16

Insulation Coordination and Overvoltage Protection

INTRODUCTION

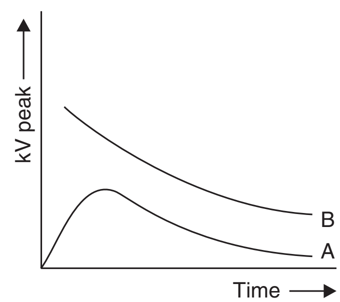

Fig. 16.1 Volt-time curve A (protecting device and) volt-time curve B (device to be protected)

Insulation coordination means the correlation of the insulation of the various equipments in a power system to the insulation of the protective devices used for the protection of those equipments against overvoltages. In a power system various equipments like transformers, circuit breakers, bus supports etc. have different breakdown voltages and hence the volt-time characteristics. In order that all the equipments should be properly protected it is desired that the insulation of the various protective devices must be properly coordinated. The basic concept of insulation coordination is illustrated in Fig. 16.1. Curve *A* is the volt-time Curve of the protective device and *B* the volt-time curve of the equipment to be protected. Figure 16.1 shows the desired positions of the volt-time curves of the protecting device and the equipment to be protected. Thus, any insulation having a withstand voltage strength in excess of the insulation strength of curve *B* is protected by the protective device of curve *A*.

The 'volt-time curve' expression will be used very frequently in this chapter. It is, therefore, necessary to understand the meaning of this expression.

16.1 VOLT-TIME CURVE

The breakdown voltage for a particular insulation or flashover voltage for a gap is a function of both the magnitude of voltage and the time of application of the voltage. The volt-time curve is a graph showing the relation between the crest flashover voltages and the time to flashover for a series of impulse applications of a given wave shape. For the construction of volt-time curve the following procedure is adopted. Waves of the same shape but of different peak values are applied to the insulation whose volt-time curve is required. If flashover occurs on the front of the wave, the flashover point gives one point on the volt-time curve. The other possibility is that the flashover occurs just at the peak value of the wave; this gives another point on the *V-T* curve. The third possibility is that the flashover occurs on the tail side of the wave. In this case to find the point on the *V-T* curve, draw a horizontal line from the peak value of this wave and also draw a vertical line passing through the point where the flashover takes place. The intersection of the horizontal and vertical lines gives the point on the *V-T* curve. This procedure is nicely shown in Fig. 16.2.

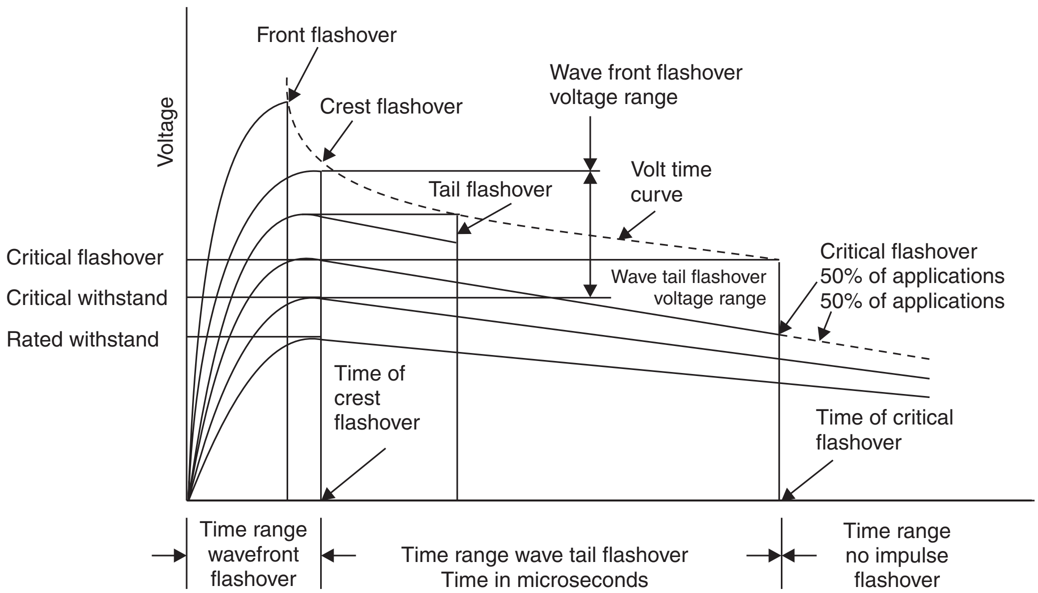

Fig. 16.2 Volt-time curve (construction)

The overvoltages against which coordination is required could be caused on the system due to system faults, switching operation or lightning surges. For lower voltages, normally up to about 345 kV, overvoltages caused by system faults or switching operations do not cause damage to equipment insulation although they may be detrimental to protective devices. Overvoltages caused by lightning are of sufficient magnitude to affect the equipment insulation whereas for voltages above 345 kV it is these switching surges which are more dangerous for the equipments than the lightning surges.

The problem of coordinating the insulation of the protective equipment involves not only guarding the equipment insulation but also it is desired that the protecting equipment should not be damaged.

To assist in the process of insulation coordination, standard insulation levels have been recommended. These insulation levels are defined as follows:

Basic impulse insulation levels (BIL) are reference levels expressed in impulse crest voltage with a standard wave not longer than 1.2/50 μsec wave. Apparatus insulation as demonstrated by suitable tests shall be equal to or greater than the basic insulation level.

The problem of insulation coordination can be studied under three steps:

1. Selection of a suitable insulation which is a function of reference class voltage (*i.e.*, 1.05 × operating voltage of the system). Table 16.1 gives the BIL for various reference class voltages.

Table 16.1 Basic Impulse Insulation Levels

Reference class kV	*Standard basic impulse level kV*	*Reduced insulation levels*
23	150	
34.5	200	
46	250	
69	350	
92	450	
115	550	450
138	650	550
161	750	650
196	900	
230	1050	900
287	1300	1050
345	1550	1300

2. The design of the various equipments such that the breakdown or flashover strength of all insulation in the station equals or exceeds the selected level as in (1).

3. Selection of protective devices that will give the apparatus as good protection as can be justified economically.

The above procedure requires that the apparatus to be protected shall have a withstand test value not less than the kV magnitude given in the second column of Table 16.1, irrespective of the polarity of the wave positive or negative and irrespective of how the system was grounded.

The third column of the table gives the reduced insulation levels which are used for selecting insulation levels of solidly grounded systems and for systems operating above 345 kV where switching surges are of more importance than the lightning surges. At 345 kV, the switching voltage is considered to be 2.7 p.u., *i.e.*, 345 × 2.7 = 931.5 kV which corresponds to the lightning level. At 500 kV, however, 2.7 p.u. will mean 2.7 × 500 = 1350 kV switching voltage which exceeds the lightning voltage level. Therefore, the ratio of switching voltage to operating voltage is reduced by using the switching resistances between the C.B. contacts. For 500 kV, it has been possible to obtain this ratio as 2.0 and for 765 kV it is 1.7. With further increase in operating voltages it is hoped that the ratio could be brought to 1.5. So, for

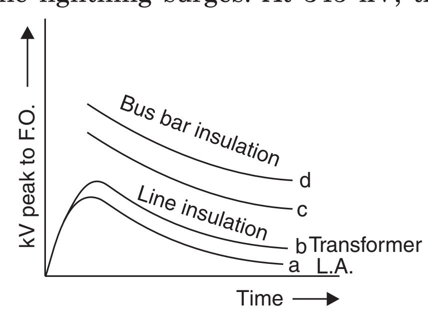

Fig. 16.3 Volt-time curves

switching voltages the reduced levels in third column are used *i.e.*, for 345 kV, the standard BIL is 1550 kV but if the equipment can withstand even 1425 kV or 1300 kV it will serve the purpose. Figure 16.3 gives the relative position of the volt-time curves of the various equipments in a substation for proper coordination. To illustrate the selection of the BIL of a transformer to be operated on a 138 kV system assume that the transformer is of large capacity and its star point is solidly grounded. The grounding is such that the line-to-ground voltage of the healthy phase during a ground fault on one of the phase is say 74% of the normal L-L voltage. Allowing for 5% overvoltage during operating conditions, the arrester rms operating voltage will be $1.05 \times 0.74 \times 138 = 107.2$ kV. The nearest standard rating is 109 kV. The characteristic of such a L.A. is shown in Fig. 16.4. From the figure the breakdown value of the arrester is 400 kV. Assuming a 15% margin plus 35 kV between the insulation levels of L.A. and the transformer, the insulation level of transformer should be at least equal to $400 + 0.15 \times 400 + 35 = 495$ kV. From Fig. 16.4 (or from the table the reduced level of transformer for 138 kV is 550 kV) the insulation level of transformer is 550 kV; therefore a lightning arrester of 109 kV rating can be applied.

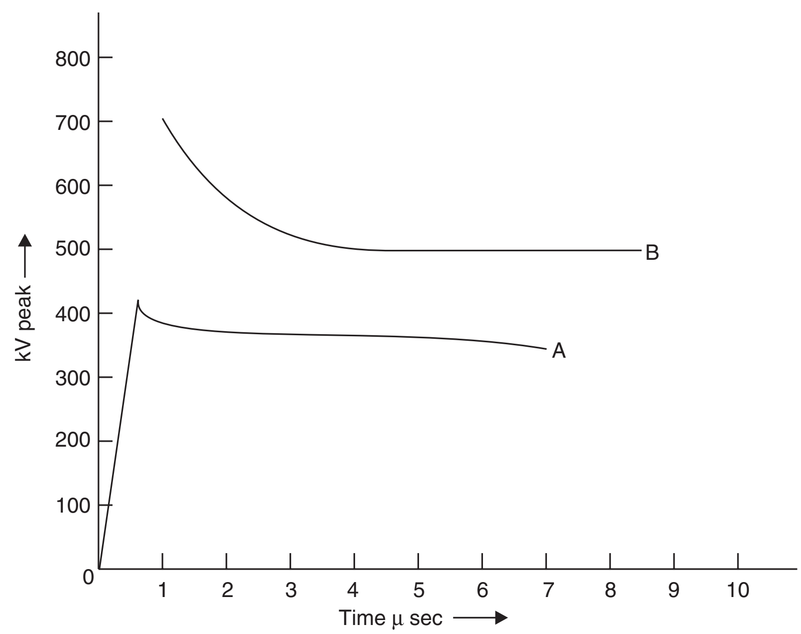

Fig. 16.4 Coordination of transformer insulation with lightning arrester: A—Lightning arrester 109 kV, B—Transformer insulation withstand characteristic.

It is to be noted that low voltage lines are not as highly insulated as higher voltage lines so that lightning surges coming into the station would normally be much less than in a higher voltage station because the high voltage surges will flashover the line insulation of low voltage line and not reach the station.

The traditional approach to insulation coordination requires the evaluation of the highest overvoltages to which an equipment may be subjected during operation and selection of standardized value of withstand impulse voltage with suitable safety margin. However, it is realized that overvoltages are a random phenomenon and it is uneconomical to design plant with such a high degree of safety that they sustain the infrequent ones. It is also known that insulation designed on this basis does not give 100% protection and insulation failure may occur even in well designed plants and, therefore, it is desired to limit the frequency of insulation failures to the most economical value taking into account equipment cost and service continuity. Insulation coordination, therefore, should be based on evaluation and limitation of the risk of failure than on the a *priori* choice of a safety margin.

The modern practice, therefore, is to make use of probabilistic concepts and statistical procedures especially for very high voltage equipments which might later on be extended to all cases where a close adjustment of insulation to system conditions proves economical. The statistical methods even though laborious are quite useful.

