x,t)\) at both line ends \((x=0, x=L)\) are:

\[
[v'_i(0,t)] = [-[R_{ij}]\hat{i_i(0,t) - [v'_i(0,t)]}
\]

\[
[v'_i(L,t)] = [-[R_{ij}]\hat{i_i(L,t) - [v'_i(L,t)]}
\]  
(B.10)

B.2.2.3 Monte Carlo simulation of ground flashes

A large number of lightning events \(n_{tot}\) are randomly generated in a numerical procedure, see Borghetti et al. [B22]. Each event is characterized by three parameters:

- \(I_p\), the peak value of the first return stroke
- \(t_f\), the time to peak of the first return stroke
- \(y\), the closest distance from the stroke location to the line

Only first return strokes are taken into account in the analysis. Further work is needed to include the effects of subsequent return strokes, solving several open issues, such as i) relationship between the subsequent stroke’s path and that of the first stroke, ii) correlation between first and subsequent stroke current parameters, iii) number of subsequent strokes.

The first two values, namely \(I_p\) and \(t_f\) for first return strokes, are assumed to follow the two-step log-normal probability distributions adopted by Anderson and Eriksson [B5] and CIGRÉ [B27]. Note that Equation (5) follows the trend of the CIGRÉ two-line distributions comparatively well [B27]. The Monte Carlo method selects random values used to generate distributions for negative first return-stroke parameters found in Table B.1. The numerical method, see Borghetti et al. [B22] includes a correlation coefficient between \(t_f\) and \(I_p\), equal to 0.47 found in Anderson and Eriksson [B5].
Table B.1—Parameters of log-normal distribution for negative downward first strokes

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Median parameter value of the parameter logarithm (base e)</th>
<th>Standard deviation value of the parameter logarithm (base e)</th>
</tr>
</thead>
<tbody>
<tr>
<td>$I_p$ (kA)</td>
<td>$61 \text{ kA}$</td>
<td>$33.3 \text{ kA}$</td>
</tr>
<tr>
<td>$t_f$ ($\mu$s)</td>
<td>$3.83 \mu s$</td>
<td>$0.553$</td>
</tr>
</tbody>
</table>

These statistical distributions have been inferred mostly from measurements obtained by using instrumented towers. A median of 31 kA is appropriate for transmission towers up to 140 m in height, see Takami and Okabe [B110]. Measurements at the towers are affected by reflections, see Guerrieri et al. [B55]. Also, for taller towers, there is an increasing fraction of upward flashes that lead only to subsequent strokes. Moreover, the electrogeometric theory suggests that current amplitude distributions of the lightning events collected by towers are biased toward values higher than those of the distributions of the flashes to ground, see Borghetti et al. [B20], for example shifting the median first negative downward flash peak current from 31 kA to 24 kA or 25 kA, as used in versions of IEEE Std 998™ [B64]. These aspects are deliberately disregarded in this Guide.

B.2.2.4 Classification of ground flash locations

The distance of the prospective stroke location from the line $y$ is assumed to be uniformly distributed as far as a value $y_{\text{max}}$ (in m) in Figure B.1. Beyond this distance, it is assumed that none of the lightning events can cause a flashover on the line.

From the total set of events, those relevant to indirect lightning within the distances $y_{\text{min}}$ and $y_{\text{max}}$ are selected by adopting a lightning incidence model for the line to establish $y_{\text{min}}$. The striking distance model represented by Equation (B.1) is adopted. A comparison between results obtained by adopting other different incidence models has been presented in Borghetti et al. [B22].

For each indirect lightning event, the maximum induced voltage value on the line from the first return stroke is calculated by means of the LIOV code described above.