16.2 OVERVOLTAGE PROTECTION

The causes of overvoltages in the system have been studied extensively in Chapter 12. Basically, there are two sources: (*i*) external overvoltages due to mainly lightning, and (*ii*) internal overvoltages mainly due to switching operation. The system can be protected against external overvoltages using what are known as shielding methods which do not allow an arc path to form between the line conductor and ground, thereby giving inherent protection in the line design. For protection against internal voltages normally non-shielding methods are used which allow an arc path between the ground structure and the line conductor but means are provided to quench the arc. The use of ground wire is a shielding method whereas the use of spark gaps, and lightning arresters are the non-shielding methods. We will study first the non-shielding methods and then the shielding methods. However, the non-shielding methods can also be used for external overvoltages.

The non-shielding methods are based upon the principle of insulation breakdown as the overvoltage is incident on the protective device; thereby a part of the energy content in the overvoltage is discharged to the ground through the protective device. The insulation breakdown is not only a function of voltage but it depends upon the time for which it is applied and also it depends upon the shape and size of the electrodes used. The steeper the shape of the voltage wave, the larger will be the magnitude of voltage required for breakdown; this is because an expenditure of energy is required for the rupture of any dielectric, whether gaseous, liquid or solid, and energy involves time. The energy criterion for various insulations can be compared in terms of a common term known as Impulse Ratio which is defined as the ratio of breakdown voltage due to an impulse of specified shape to the breakdown voltage at power frequency. The impulse ratio for sphere gap is unity because this gap has a fairly uniform field and the breakdown takes place on the field ionization phenomenon mainly whereas for a needle gap it varies between 1.5 to 2.3 depending upon the frequency and gap length. This ratio is higher than unity because of the nonuniform field between the electrodes. The impulse ratio of a gap of given geometry and dimension is greater with solid than with air dielectric. The insulators should have a high impulse ratio for an economic design whereas the lightning arresters should have a low impulse ratio so that a surge incident on the lightning arrester may be by passed to the ground instead of passing it on to the apparatus.

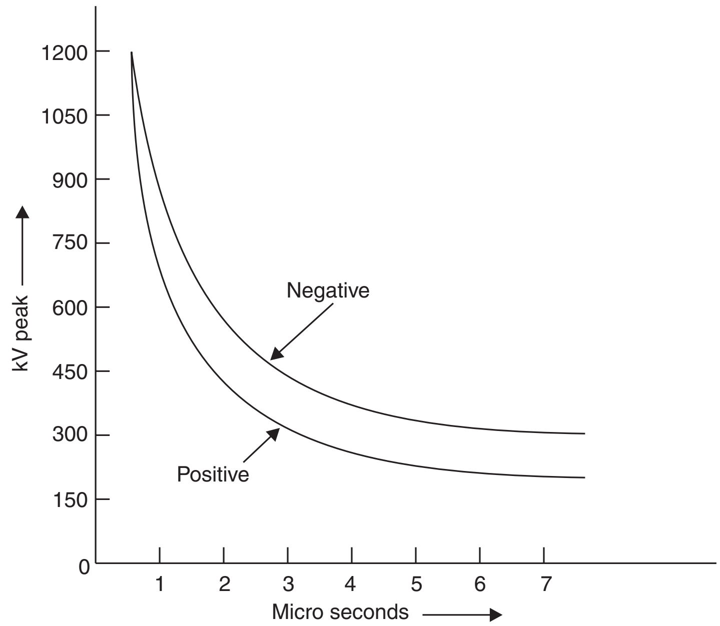

Fig. 16.5 Volt-time curves of gaps for positive and negative polarity.

The volt-time characteristics of gaps having one electrode grounded depend upon the polarity of the voltage wave. From Fig. 16.5 it is seen that the volt-time characteristic for positive polarity is lower than the negative polarity, *i.e.*, the breakdown voltage for a negative impulse is greater than for a positive because of the nearness of earthed metal or of current carrying conductors. For post insulators the negative polarity wave has a high breakdown value whereas for suspension insulators the reverse is true.

Horn Gap

The horn gap consists of two horn shaped rods separated by a small distance. One end of this is connected to the line and the other to the earth as shown in Fig. 16.6, with or without a series resistance. The choke connected between the equipment to be protected and the horn gap serves two purposes: (*i*) The steepness of the wave incident on the equipment to be protected is reduced. (*ii*) It reflects the voltage surge back on to the horn.

Whenever a surge voltage exceeds the breakdown value of the gap a discharge takes place and the energy content in the rest part of the wave is by-passed to the ground. An arc is set up between the gap, which acts like a flexible conductor and rises upwards under the influence of the electromagnetic forces, thus increasing the length of the arc which eventually blows out.

There are two major drawbacks of the horn gap: (*i*) The time of operation of the gap is quite large as compared to the modern protective gear. (*ii*) If used on isolated neutral the horn gap may constitute a vicious kind of arcing ground. For these reasons, the horn gap has almost vanished from important power lines.

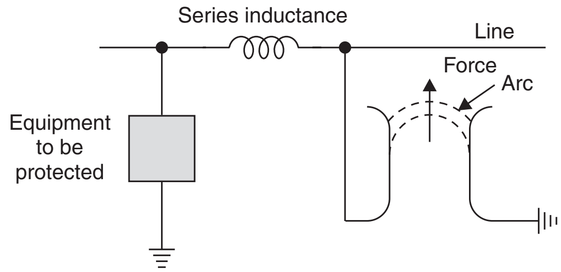

Fig. 16.6 Horn gap connected in the system for protection.

Surge Diverters

The following are the basic requirements of a surge diverter:

(*i*) It should not pass any current at normal or abnormal (normally 5% more than the normal voltage) power frequency voltage.

(*ii*) It should breakdown as quickly as possible after the abnormal high frequency voltage arrives.

(*iii*) It should not only protect the equipment for which it is used but should discharge the surge current without damaging itself.

(*iv*) It should interrupt the power frequency follow current after the surge is discharged to ground.

There are mainly three types of surge diverters: (*i*) Rod gap, (*ii*) Protector tube or expulsion type of lightning arrester, (*iii*) Valve type of lightning arrester.

Rod Gap

This type of surge diverter is perhaps the simplest, cheapest and most rugged one. Fig. 16.7 shows one such gap for a breaker bushing. This may take the form of arcing ring. Fig. 16.8 shows the breakdown characteristics (volt-time) of a rod gap. For a given gap and wave shape of the voltage, the time for breakdown varies approximately inversely with the applied voltage.

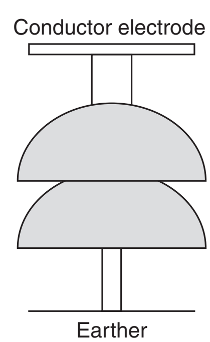

Fig. 16.7 A rod gap.

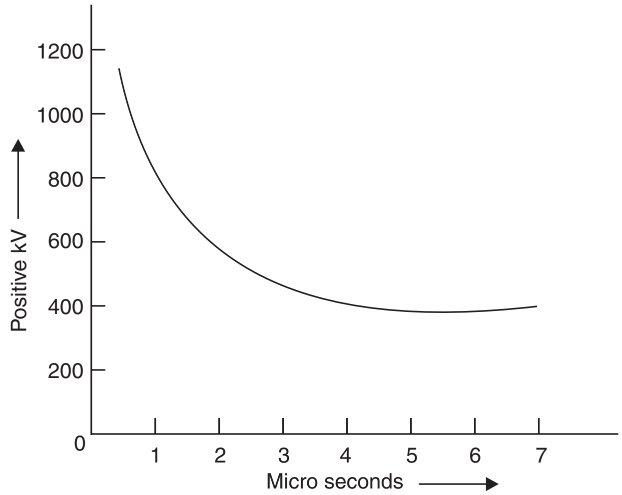

Fig. 16.8 Volt-time characteristic of rod gap.

The times to flashover for positive polarity are lower than for negative polarities. Also it is found that the flashover voltage depends to some extent on the length of the lower (grounded) rod. For low values of this length there is a reasonable difference between positive (lower value) and negative flashover voltages. Usually a length of 1.5 to 2.0 times the gap spacing is good enough to reduce this difference to a reasonable amount. The gap setting normally chosen is such that its breakdown voltage is not less than 30% below the voltage withstand level of the equipment to be protected.

Even though rod gap is the cheapest form of protection, it suffers from the major disadvantage that it does not satisfy one of the basic requirements of a lightning arrester listed at no. (*iv*) *i.e.*, it does not interrupt the power frequency follow current. This means that every operation of the rod gap results in a *L-G* fault and the breakers must operate to de-energize the circuit to clear the flashover. The rod gap, therefore, is generally used as back up protection.

Expulsion Type of Lightning Arrester: An improvement of the rod gap is the expulsion tube which consists of (*i*) a series gap (1) external to the tube which is good enough to withstand normal system voltage, thereby there is no possibility of corona or leakage current across the tube; (*ii*) a tube which has a fibre lining on the inner side which is a highly gas evolving material; (*iii*) a spark gap (2) in the tube; and (*iv*) an open vent at the lower end for the gases to be expelled (Fig. 16.9). It is desired that the breakdown voltage of a tube must be lower than that of the insulation for which it is used. When a surge voltage is incident on the expulsion tube the series gap is spanned and an arc is formed between the electrodes within the tube. The heat of the arc vaporizes some of the organic material of the tube wall causing a high gas pressure to build up in the tube. The resulting neutral gas creates lot of

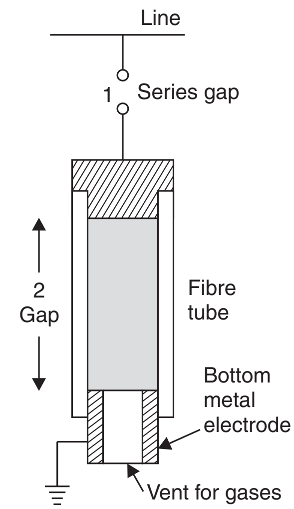

Fig. 16.9 Expulsion type lightning arrester.

turbulence within the tube and is expelled out from the open bottom vent of the tube and it extinguishes the arc at the first current zero. At this instant the rate of build up of insulation strength is greater than the RRRV. Very high currents have been interrupted using these tubes. The breakdown voltage of expulsion tubes is slightly lower than for plain rod gaps for the same spacing. With each operation of the tube the diameter of the tube (fibre lining) increases; thereby the insulation characteristics of the tube will lower down even though not materially. The volt-time characteristics (Fig. 16.10) of the expulsion tube are somewhat better than the rod gap and have the ability to interrupt power voltage after flashover.

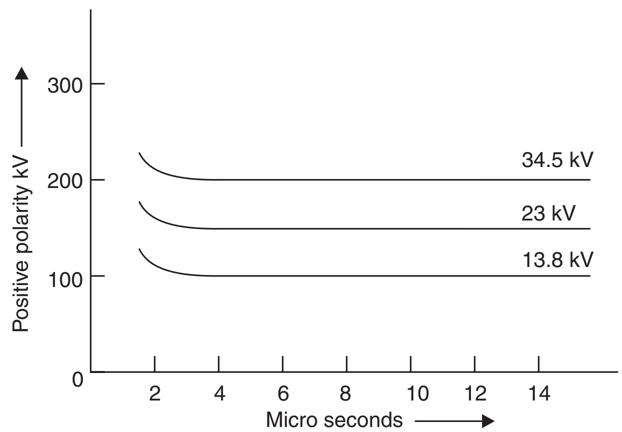

Fig. 16.10 Volt-time characteristic of expulsion gaps.

Valve Type Lightning Arresters: An improved but more expensive surge diverter is the valve type of lightning arrester or a non-linear surge diverter. A porcelain bushing (Fig. 16.11) contains a number of series gaps, coil units and the valve elements of the non-linear resistance material usually made of silicon carbide disc, the latter possessing low resistance to high currents and high resistance to low currents. The characteristic is usually expressed as $I = KV^n$, where n lies between 2 and 6 and K is constant, a function of the geometry and dimension of the resistor. The non-linear characteristic is attributed to the properties of the electrical contacts between the grains of silicon carbide. The discs are 90 mm in dia and 25 mm thick. A grading ring or a high resistance is connected across the disc so that the system voltage is evenly distributed over the discs. The high resistance keeps the inner assembly dry due to some heat generated.

Figure 16.12 shows the volt-ampere characteristics of a non-linear resistance of the required type. The closed curve represents the dynamic characteristic corresponding to the application of a voltage surge whereas the dotted line represents the static characteristic. The voltage corresponding to the horizontal tangent to the dynamic characteristic is known as the residual voltage (IR drop) and is the peak value of the voltage during the discharge of the surge current. This voltage varies from 3 kV to 6 kV depending upon the type of arrester *i.e.,* whether station or line type, the magnitude and wave shape of the discharge current. The spark gaps are so designed that they give an impulse ratio of unity to the surge diverter and as a result they are unable to interrupt high values of current and the follow up currents are limited to 20 to 30 A. The impulse breakdown strength of a diverter is smaller than the residual voltage, and therefore, from the point of view of insulation coordination residual voltage decides the protection level.

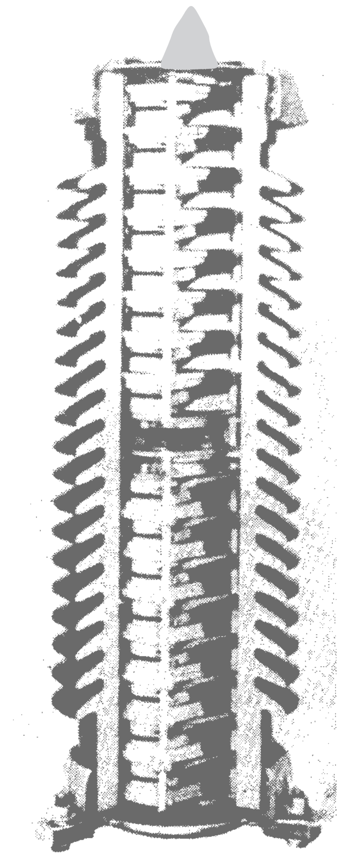

Fig. 16.11 Valve-type lightning arrester.

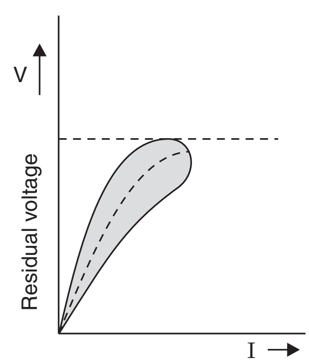

Fig. 16.12 Volt-ampere characteristic of valve-type LA.

The operation of the arrester can be easily understood with the help of Figs. 16.13 (*a*) and (*b*). When a surge voltage is incident at the terminal of the arrester it causes the two gap units to flashover, thereby a path is provided to the surge to the ground through the coil element and the non-linear resistor element. Because of the high frequency of the surge, the coil develops sufficient voltage across its terminals to cause the by-pass gap to flashover. With this the coil is removed from the circuit and the voltage across the LA is the IR drop due to the non-linear element. This condition continues till power frequency currents follow the preionized path. For power frequency the impedance of the coil is very low and, therefore, the arc will become unstable and the current will be transferred to the coil (Fig. 16.13 (*b*)). The magnetic field developed by the follow current in the coil reacts with this current in the arcs of the gap assemblies, causing them to be driven into arc quenching chambers which are an integral part of the gap unit. The arc is extinguished at the first current zero by cooling and lengthening the arc and also because the current and voltage are almost in phase. Thus the diverter comes back to normal state after discharging the surge to the ground successfully.

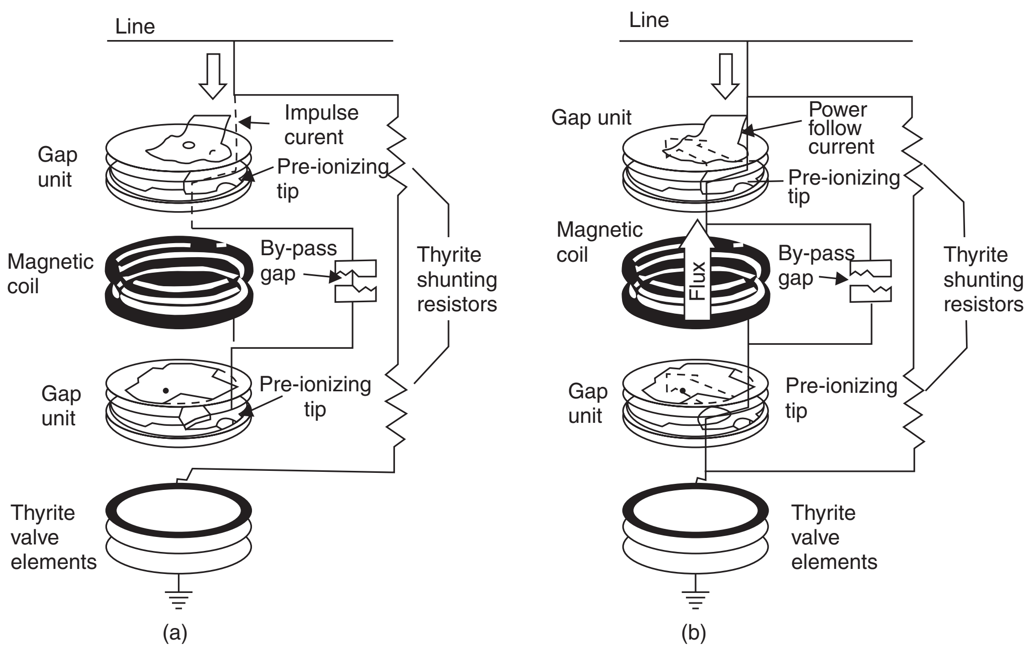

Fig. 16.13 Schematic diagram of valve-type arrester indicating path of (*a*) Surge current, (*b*) Follow current.

Location of Lightning Arresters: The normal practice is to locate the lightning arrester as close as possible to the equipment to be protected. The following are the reasons for the practice: (*i*) This reduces the chances of surges entering the circuit between the protective equipment and the equipment to be protected. (*ii*) If there is a distance between the two, a steep fronted wave after being incident on the lightning arrester, which sparks over corresponding to its spark-over voltage, enters the transformer after travelling over the lead between the two. The wave suffers reflection at the terminal and, therefore, the total voltage at the terminal of the transformer is the sum of reflected and the incident voltage which approaches nearly twice the incident voltage *i.e.,* the transformer may experience a surge twice as high as that of the lightning arrester. If the lightning arrester is right at the terminals this could not occur. (*iii*) If L is the inductance of the lead between the two, and IR the residual voltage of the lightning arrester, the voltage incident at the transformer terminal will be

$$V = IR + L\frac{di}{dt}$$

where di/dt is the rate of change of the surge current.

It is possible to provide some separation between the two, where a capacitor is connected at the terminals of the equipment to be protected. This reduces the steepness of the wave and hence the rate di/dt and this also reduces the stress distribution over the winding of the equipment.

There are three classes of lightning arresters available:

(*i*) **Station type:** The voltage ratings of such arresters vary from 3 kV to 312 kV and are designed to discharge currents not less than 100,000 amps. They are used for the protection of substation and power transformers.

(*ii*) **Line type:** The voltage ratings vary from 20 kV to 73 kV and can discharge currents between 65,000 amps and 100,000 amps. They are used for the protection of distribution transformers, small power transformers and sometimes small substations.

(*iii*) **Distribution type:** The voltage ratings vary from 8 kV to 15 kV and can discharge currents up to 65,000 amperes. They are used mainly for pole mounted substation for the protection of distribution transformers up to and including the 15 kV classification.

Rating of Lightning Arrester: A lightning arrester is expected to discharge surge currents of very large magnitude, thousands of amperes, but since the time is very short in terms of microseconds, the energy that is dissipated through the lightning arrester is small compared with what it would have been if a few amperes of power frequency current had been flown for a few cycles. Therefore, the main considerations in selecting the rating of a lightning arrester is the line-to-ground dynamic voltage to which the arrester may be subjected for any condition of system operation. An allowance of 5% is normally assumed, to take into account the light operating condition under no load at the far end of the line due to Ferranti effect and the sudden loss of load on water wheel generators. This means an arrester of 105% is used on a system where the line to ground voltage may reach line-to-line value during line-to-ground fault condition.

The overvoltages on a system as is discussed earlier depend upon the neutral grounding condition which is determined by the parameters of the system. We recall that a system is said to be solidly grounded only if

$$\frac{R_0}{X_1} \leq 1$$

and

$$\frac{X_0}{X_1} \leq 3$$

and under this condition the line-to-ground voltage during a L-G fault does not exceed 80% of the L-L voltage and, therefore, an arrester of (80% + 0.05 × 80%) = 1.05 × 80% = 84% is required. This is the extreme situation in case of solidly grounded system. In the same system the voltage may be less than 80%; say it may be 75%. In that case the rating of the lightning arrester will be 1.05 × 75% = 78.75%. The overvoltages can actually be obtained with the help of precalculated curves. One set of curves corresponding to a particular system is given in Fig. 16.14.

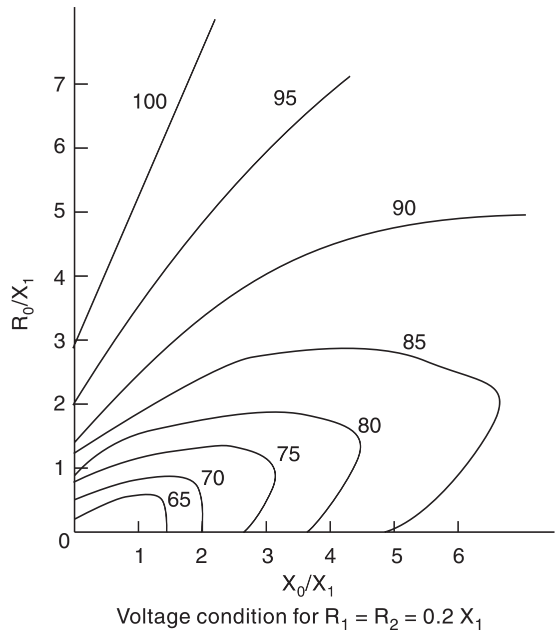

Fig. 16.14 Maximum line-to-ground voltage at fault location for grounded neutral system under any fault condition.

For system grounded through Peterson coil, the overvoltages may be 100% if it is properly tuned and, therefore, it is customary to apply an arrester of 105% for such systems. Even though there is a risk of overvoltages becoming more than 100% if it is not properly tuned, but it is generally not feasible to select arresters of sufficiently high rating to eliminate all risks of arrester damage due to these reasons. The voltage rating of the arrester, therefore, ranges between 75% to 105% depending upon the neutral grounding condition.

So far we have discussed the non-shielding method. We now discuss the shielding method *i.e.*, the use of ground wires for the protection of transmission lines against direct lightning strokes.

16.3 GROUND WIRES

The ground wire is a conductor running parallel to the power conductors of the transmission line and is placed at the top of the tower structure supporting the power conductors (Fig. 16.15 (*a*)). For horizontal configuration of the power line conductors, there are two ground wires to provide effective shielding to power conductors from direct lightning stroke whereas in vertical configuration there is one ground wire. The ground wire is made of galvanized steel wire or in the modern high voltage transmission lines ACSR conductor of the same size as the power conductor is used. The material and size of the conductor are more from mechanical consideration rather than electrical. A reduction in the effective ground resistance can be achieved by other relatively simpler and cheaper means. The ground wire serves the following purposes: (*i*) It shields the power conductors from direct lightning strokes. (*ii*) Whenever a lightning stroke falls on the tower, the ground wires on both sides of the tower provide parallel paths for the stroke, thereby the effective impedance (surge impedance) is reduced and the tower top potential is relatively less. (*iii*) There is electric and magnetic coupling between the ground wire and the power conductors, thereby the changes of insulation failure are reduced.