With $n$ the number of events generating induced voltages larger than the insulation level, here assumed equal to the line critical flashover voltage (CFO) multiplied by a factor equal to 1.5, the number of annual insulation flashovers per 100 km of distribution line $F_p$ is obtained as:

$$F_p = 200 \cdot \frac{n}{n_{\text{tot}}} \cdot N_g \cdot y_{\text{max}} \quad (B.11)$$

where

$F_p$ is the number of flashovers from induced voltage per 100 km of distribution line per year

$N_g$ is the annual lightning ground flash density (flashes per km$^2$ per yr)

$y_{\text{max}}$ is the dimension defined in Figure B.1

$n_{\text{tot}}$ is the total number of flashes considered to fall in the zone of $\pm y_{\text{max}}$

In order to obtain Figure 5 results, this procedure was applied to a 2 km long line matched at both terminations with surge impedance $Z_o$, with flash locations equidistant from the line ends, giving a configuration equivalent to an infinitely long line.
B.2.3 Case of a single-conductor line above an ideal ground

The more general procedure adopted in this Guide and the one proposed in the previous versions of the Guide differ in two main aspects:

a) The adopted statistical approach

b) The method for computing the peak induced voltage

For a lightning channel perpendicular to a perfectly conducting ground plane, the Agrawal et al. coupling model and the Rusck model predict the same results, see Cooray [B33]. It is thus possible to compare the effects of the second aspect (b) by carrying out a LIOV and Monte Carlo computation that forces $t_f$ to be constant and equal to a small value in the Monte Carlo method. Figure B.2 shows the results of this, using $t_f = 1 \mu s$ for curve C, which shows good match with curve A obtained with the Rusck model. In contrast, the result from Borghetti et al. [B22] for a realistic distribution of first return stroke front time from Anderson and Eriksson [B5] shows an order of magnitude difference in the flashover rate for a line with 150 kV–200 kV CFO.

![Figure B.2—Influence of front time in models of distribution line flashover rate](image)

NOTE 1—Curve A from IEEE Std. 1410™-2002 using the Rusck model for step wave over perfect ground

NOTE 2—Curve B from LIOV analysis for realistic distribution of front time over perfect ground

NOTE 3—Curve C from LIOV analysis for 1$\mu$s front time over perfect ground

NOTE 4—The line configuration with the same shielding factor value used for the previous version of the Guide, namely 0.75 has been assumed, see Short [B105]

NOTE 5—The line has a height $h=10$ m with radius $r=5$ mm
B.2.4 Case of two-conductor line over ideal ground

Concerning the mitigation effect of the OHGW grounding, the recommended LIOV procedure takes into account the LEMP-coupling of the OHGW together with the presence of the multiple groundings, as described by Paolone et al. [B84]. In order to take into account also the relative position of the flash location with respect the locations of these multiple ground connections, with particular reference to the closest ones, the random flash locations are, for this case, uniformly spread within a given surface that surrounds the line (henceforth called indirect striking area) and that includes all flashes at a distance equal or less than 1 km from the line.

Figure B.3 shows the comparison between the flashover rate curve of Figure 5 of the previous version of the Guide (curve A) and those obtained by using the procedure proposed in this Guide, with the shielding wire grounded each 30 m (curve B), and grounded each 500 m (curve C), for the case of a line above an ideal ground. Both curve B and C are obtained by forcing $t_f$ to be equal to 1 μs, in order to make the comparison consistent and to emphasize the impact of the grounding spacing on the results. Figure B.3 shows that $\eta$ from Equation (B.5) gives accurate results only for short spacing values between two adjacent groundings, in accordance with the findings of Paolone et al. [B84].

![Figure B.3—Influence of grounding interval on distribution line flashover rate, using models with fast front time](image)

NOTE 1—Curve A from IEEE Std 1410-2004 using the Rusck model for step wave over perfect ground with shield wire assumed to be at zero potential

NOTE 2—Curve B from LIOV analysis for 1 μs front time over perfect ground and grounding every 30 m

NOTE 3—Curve C from LIOV analysis for 1 μs front time over perfect ground and grounding every 500 m

NOTE 4—The line configuration with the same shielding factor value used for the previous version of the Guide, namely 0.75 has been assumed, see Short [B105].

NOTE 5—The line has a height $h=10$ m with radius $r=5$ mm; A shield (neutral) wire is placed at $h=8.37$ m above a perfectly conducting ground, having the same radius, and resistance at each grounding point is $R_g=0$ Ω
B.2.5 Effect of shielding

The results given in Figure 5 pertain to a distribution line in open ground. A circuit with nearby trees or buildings will not have as many direct strokes, but there will be more of an opportunity for induced-voltage flashovers because the nearby objects will allow strokes closer to the line.