Protective angle of the ground wire is defined as the angle between the vertical line passing through the ground wire and the line passing through the outermost power conductor (Fig. 16.15 (*a*)) and the protective zone is the zone which is a cone with apex at the location of the ground wire and surface generated by line passing through the outermost conductor. According to Lacey, a ground wire provides adequate shielding to any power conductor that lies below a quarter circle drawn with its centre at the height of ground wire and with its radius equal to the height of the ground wire above the ground. If two or more ground wires are used, the protective zone between the two adjacent wires can be taken as a semicircle having as its diameter a line connecting the two ground wires (Fig. 16.15 (*b*)). The field experience alongwith laboratory investigation has proved that the protective angle should be almost 30° on plain areas whereas the angle decreases on hilly areas by an amount equal to the slope of the hill.

The voltage to which a transmission tower is raised when a lightning strikes the tower, is independent of the operating voltage of the system and hence the design of transmission line against lightning for a desired performance is independent of the operating voltage. The

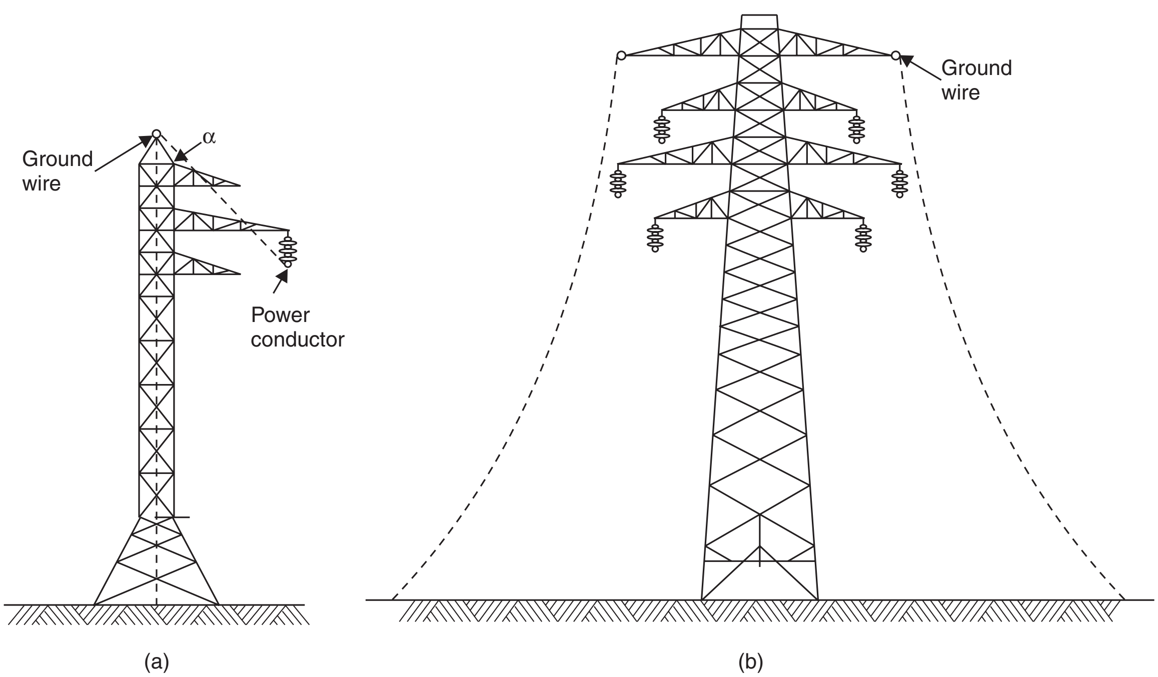

Fig. 16.15 (*a*) Protective angle; (*b*) Protection afforded by two ground wires.

basic requirement for the design of a line based on direct stroke are: (*i*) The ground wires used for shielding the line should be mechanically strong and should be so located that they provide sufficient shield. (*ii*) There should be sufficient clearance between the power conductors themselves and between the power conductors and the ground or the tower structure for a particular operating voltage. (*iii*) The tower footing resistance should be as low as can be justified economically.

To meet the first point the ground wire as is said earlier is made of galvanized steel wire or ACSR wire and the protective angle decides the location of the ground wire for effective shielding. The second factor, *i.e.*, adequate clearance between conductor and tower structure is obtained by designing a suitable length of cross arm such that when a string is given a swing of 30° towards the tower structure the air gap between the power conductor and tower structure should be good enough to withstand the switching voltage expected on the system, normally four times the line-to-ground voltage (Fig. 16.16).

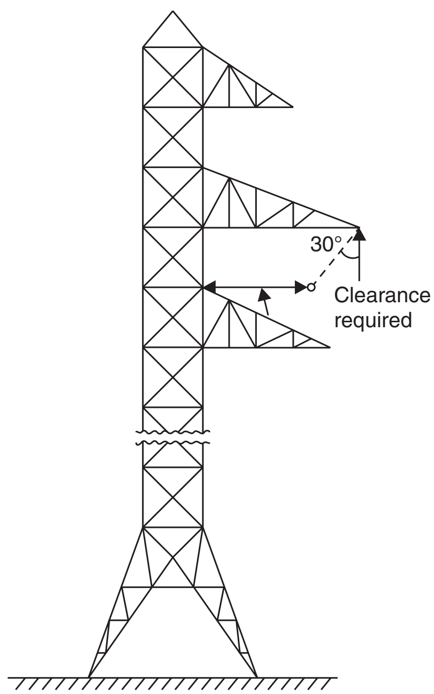

Fig. 16.16 Clearance determination or cross arm length determination.

The clearances between the conductors also should be adjusted by adjusting the sag so that the mid span flashovers are avoided.

The third requirement is to have a low tower footing resistance economically feasible. The standard value of this resistance acceptable is approximately 10 ohms for 66 kV lines and increases with the operating voltage. For 400 kV it is approx. 80 ohms. The tower footing resistance is the value of the footing resistance when measured at 50 Hz. The line performance with regard to lightning depends upon the impulse value of the resistance which is a function of the soil resistivity, critical breakdown gradient of the soil, length and type of driven grounds or counterpoises and the magnitude of the surge current. If the construction of the tower does not give a suitable value of the footing resistance, following methods are adopted.

One possibility could be the chemical treatment of the soil. This method is not practically possible because of the long length of the lines and because this method needs regular check up about the soil conditions. It is not possible to check up the soil conditions at each and every tower of the line which runs in several miles. Therefore, this method is used more for improving the grounds of the substation.

The methods normally used for improving the grounds of transmission towers are the use of (*i*) ground rods, and (*ii*) counterpoises.

Ground Rods

Ground rods are used to reduce the tower footing resistance. These are put into the ground surrounding the tower structure. Fig. 16.17 shows the variation of ground resistance with the length and thickness of the ground rods used. It is seen that the size (thickness) of the rod does not play a major role in reducing the ground resistance as does the length of the rod. Therefore, it is better to use thin but long rods or many small rods.

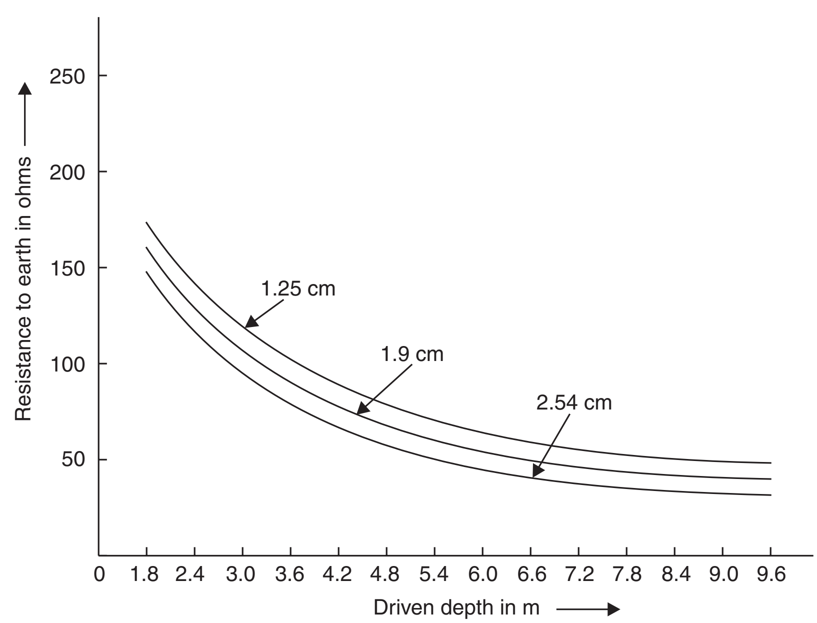

Fig. 16.17 Ground rod resistance as a function of rod length.

Counterpoise

A counterpoise is galvanized steel wire run in parallel or radial or a combination of the two, with respect to the overhead line. The various configurations used are shown in Fig. 16.18.

The corners of the squares indicate the location of the tower legs. The lightning stroke as is incident on the tower, discharges to the ground through the tower and then through the counterpoises. It is the surge impedance of the counterpoises which is important initially and once the surge has travelled over the counterpoise it is the leakage resistance of the counterpoise that is effective. While selecting a suitable counterpoise it is necessary to see that the leakage resistance of the counterpoise should always be smaller than the surge impedance; otherwise, positive reflections of the surge will take place and hence instead of lowering the potential of the tower (by the use of counterpoise) is will be raised.

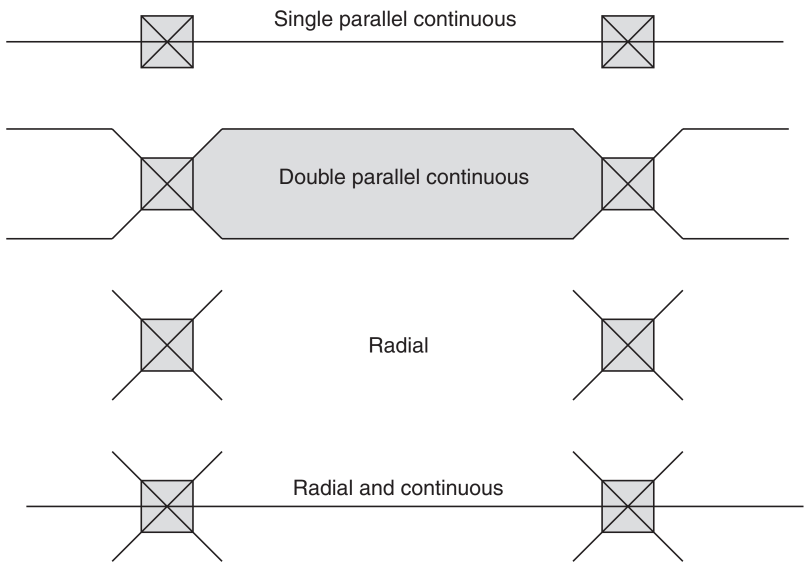

Fig. 16.18 Arrangement of counterpoise.

The leakage resistance of the counterpoise depends upon the surface area, *i.e.*, whether we have one long continuous counterpoise say 1000 m or four smaller counterpoises of 250 m each, as far as the leakage resistance is concerned it is same, whereas the surge impedance of say 1000 m if it is 200 ohms, then it will be 200/4, if there are four counterpoises of 250 m. each, as these four wires will now be connected in parallel. Also if the surge takes say 6 micro-seconds to travel a distance of 1000 m to reduce the surge impedance to leakage impedance, with four of 250 m, it will take 1.5. μ sec, that is, the surge will be discharged to ground faster, the shorter the length of the ground wire. It is, therefore, desirable to have many short counterpoises instead of one long counterpoise. But we should not have too many short counterpoises, otherwise the surge impedance will become smaller than the leakage resistance (which is fixed for a counterpoise) and positive reflections will occur.

The question arises as to why we should have a low value of tower footing resistance. It is clear that, whenever a lightning strikes a power line, a current is injected into the power system. The voltage to which the system will be raised depends upon what impedances the current encounters. Say if the lightning stroke strikes a tower, the potential of the tower will depend upon the impedance of the tower. If it is high, the potential of the tower will also be high which will result in flashover of the insulator discs and result in a line-to-ground fault. The flashover will take place from the tower structure to the power conductor and, therefore, it is known as back flashover,

Surge absorbers: A surge absorber is a device which absorbs energy contained in a travelling wave. Corona is a means of absorbing energy in the form of corona loss. A short length of cable between the equipment and the overhead line absorbs energy in the travelling wave because of its high capacitance and low inductance. Another method of absorbing energy is the use of Ferranti surge absorber which consists of an air core inductor connected in series with the line and surrounded by an earthed metallic sheet called a dissipator. The dissipator is insulated from the inductor by the air as shown in Fig. 16.19.

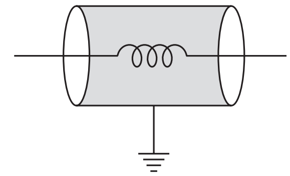

Fig. 16.19 Ferranti surge absorber.

The surge absorber acts like an air cored transformer whose primary is the low inductance inductor and the dissipator acts as the single turn short circuit secondary. Whenever the travelling wave is incident on the surge absorber a part of the energy contained in the wave is dissipated as heat due to transformer action and by eddy currents. Because of the series inductance, the steepness of the wave also is reduced. It is claimed that the stress in the end turns is reduced by 15% with the help of surge absorber.

16.4 SURGE PROTECTION OF ROTATING MACHINE

A rotating machine is less exposed to lightning surge as compared to transformers. Because of the limited space available, the insulation on the windings of rotating machines is kept to a minimum. The main difference between the winding of rotating machine and transformer is that in case of rotating machines the turns are fewer but longer and are deeply buried in the stator slots. Surge impedance of rotating machines in approx. 1000 Ω and since the inductance and capacitance of the windings are large as compared to the overhead lines the velocity of propagation is lower than on the lines. For a typical machine it is 15 to 20 metres/ μ sec. This means that in case of surges with steep fronts, the voltage will be distributed or concentrated at the first few turns. Since the insulation is not immersed in oil, its impulse ratio is approx. unity whereas that of the transformer is more than 2.0.

The rotating machine should be protected against major and minor insulations. By major insulation is meant the insulation between winding and the frame and minor insulation means inter-turn insulation.

The major insulation is normally determined by the expected line-to-ground voltage across the terminal of the machine whereas the minor insulation is determined by the rate of rise of the voltage. Therefore, in order to protect the rotating machine against surges requires limiting the surge voltage magnitude at the machine terminals and sloping the wave front of the incoming surge. To protect the major insulation a special lightning arrester is connected at the terminal of the machine and to protect the minor insulation a condenser of suitable rating is connected at the terminals of the machine as shown in Fig. 16.20.

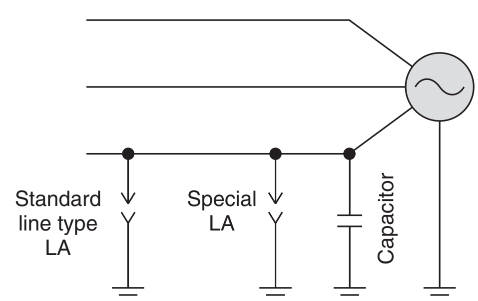

Fig. 16.20 Surge protection of rotating machine.

PROBLEMS

16.1. What are volt-time curves ? What is their significance in power system studies ?

16.2. What are BILS ? Explain their significance in power system studies.

16.3. Describe the construction, principle of operation and applications of (*i*) Rod gaps; (*ii*) Expulsion gap; and (*iii*) Valve type lightning arrester.

16.4. Compare the relative performances of the following: (*i*) Rod gap; (*ii*) Expulsion gap; and (*iii*) Valve type L.A.

16.5. Explain clearly how the rating of a lightning arrester is selected. What is the best location of a lightning arrester and why ?

16.6. What is tower-footing resistance ? What are the methods to reduce this resistance ? Why is it required to have this resistance as low as economically feasible ?

16.7. What are ground rods and counterpoises ? Explain clearly how these can be used to improve the grounding conditions. Give various arrangements of counterpoise.

16.8. "The leakage resistance of a counterpoise should be lower than its surge impedance." Why ?

16.9. What is a ground wire ? Discuss its location with respect to power conductors.

16.10. What are the requirements of a ground wire for protecting power conductors against direct lightning stroke ? Explain how they are achieved in practice.

16.11. Explain the principle of operation of Ferranti surge absorber.

16.12. What are the basic requirements of a lightning arrester ? Differentiate between (*i*) a lightning arrester and a lightning conductor, and (*ii*) a surge diverter and a surge absorber.

16.13. Explain clearly how a lightning arrester is selected for protecting a power transformer.

16.14. Give a scheme of protecting a rotating machine against overvoltages. Explain clearly how the scheme is different from protecting a power transformer.

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20

Overvoltage Phenomena

We have examined the mechanisms of switching surges and other overvoltage phenomena in the previous chapters. In this chapter, we try to survey generally the various kinds of overvoltage behaviour including power frequency phenomena as well as switching surges and lightning surges. These determine the essential prerequisite conditions for coordinating the insulation of power system networks.

20.1 Classification of Overvoltage Phenomena

Table 20.1 lists our classification of overvoltages. We examine several different overvoltages along with this classification. The phenomena listed in item 2a, for example, may not usually be discussed because they seldom occur. However, engineers need to study the mechanism and to assure the absence of this sort of problem.

20.2 Fundamental (Power) Frequency Overvoltages (Non-resonant Phenomena)

Phenomena for four different power frequency overvoltages are listed below.

20.2.1 Ferranti effect

In the power system of Figure 20.1a, the vector diagrams for the cases of lagging and leading power factors are shown in Figures 20.1b and c. In the leading power-factor operation, the receiving terminal voltage v_r becomes larger than the sending terminal voltage v_s (i.e. $v_s < v_r$). The phenomenon in which v_r becomes larger than v_s is called the **Ferranti effect**. In fact, we have already discussed much of this in regard to leading power-factor load operation in Section 16.3.

20.2.1.1 Overvoltage by transmission line charging

The utmost case of pure capacitive load is transmission line charging from one terminal, whose vector diagram is shown in Figure 20.1d.

Handbook of Power System Engineering Yoshihide Hase

Table 20.1 Overvoltage phenomena

1. Fundamental (power) frequency overvoltages (temporary overvoltages)
   1a. Ferranti effect
   1b. Self-excitation of generator
   1c. Overvoltages of unfaulted phases by one line-to-ground fault
   1d. Sudden load tripping or load failure
2. Lower frequency harmonic resonant overvoltages
   2a. Broad area resonant overvoltages (lower order frequency resonance)
   2b. Local area resonant overvoltages
3. Switching surge
   3a. Breaker closing overvoltages
   3b. Breaker tripping overvoltages
   3c. Switching surges by line switches
4. Overvoltage phenomena by lightning strikes
   4a. Direct stroke to phase conductors (direct flashover)
   4b. Direct stroke to overhead grounding wire or to tower structure (inverse flashover)
   4c. Induced strokes (electrostatic induced strokes, electromagnetic induced strokes)
5. Overvoltages caused by abnormal conditions
   5a. Interrupted ground fault of cable (in high-impedance neutral grounding system)
   5b. Overvoltages induced on cable sheath (see Chapter 23)
   5c. Touching of different kilovolt lines etc.

We calculate the overvoltage when the line is charged from sending terminal point s (the receiving terminal r is open). The four-terminal circuit equation between points s and r is given by Equation 18.19b. Now, putting $I_r = 0$ in the equation, we have

$$\left.\begin{aligned} &\frac{V_r(x)}{V_s(x)} = \frac{1}{\cosh \gamma(s) \cdot x} \qquad \text{(ratio of overvoltages at the receiving terminal)} \\ &\text{where } \gamma(s) = \sqrt{(Ls+R)(Cs+G)} \end{aligned}\right\} \tag{20.1}$$

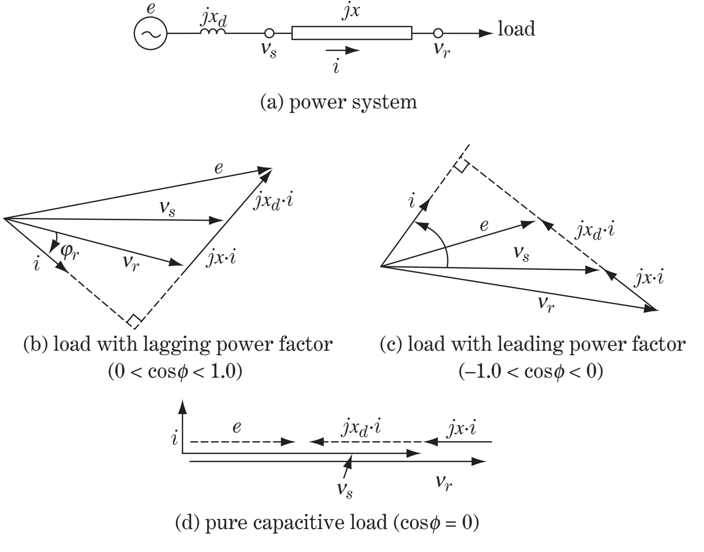


Figure 20.1 Vector diagrams under different power-factor loads

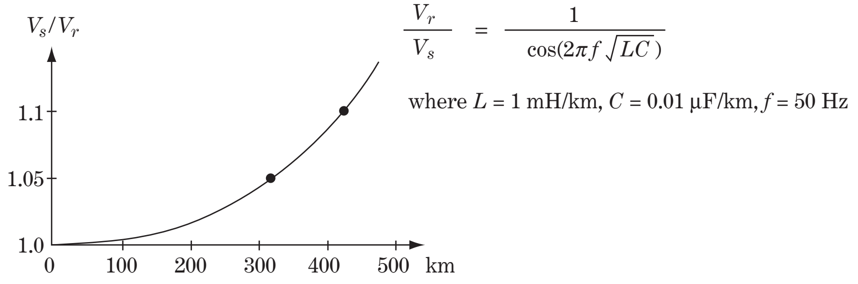


Figure 20.2 Ferranti effect, ratio of voltage at receiving end

Neglecting R and G, and letting $s \to j\omega$ (because fundamental phenomena are investigated here),

$$\left.\begin{aligned} \cosh\gamma(s)\cdot x &= \cosh\left\{j\omega\sqrt{LC}\cdot x\right\} = \frac{1}{2}\left\{e^{j\omega\sqrt{LC}x} + e^{-j\omega\sqrt{LC}x}\right\} \\ &= \cos\left(\omega x\cdot\sqrt{LC}\right) \\ \therefore\ \frac{V_r}{V_s} &= \frac{1}{\cos\left(\omega x\cdot\sqrt{LC}\right)} \geqq 1.0 \end{aligned}\right\} \quad (20.2)$$

The equation is the rate of overvoltage caused at point r in comparison with the voltage at point s.

Figure 20.2 shows the calculated result of the equation under the condition of typical line constants $L = 1$ mH/km, $C = 0.01$ μF/km, 50 Hz. The rate of overvoltage at point r is 1.05 for line length $l = 312$ km, and 1.10 for $l = 432$ km. In the case of n parallel circuits, the constants are replaced as $L \to 1/n\cdot L$, $C \to n\cdot C$ so that $\sqrt{LC}$ and the rate V_r/V_s are not affected.