B.3 Shield-wire modeling for direct lightning


Because of shorter span lengths on distribution lines, reflections from adjacent poles will greatly reduce the insulator voltage. Reflections from adjacent poles will reduce both the peak voltage and the tail of the waveshape. For calculation of the peak voltage, only the adjacent poles need to be considered. For calculation of tail voltages, additional poles need to be considered (the FLASH model neglects towers beyond the adjacent span).

The FLASH model performs voltage calculations at the return of the first reflection from the adjacent tower, at about 2 µs, and again after multiple reflections at 6 µs. For distribution lines, only a 2 µs voltage will be calculated. It is assumed that reflections from adjacent poles will quickly reduce the tail, so that if flashover does not occur at the 2 µs current crest it will not occur at all.

Power-frequency voltages may be ignored. Although this may affect which phase(s) flash over, power-frequency effects will not change the overall flashover rate.

The surge impedance of pole bonds is typically high but the travel time \( t = h_{sw}/c \) is short, so the combined effect does not significantly contribute to increased voltage on the linearly rising front of the wave. Therefore, pole effects may be ignored.

The simplified model considered is shown in Figure B.4 with adjacent pole grounds modeled. \( Z_s \) is the self-surge impedance of the OHGW.

![Simplified model of a direct stroke to a OHGW for distribution lines](image-url)
An expression for the voltage, including reflections from adjacent poles, is solved at $t = 2 \mu s$ as shown in Equation (B.6). The derivation is given for a similar problem, see Anderson [B4].

\[
V = \frac{I_R}{2} \left[ Z_L - \frac{Z_w}{1 - \psi} \right] + I_R \tau \frac{Z_w}{(1 - \psi)} \left[ \frac{1}{(1 - \psi)^2} - N\psi^N \right]
\]  
\[\text{(B.12)}\]

\[
Z_L = \frac{R Z}{Z + R_i}
\]  
\[\text{(B.13)}\]

\[
Z_w = \frac{2R_i^2 Z}{(Z + R_i)^2} \frac{Z - R_n}{Z + R_n}
\]  
\[\text{(B.14)}\]

\[
\psi = \frac{(Z - R_i)(Z - R_n)}{(Z + R_i)(Z + R_n)}
\]  
\[\text{(B.15)}\]

\[
R_n = 0.5 \frac{Z_S R_n}{(Z_S + R_n)}
\]  
\[\text{(B.16)}\]

where

- $V$ is voltage on the ground lead at the stroke location (kV)
- $I_R$ is the peak first-stroke current (kA)
- $t$ is the assumed equivalent linear front for all first negative return strokes (2 $\mu$s)
- $\tau$ is the travel time to the next adjacent pole at the speed of light $c$ (s)
- $Z$ is one-half of the unidirectional surge impedance $Z_I$ of the ground wire from Equation (10) ($\Omega$)
- $R_i$ is the local pole footing resistance ($\Omega$) including ionization effects ($\Omega$)
- $R_n$ is the adjacent pole footing resistance without ionization effects ($\Omega$)
- $R_o$ is the parallel resistance of the two adjacent pole footings and ground wires
- $Z_I$ is the intrinsic impedance ($\Omega$)
- $Z_W$ is the wave impedance ($\Omega$)
- $\psi$ is the dimensionless damping coefficient
- $N$ is the number of relevant reflections from the adjacent pole, namely the largest whole number $\leq t/2\tau$

The voltage from phase-to-ground across the insulation is equal to $V(1 - C_n)$, where $C_n$ is the coupling coefficient modified by corona effects, as in IEEE Standard 1243-1997, Anderson [B4] and the IEEE Working Group [B62]. Note that, if the corona radius exceeds a large fraction of the spacing from shield wire to conductor, the coupling coefficient approaches unity (no backflashovers) but the probability of midspan flashovers through the corona envelope increases.