As we discussed in Chapter 18, the overvoltages by the Ferranti effect under relatively light load conditions might be rather severe, especially at a receiving substation in a big-city area where numbers of cable feeders are connected. An effective countermeasure to prevent excess overvoltage is to install reactor banks. ***V–Q* control** equipment based on the control of **reactor banks** and **on-load tap-changing transformers** is often adopted at key substations as another effective countermeasure to prevent excess overvoltages or to maintain stable voltages for 24 h.

20.2.2 Self-excitation of a generator

If a capacitive load is connected to a generator as shown in Figure 20.3a, overvoltage will be caused on the generator terminal even if E_{fd} is kept at zero. This phenomenon is called the **self-excitation phenomenon of a generator**.

We refer to Figure 16.8 in Chapter 16 in order to understand this phenomenon. Under leading power-factor operation, large terminal voltage e_G is apt to arise in spite of the small excitation jE_{fd}, as seen in Figure 16.8c.

Now we refer to Figure 20.3b, and examine the behaviour of a generator under the leading power-factor condition.

The generator is driven by the prime-mover under the open-terminal condition and is running at constant speed with excitation current zero ($jx_{ad}\cdot i_{fd} = E_f = 0$, in Equation 10.60 and Figure 10.5). However, small residual magnetism remains as a magnetic field of the rotor since termination of the last operation, even if the excitation current is zero. A small terminal voltage e_G also remains. If a very small capacitance C is connected to the generator under this condition, the same terminal voltage e_G will be maintained. Furthermore, if connected capacitance C is enlarged, e_G as well as leading current i in the armature coil will become larger; their characteristics can be written as curve a in Figure 20.3b.

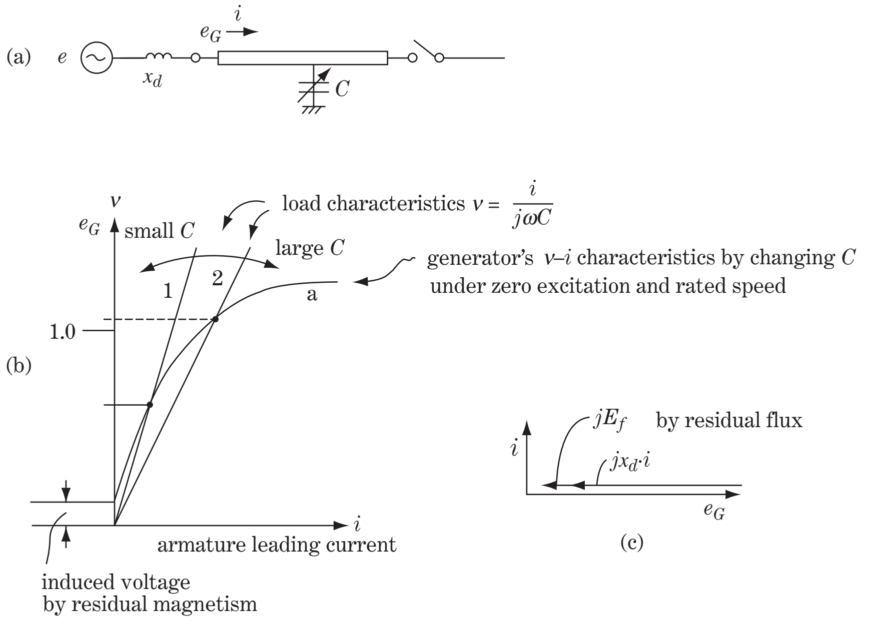


Figure 20.3 Self-excitation of a generator

This is a specific *v-i* characteristic of a generator under capacitive load with excitation current zero. As seen in the vector diagram of Figure 20.3c, current *i* leads the voltage by 90°. The characteristics begin to saturate naturally at around the rated voltage area. On the other hand, specific *v–i* characteristics of the capacitance *C* are given by $v = i/(\omega C)$ as shown in Figure 20.3b. The intersecting point of both characteristics is the actual operating point of the *C* load.

In conclusion, the self-excitation phenomenon of a generator is caused by connection of a pure capacitive load, in that extreme and sustained overvoltage (perhaps the ceiling voltages of the saturated zone) is caused. This condition also means critical overheating of the generator caused by the rapid increase of iron loss as a result of core saturation. Obviously, we need to avoid the self-excitation of generators.

20.2.3 Sudden load tripping or load failure

If some amount of load tripping or load failure occurs at a receiving substation, the voltage would be boosted by the following three reasons:

a) The generator terminal voltage at the sending station may be transiently boosted, so that the voltage at the receiving point also has to be boosted. Referring to Figure 10.5, or Equation 10.63, if the generator current i_{a1} suddenly decreases, the terminal voltage vector $e_{a1} = (E_{a1}e^{j\alpha_1})$ has to change unavoidably to a large vector closer to jE_f in magnitude and angle. In other words, the generator terminal voltage would be transiently boosted by the sudden decrease of load current, until the AVR decreased the excitation jE_f.

b) The impedance drop voltage of the transmission line would be decreased.

c) The total power factor of the load may be shifted a little in the leading power-factor direction so that the Ferranti effect only partially occurs.

20.2.4 Overvoltages of unfaulted phases by one line-to-ground fault

We have already studied the principles of this matter in Chapter 8, Section 8.2 and Table 8.2.

Referring to Figure 8.1 as well as Figure 21.1, overvoltage of unfaulted phases would become $0.8\,E$-$1.3\,E$ for the solidly neutral grounding system ($\delta = 0$-1) and $1.5\,E$-$1.9\,E$ for the non-effective neutral grounding system ($\delta \doteqdot 5$ to ∞). Figure 8.1 indicates the overvoltage ratio of the power frequency term, and the transient overvoltage ratio would become a little higher. (Transient overvoltage ratio can be derived in a similar curve format to that in Figure 8.1.)

Although the overvoltage ratio of unfaulted phases is quite large, every part of the network (including installed surge arresters) has to have enough insulation to withstand this overvoltage. Accordingly, this temporary overvoltage phenomenon is one of the important factors for the fundamental design of **insulation coordination**, which we discuss in detail in Chapter 21.

20.3 Lower Frequency Harmonic Resonant Overvoltages

If one inductance L and one capacitance C exist as elements of a circuit, series resonant frequency and parallel resonant frequency inevitably exist. Our concern is with series resonance phenomena of n-th order harmonic voltages and currents of the power system from the viewpoint of overvoltages and waveform distortion, whose condition is given by $Z = j2\pi n fL + 1/(j2\pi fC) \rightarrow 0$ (f: resonant frequency). We need to consider if such a series resonant condition could exist actually in our power network even at a low probability. Lower order harmonic phenomena especially (under, say, 1 kHz) may cause problems over a broad area and continue for a long period without attenuation. We need to assure the absence of these sorts of problems. Meanwhile, higher frequency voltages (over, say, 1 kHz) caused by resonant phenomena would not actually exist because such a component would be largely attenuated by corona loss and eddy-current loss of the transmission line even if it did exist.

20.3.1 Broad-area resonant phenomena (lower order frequency resonance)

Figure 20.4a is a simplified model connecting a generating plant and urban area with some cable lines. If we represent the overhead line by L and the cable lines by C, the circuit can be simply written as an LC series-connected circuit as shown in Figure 20.4b, although the load circuit is usually connected. We have already studied the Ferranti effect of these circuits as phenomena of power frequency. Nevertheless, we need to check the possibility of series resonance in other frequency zones under different conditions such as nominal load condition, or irregular conditions (phase fault, grounding fault, phase opening, reclosing time, etc.). At least we need to prove that such resonant phenomena would not occur during actual system operation.

20.3.1.1 Positive-sequence series resonance

We try to find a series resonance condition which may exist by chance in our power system. Let us assume the line constants below for the positive-sequence circuit of Figure 20.4b:

$$\left.\begin{array}{ll}\text{Overhead line}: 0.01\,\text{mH/km},\ l_1(\text{km}) & L = 10^{-5}\cdot l_1 \quad [\text{H}] \\ \text{Cable lines}: 0.33\,\mu F/\text{km},\ l_2(\text{km}),\ \text{numbers of circuits}: m, & C = 0.33\times 10^{-6}\cdot l_2\cdot m \quad [\text{F}]\end{array}\right\} \tag{20.3}$$

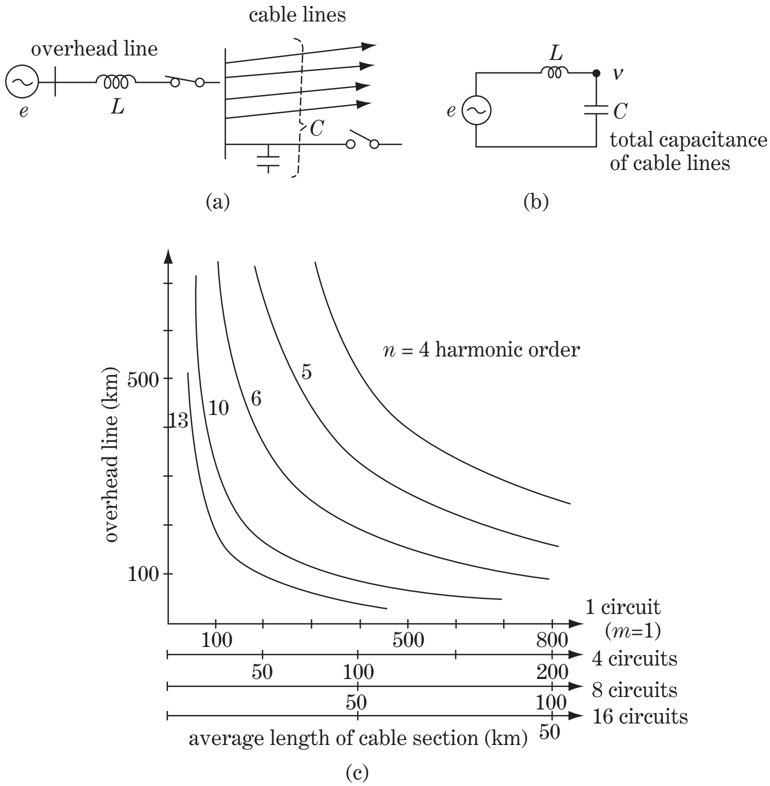


Figure 20.4 Series resonance of a power system with cable lines (positive-sequence resonance)

Assuming a no-load condition, the condition of *LC* series resonance for the *n*th-order higher harmonic components is

$$\left.\begin{aligned} & j2\pi n f_0 L - j\frac{1}{2\pi n f_0 C} = 0 \\ & \text{Then} \\ & (2\pi n f_0)^2 LC = (314n)^2 LC = 1.0 \\ & \therefore \quad 10^5 n^2 \cdot LC \fallingdotseq 1.0 \\ & \text{where } f_0 = 50\,\text{Hz} \end{aligned}\right\} \tag{20.4a}$$

Substituting 20.3 into 20.4 the resonant condition of the positive-sequence circuit is

$$n^2 \cdot l_1(l_2 m) \fallingdotseq 3 \times 10^6 \tag{20.4b}$$

Figure 20.4c is the series resonant condition derived from 20.4b. This figure gives us a rough idea of the existing zone of series resonance for the model system. Assuming that the length of the overhead line $l_1 = 200$ km and a total length of cable $l_2 = 400$ km or less, for example, resonant conditions do not exist fortunately for the lower frequency of $n = 2$–6.

Although the curves were derived under a somewhat unrealistic no-load condition, a similar condition may be caused by a sudden bus fault (all the connected breakers have to be tripped) in the key substation.

Recall that only the existing zone of resonant frequency in the system was checked in the above study, from which it may be concluded that natural resonance would seldom be caused in the system. However, we need to recognize that actual power system networks include not only generators of almost ideal sinusoidal source voltages, but also various loads and power conditioners which generate large rippled currents. If the frequency of the harmonic components of such a ‘dirty forced current’ matches the network natural frequency, abnormal overvoltages or waveform distortion could be significantly amplified in some local areas of the network. As a matter of fact, we have to recognize that harmonic currents of various frequency are apt to flow forcibly into the power system, so resonance phenomena have to be checked from the viewpoint of waveform distortion as well as overvoltage phenomena. (Waveform distortion is examined again in Chapter 22.)

20.3.1.2 Series resonance under temporary conditions (faults, phase opening, reclosing time, etc.)

We investigate the possibility of resonance under some irregular conditions including line-to-line fault modes, line-to-ground fault modes and phase opening modes (including dead-voltage time for faulting phase reclosing).

Referring to Tables 3.1 and 3.2 in Chapter 3 and Tables 6.1 and 6.2 in Chapter 6, we need to check if these conditions exist, that is the denominators of the equations in the tables would tend to values close to zero by chance.

For the modes of series resonance

$$\left.\begin{array}{lll} \text{normal condition}: & & Z_1 \to 0 \\ 2\phi G: & Z_1 + Z_0 \to 0, & Z_1 + 2Z_0 \to 0 \\ 1\phi G: & & 2Z_1 + Z_0 \to 0 \\ 1\phi \text{ opening}: & & Z_1 + Z_0 \to 0 \\ 2\phi \text{ opening}: & & Z_1 + 2Z_0 \to 0 \end{array}\right\} \qquad (20.5)$$

Perhaps we need not worry about the possibility of resonance caused by line-to-line and line-to-ground faults, because the resonance condition may be unlikely to occur by analogy to Figure 20.4c. Furthermore, these faults would be cleared within a few cycles, generally by the associated protective relays. However, phase-opening mode trouble may cause a little bother, because the protective relays may not be able to detect the phase open-mode faults exactly and would be able to continue without noticing.

In conclusion, it is worthwhile assuring the absence of these sorts of resonant conditions at several different points in the network.

20.3.2 Local area resonant phenomena

20.3.2.1 Transformer winding resonant oscillation triggered by switching oscillatory surge

In Figure 20.5, the transformer is going to be energized through the cable line. This circuit can be simplified to the parallel circuit of C by the cable and L by the excitation reactance of the transformer,

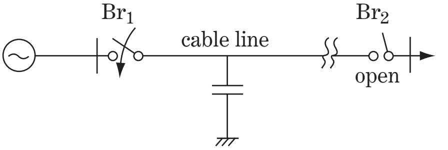


Figure 20.5 Resonance caused by transformer excitation

so that overvoltage would not be caused when the breaker Br_1 is closed. However, the resulting oscillatory inrush voltage and current could trigger a unique voltage oscillation in the transformer windings by chance.

Let us assume a cable line of 275 kV, 2000 sq (given in Table 2.3: 0.392 mH/km, $C = 0.25\mu F/km$, velocity $100\,m/\mu s$) and length 10 km.

The travelling time of the surge from the breaker Br to the transformer is 100 μs, so the oscillatory frequency of the switching surge is 5 kHz. The transient oscillation of this frequency zone (10th or lower order) will not be attenuated so quickly. Therefore a reasonable countermeasure is required at the transformer engineering side to avoid winding coil resonance by the frequency of the order above. This scheme is discussed again in Section 21.6.

Note that the **in-rush current of the transformer** is, regardless of the condition of the feeding line, an extremely waveform-distorted offset current which would continue for more than 10 seconds as shown in Figure 16.10. As an inrush current contains d.c. and lower order harmonics of slow attenuation, special consideration would generally be required in the practical engineering of transformers, breakers, generators, protective relays, etc.

20.3.2.2 Ferro-resonance caused by core saturation

Figure 20.6 explains the **ferro-resonance phenomenon**. As seen in diagram (c), if the operating voltage exceeds 1.1 pu of the rated voltage, it begins to saturate rapidly and the excitation current i greatly increases. In other words, the excitation impedance $Z_l (= jX_l)$ of the transformer has non-linear characteristics which rapidly decrease in value in the saturated zone. Figures 20.6a and b show the well-known 'series ferro-resonance circuit' and 'parallel ferro-resonance circuit' respectively. Both circuits have unique behaviour (although the explanation is omitted in this book) and we need to prevent its occurrence. The parallel ferro-resonance (b) may arise especially for a circuit with a large cable capacitance, while the series ferro-resonance (a) may arise especially for the irregular cases of unbalanced phase opening or lines with a series capacitor.

Ferro-resonance causes unstable, jumping overvoltage phenomena and, furthermore, extreme flux saturation leading to a severe temperature rise of the flux pass, all of which need to be avoided.

If the transformer excitation voltage v_l is increased a little beyond point a, the excitation current i_l is greatly increased because of the generator's non-linear v–i characteristics, which means that the excitation reactance Z_L would become quite a smaller unstable value

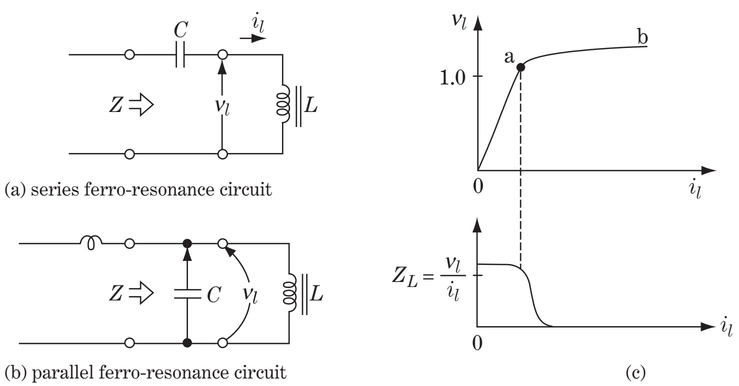


Figure 20.6 Ferro-resonance phenomena

20.3.3 Interrupted ground fault of cable line in a neutral ungrounded distribution system

This is a unique feature which would be limited to the distribution cable lines of a neutral ungrounded system (see Table 8.1). This system is a widely adopted one for distribution networks of 3–20 kV, whose advantage is that the grounding fault current for one phase to ground fault ($1\phi G$) can be greatly reduced to a value of only, say, 10 mA to 100 mA, that is 'zero amps'. With this system, noise interference on the communication lines can also be reduced because the zero-sequence circuit is actually open.

However, there is one disadvantage of this practice, namely the **interrupted ground fault of the cable lines**.

The solid insulation of a distribution cable may slowly deteriorate over a long period of operation. We assume that a minute pinhole crack appears in the cable insulation after this period of operation. A very small $1\phi G$ current flows through the pinhole across the insulation layer (called 'minute grounding' of perhaps 10 mA). However, this grounding current would be halted immediately and the insulation of the pinhole would soon be recovered, because the pinhole pass is not adversely affected by such a small current through the pinhole. The pinhole grounding would probably occur at a timing around the peak value of sinusoidal a.c. voltage, and would always be halted at the time of 'voltage zero'. Incidentally, the minute grounding fault current is almost of 90° lagging power factor, so halting the pinhole grounding current at the time of voltage zero gives rise to 'current chopping' phenomena, which cause large needle-shaped high-frequency transient overvoltages by the same mechanism explained in Section 19.4.

If such processes are caused once at an arbitrary point of the cable system, the same processes would be intermittently repeated at the same point over time so that deterioration of the pinhole would be slowly accelerated and finally lead to a permanent breakdown fault. Moreover, the above interrupted ground fault at one pinhole point intermittently generates needle-like high-frequency overvoltages, which would deteriorate the insulation of another part of the cable. Finally, all the parts of the cable line within a limited area would be frequently stressed by the high-frequency overvoltages caused at many pinhole points, and then cable deterioration would accelerate quickly and broadly.

Due to the excellent insulation technology of recent CV cables for distribution lines, interrupted ground faults may seldom occur today. However, such phenomena could occur on a neutral ungrounded system at any time and at any part, and furthermore may be initialized without any indication or notice.

Continuous observation or pre-detection of such overvoltages on a commercially operated system is perhaps almost impossible, so such phenomena, if they were to happen, would probably be noticed only after the cable lines have been damaged or seriously deteriorated.

20.4 Switching Surges

The operation of circuit-breaker and line switch opening (tripping) or closing inevitably causes severe switching surges. It is fair to say that such phenomena may be even more difficult to manage than lightning surges from some practical viewpoints. In order to understand the precise characteristics, we commence our discussion with a comparison of the characteristics with those of lightning surges.

Lightning surges are severe phenomena in nature. However, the following characteristics may mitigate the severity to some extent in comparison with switching surges:

Lightning strike

a) Rare occurrence.

b) Occurrence mostly on overhead transmission lines whose structures are made of metal (conductors, tower structures, arcing horns, etc.) and porcelain insulators, so that fault self-recovery characteristics are inherent (except a direct strike on a substation).

c) Very short surge duration of typical waveform $1.2 \times 50\,\mu s$.

d) Single action phenomena (mostly).

e) Attenuation through line travelling to substations expected in most cases.

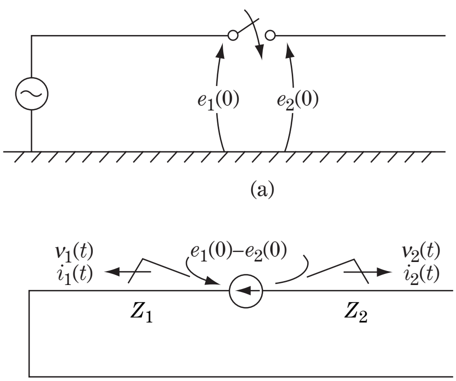
f) Upper limit of incidental voltage level (can be controlled by arcing horns).

g) Relatively easy protection by advanced arresters installed at substations.

The characteristics of switching surges (SS for short) may be explained conversely. SS appear whenever any one of several breakers or line switches at the same substation are operated. SS occur in the vicinity of apparatus with no self-recovery insulation (generators, transformers, breakers, cables, etc.) in the same substation. SS are oscillatory transient surge of extremely high frequency with sharp waveforms and continuing for a long duration before attenuation or extinction. SS are often caused whenever switching operation is conducted. They may seldom attenuate before reaching weak insulation points at the substation because of the short travelling distance. Arresters may not be able to protect the weak insulation points at the substation because of the **relative distance of installed arresters** from the surge-generating breakers or line switches and the other equipment to be protected. Furthermore, SS would cause thermal heating of arrester–non-linear resistive elements and may even break the installed arresters by large switching energy.

20.4.1 Overvoltages caused by breaker closing (breaker closing surge)

We have already studied the mechanisms caused by breaker closing surges in Sections 19.5 and 19.6. In Figure 20.7, the transient phenomenon caused by the breaker closing means sudden insertion of the initial voltage $\{e_1(0)-e_2(0)\}\mathbf{1}(0)$ with opposite polarity across the breaker contacts. Accordingly the initially resulting voltage and current travelling waves are proportional to the initial voltage $\{e_1(0)-e_2(0)\}$ as shown by the equations in the Figure 20.7b. Successive phenomena will be affected by the circuit conditions, especially the included transition points.



$$v_1(t) = Z_1 \cdot i_1(t) = \frac{Z_1}{Z_1 + Z_2} \cdot \{e_1(0)-e_2(0)\}$$

$$v_2(t) = Z_2 \cdot i_2(t) = \frac{-Z_2}{Z_1 + Z_2} \cdot \{e_1(0)-e_2(0)\}$$

$$i_1(t) = \frac{1}{Z_1 + Z_2} \cdot \{e_1(0) - e_2(0)\}$$

$$i_2(t) = \frac{-1}{Z_1 + Z_2} \cdot \{e_1(0) - e_2(0)\}$$

Figure 20.7 Breaker closing surge

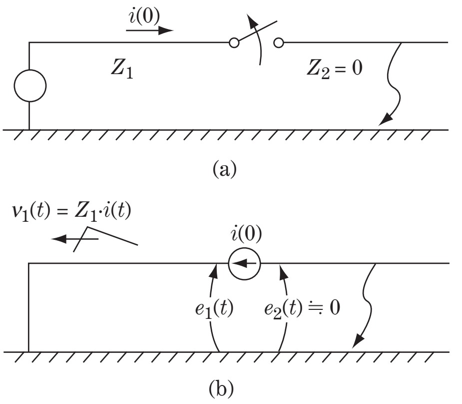


Figure 20.8 Breaker tripping surge

20.4.2 Overvoltages caused by breaker tripping (breaker tripping surge)

We have already studied the mechanisms of breaker tripping in Chapter 19 in detail and so only recall them here briefly. A typical case of fault current tripping is shown in Figure 20.8, where the initial surge voltage caused by breaker tripping can be calculated by the equations in diagram (b). The breaker's reignition surges also have to be taken into account. We already know that the breaker's restriking should be avoided not only to prevent breaker failure, but also to avoid the extremely serious overvoltages which may cause other insulation failures in the network.