For single rod electrodes that can ionize to a hemispherical zone under typical direct lightning flash current, a nonlinear ground given by the following equations is used for the ground of the stricken pole, see CIGRÉ Working Group 33.01 [B27] and Mousa [B77]:
\[ R_i = \frac{R_o}{\sqrt{1 + i_R/i_g}} \]  \hspace{1cm} (B.17)

\[ I_g = \frac{E_g \rho}{2\pi R_o^2} \]  \hspace{1cm} (B.18)

\[ i_R = I_R \frac{R_o Z}{R_o + Z} \]  \hspace{1cm} (B.19)

where

- \( R_i \) is the pole footing resistance which is a function of its footing current \( i_R \)
- \( R_o \) is the normally measured low-frequency low-current resistance (\( \Omega \))
- \( I_g \) is a reference current at which ionization effects become important (kA)
- \( E_g \) is the soil ionization gradient, assumed at 300 kV/m, see Mousa [B77]
- \( \rho \) is the soil resistivity (\( \Omega \)m)
- \( I_R \) is the peak stroke current (kA)
- \( Z \) is one-half of the unidirectional surge impedance \( Z_s \) of the ground wire from Equation (10) (\( \Omega \))

Because much less current will flow through the adjacent pole grounds, the low-current resistance, \( R_o \), is used for the adjacent pole grounds in Equation (B.16).

At 2 µs, the volt-time insulation curve is assumed to have a turn up of 1.5 times the CFO. This is similar to the volt-time curve for insulator lengths used in the FLASH model (which is 1.52 times the CFO at 2 µs). This model is iterated to find a critical current used to find a probability of flashover using Equation (4). The remainder of the assumptions for shield-wire modeling are the same as the FLASH model.

For the shield-wire results shown in Figure 8, \( C_n = 0.35 \), \( Z_s = 400 \Omega \), \( \rho = 1000 \Omega \)-m, span length = 75 m, and span travel time \( \tau = 0.25 \) µs.

### B.4 Arrester spacing

#### B.4.1 Direct strokes

If a direct lightning flash terminates at midspan between a pole with arresters and a pole without arresters, the voltage that may develop on the unprotected pole is determined by the separation distance between the lightning stroke and the pole with arresters. This is determined by:

\[ V = \left( V_{IR} + \frac{L IZ_o}{c 2t_m} \right) \]  \hspace{1cm} (B.20)
where

\[ V \] is the peak voltage across the insulation
\[ V_{IR} \] is the arrester-discharge voltage level
\[ L \] is the separation distance to the next pole with arresters (m), which is half the span length at midspan
\[ c \] is the wave velocity (3 \times 10^8 \text{ m/s})
\[ I \] is the peak of the lightning impulse current
\[ Z_0 \] is the line surge impedance
\[ t_m \] is the linear equivalent 0-100\% front time, assumed to be 2 \mu s for a large first return stroke

The peak first return-stroke current required to cause a flashover may be found by setting \( V = 1.5 \cdot CFO \) and solving for \( I_{midspan} \):

\[
I_{midspan} = \frac{2ct_m(1.5 \cdot CFO - V_{IR})}{LZ_0}
\]  

(B.21)

The 1.5 factor approximates the turn-up in the insulation volt-time curve for \( T_f = 2 \mu s \).

Assuming a \( t_m = 2 \mu s \), \( CFO = 350 \text{ kV} \), \( Z_0 = 400 \Omega \), \( L = 75 \text{ m} \), and \( V_{IR} = 40 \text{ kV} \), the percentage of flashovers may be calculated:

\[ I_{midspan} = 19.4 \text{ kA} \]

The probability of exceeding this current, given by Equation (4), gives the probability of flashover as

\[ P_{midspan} = 77.2\% \]

A direct flash to a pole with phases not protected by arresters is assumed to flashover 100\% of the time. A direct flash to a pole fully protected by arresters will not flashover, but the probability of flashover at the next unprotected pole still exists. This is determined by the CFO of the unprotected pole and the ground resistance \( R_o \) at the pole with arresters.

\[
I_{pole} = \frac{1.5 \cdot CFO - V_{IR}}{R_o}
\]

(B.22)

The probability of flashover may be calculated from the critical current \( I_{pole} \) with \( V_{IR} = 40 \text{ kV} \).

If \( R_o = 25 \Omega \) and \( CFO = 150 \text{ kV} \), then

\[ I_{pole} = 7.4 \text{ kA}, \ P_{pole} = 98\% \]

If \( R_o = 10 \Omega \) and \( CFO = 350 \text{ kV} \), then

\[ I_{pole} = 48.5 \text{ kA}, \ P_{pole} = 24\% \]

Using the probabilities of flashes to poles with arresters, poles without arresters, and conductors midspan between poles (assuming 50\% of the time the flash terminates at midspan), it is possible to create a table of flashovers versus arrester spacing as shown in Table B.2.
Table B.2—Direct first return stroke flashovers for different spans to the next arrester

<table>
<thead>
<tr>
<th>Spans between arresters</th>
<th>Percent flashover $R_o = 25 \Omega$, CFO = 150 kV</th>
<th>Percent flashover $R_o = 10 \Omega$, CFO = 350 kV</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td>2</td>
<td>100</td>
<td>70</td>
</tr>
<tr>
<td>3</td>
<td>100</td>
<td>80</td>
</tr>
<tr>
<td>4</td>
<td>100</td>
<td>85</td>
</tr>
<tr>
<td>Infinite</td>
<td>100</td>
<td>100</td>
</tr>
</tbody>
</table>

With two spans between arresters and $R_o = 10 \Omega$ and CFO = 350 kV, use the following to arrive at the number in the table:

Assume, 50% terminate at midspan (use $P_{mid} = 77.2\%$), 25% terminates at a pole with arresters (use $P_{pole}=24\%$), 25% terminates at an unprotected pole (100% of these flash), so

\[
\text{Probability} = 0.25 + 0.25 \cdot P_{pole} + 0.50 \cdot P_{mid} = (0.25 + 0.25 \cdot 24 + 0.5 \cdot 77.2) = 70\% 
\]

For three spans between arresters use:

\[
\frac{3}{6} + \frac{1}{6} \cdot P_{pole} + \frac{2}{6} \cdot P_{mid}
\]

For four spans between arresters, use:

\[
\frac{5}{8} + \frac{1}{8} \cdot P_{pole} + \frac{2}{8} \cdot P_{mid}
\]

B.4.2 Induced voltage flashovers

With the procedure described in B.2.2, more realistic line configurations than those considered in the previous versions of the Guide, for instance including the presence of active protection devices, like surge arresters, can be analyzed. This Section illustrates this point.

A single-conductor line with height $h=10 \text{ m}$ high has surge arresters placed at regular intervals along the line. According to the recommendations in the IEEE Fast Fronts Transient Task Force [B63], the surge arresters are modeled using a $V-I$ non-linear characteristic, which has been obtained by the standard 1.2/50 $\mu$s pulse test on a typical 20 kV surge arrester, see Borghetti et al. [B21].

Figure B.5 shows the influence of the presence of the surge arresters and of the spacing between two consecutive surge arrester positions for the case of ideal ground.
Figure B.5—Line flashover rate curves for the case of a line over ideal ground, without and with surge arresters (SA) located every 500 m and 200 m; $h=10$ m, $N_g=1$ flash/(km²·yr)

The results of Figure B.5 show that a significant improvement of the lightning performance of the considered distribution line can be obtained by reducing the spacing between the surge arresters below 300 m, in accordance with the results published in Borghetti et al. [B21].

Figure B.6—Line flashover rate curves for the case of a line with $h=10$ m, over lossy ground ($\sigma_g = 1$ mS/m) without and with surge arresters (SA) located every 500 m and 200 m $N_g=1$ flash/(km²·yr)
The lightning performance of the lines with low CFO may be even degraded rather than improved by the presence of surge arresters as shown in Figure B.6. This is due to the surge reflections occurring in correspondence of surge arrester operations, particularly important for stroke locations distant from the line and for large separation between consecutive arrester positions, see Paolone et al. [B84].
Annex C

(informative)

Bibliography