20.4.3 Switching surge by line switches

LS switching surge voltages are unique phenomena, as was mentioned in Section 19.7 and shown in Figure 19.18, and insulation coordination in a substation is consequently very important. We discuss this matter in detail in Section 21.3.

20.5 Overvoltage Phenomena by Lightning Strikes

The mechanism of a lightning stroke is typically explained as follows.

When the electric field gradient at some point in a charged concentration of a cloud exceeds the breakdown value for moisture-laden and ionized air, an **electric streamer** darts out towards the ground but may soon halt after progressing perhaps a hundred metres. After a shot interval, the streamer again darts out and repeats the performance. This initial streamer progresses by a series of jumps called a **stepped leader**. Just after the point of the stepped leader reaches the ground, a heavy current (the **main stroke** of 1000–20 000 A) flows up the path blazed by the stepped leader with a velocity about one-tenth that of light.

Our concern in this book is with lightning which directly strikes electricity networks or causes surge voltages and current phenomena on them.

Lightning surge phenomena can be classified into three different stroke modes, which we discuss in turn.

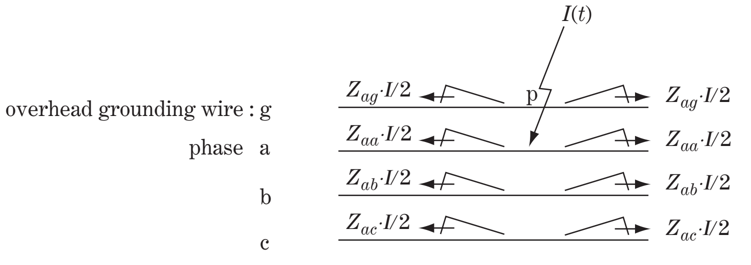


Figure 20.9 Direct lightning stroke to phase a conductor

20.5.1 Direct strike on phase conductors (direct flashover)

This is the case where a lightning stroke directly strikes one or more phase conductors. Figure 20.9 shows the case, in that the main stroke directly hits the phase a conductor and the current $I(t)$ is injected into it. The successive surge phenomena have already been discussed in detail in Sections 18.4 and 18.5.

The surge voltages $v_a = Z_{ag} \cdot (I/2)$, $v_g = Z_{gg} \cdot (I/2)$, etc., in the figure are induced because of the associated surge impedances. The induced surge voltage $v_{ag} = (v_a - v_g)$ would certainly exceed the insulation strength across the phase a conductor and point g (which may be the OGW, the top or arm of a tower, or arcing horn), flashover is caused and a phase a line-to-ground fault occurs. Furthermore, if the induced $v_{ab} (= Z_{ab} \cdot (I/2))$ exceeds the insulation strength across the phase a and b conductors, flashover occurs also between the two conductors causing a line-to-line fault. Of course, whenever a direct stroke occurs, line faults would be caused without exception.

20.5.2 Direct strike on OGW or tower structure (inverse flashover)

This is the case where a lightning stroke directly strikes the OGW or tower structure as shown in Figure 20.10a.

The induced surge voltage $v_{ga} = (v_g - v_a)$ would certainly exceed the insulation strength across point g and the phase a conductor. Flashover would be caused between point g (OGW, the top or arm of a tower, or arcing horn) and the phase a conductor.

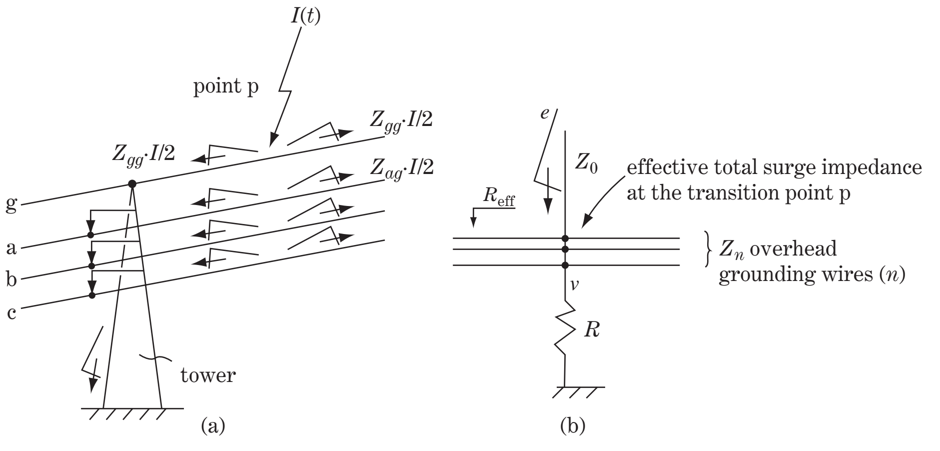


Figure 20.10 Direct stroke to overhead grounding wire or to tower structure

Assuming a lightning $e(t) = I(t) \cdot Z_0$ strikes at the transmission tower top directly as is shown Figure 20.10b, the stroke current $I(t)$ is divided into three passes (the tower and the both sides of the OGW), so that the induced voltage $v(t)$ is calculated as follows

$$v(t) = \frac{Z_g}{Z_0 + Z_g} \cdot e(t) = \frac{1}{\frac{1}{Z_o} + \frac{1}{Z_g}} \cdot \frac{e(t)}{Z_0} \equiv R_{\text{eff}} \cdot I(t) \tag{20.6}$$

where $$R_{\text{eff}} = \frac{1}{\frac{1}{Z_0} + \frac{1}{Z_g}} = \frac{1}{\frac{1}{Z_0} + \left(\frac{2}{Z_n} + \frac{1}{R}\right)}, \quad Z_g = \left(\frac{2}{Z_n} + \frac{1}{R}\right)^{-1}$$

R_{eff} : effective surge impedance

Z_0 : surge impedance of lightning pass through air from clouds

R : surge impedance of tower structure

Z_g : surge impedance of total overhead grounding wires(n)/tower circuit

$Z_n = Z_{gw}/n$, where Z_{gw}, Z_n are the surge impedances of OGW of one and n stripes respectively.

Trial calculation of the induced voltage at the top of the tower or on the OGW

Assuming $R = 300\,\Omega$, $Z_0 = 400\,\Omega$, $Z_n = Z_{gw} = 300\,\Omega$ (where $n = 1$),

Then the effective surge impedance $R_{\text{eff}} = 1/\{1/400 + 2/300 + 1/300\} = 80\,\Omega$.

If we assume $I(t) = 20\,\text{kA}$, the induced initial voltage v_g at the top is $v_g = 20\,\text{kA} \times 80\,\Omega = 1600\,\text{kV}$.

The power frequency voltage is of course superimposed on the above injected surge voltage. In the case when the rated line voltage is 400 kV, the power frequency voltages v_a, v_b, v_c have values of $\pm 400 \times \sqrt{2}/\sqrt{3} = \pm 327\,\text{kV}$ (peak value). Accordingly, the superposed voltage $v_{ga} = 1173 \sim 1927\,\text{kV}(3.6-5.89E)$, so flashover is inevitably caused.

If the number of OGWs is increased from one to n, the surge impedance Z_n is decreased $1/n$ times so that the total surge impedance R_{eff} can also be effectively decreased. Assuming $n = 3$ in the above calculation, then $Z_n = Z_g/3 = 100\,\Omega$, and accordingly $R_{\text{eff}} = 38\,\Omega$. That is, we can reduce the equivalent surge impedance by about half.

20.5.3 Induced strokes (electrostatic induced strokes, electromagnetic induced strokes)

These are called **induced lightning surges,** whose mechanism is shown in Figure 20.11.

Lightning may strike some point on the Earth at a short distance from a transmission line. This phenomenon can be considered as one virtual wire connecting a cloud and an earth point and a surge current $I(t)$ suddenly flowing along the virtual wire. The virtual wire has mutual capacitances and mutual inductances across the line conductors and the OGWs of the neighboured transmission line as a matter of course, so that capacitive induced voltage as well as inductive induced voltage appear on the phase conductors as well as on the OGWs.

20.5.3.1 Capacitive induced lightning surges

In Figure 20.11, a virtual conductor (named a) and one conductor (named b) exist. If the potential voltage of the cloud is $e(t)$, the capacitive induced voltage on conductor b is $v(t) = \{C/(C + C')\} \cdot e(\text{t})$. The insulation may be broken down by the induced voltage and the resulting flashover is named the **capacitive induced lightning stroke**.

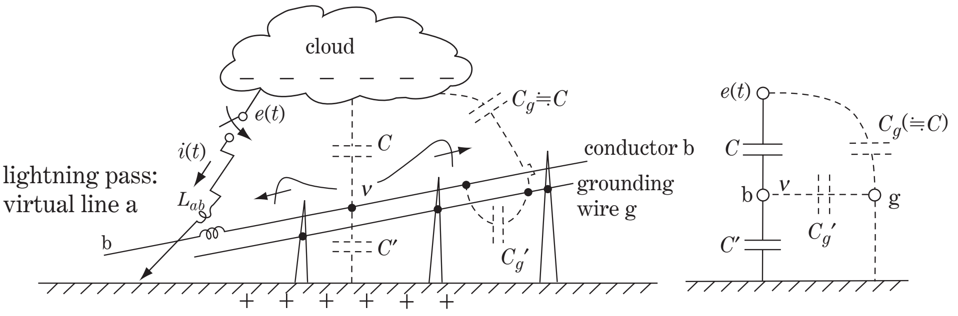


Figure 20.11 Induced lightning stroke

Now, we need to examine the function and the effect of the OGWs through a thought experiment.

In Figure 20.11, the virtual wire a (from the cloud to the Earth) and the line conductor b already exist. If one grounding wire g were additionally installed in parallel close to the conductor a, the equation of the capacitive induced voltage would be modified as follows:

capacitive induced voltage on conductor b

$$\left.\begin{aligned} &\text{without grounding wire}: v = \frac{C}{C+C'}\cdot e(t) && ① \\ &\text{with grounding wire g}: v \fallingdotseq \frac{C}{(C+C')+C'_g}\cdot e(t), \quad \text{where} C \fallingdotseq C_g \ll C' < C'_g && ② \\ &\text{inductive induced voltage}: v = L\frac{d}{dt}i(t) && ③ \end{aligned}\right\} \quad (20,7)$$

That is, the replaced new equation ② includes an additional C'_g in the denominator for the additional installation of the grounding wire. The conductor b and the grounding wire g are closely linked, so the relation of each capacitance size $C_g \fallingdotseq C < C' < C'_g$ is obviously justified. Accordingly, the capacitive induced voltage $v(t)$ can be reduced by the addition of the grounding wire g (the shielding effect). We discuss this matter again in Chapter 21.

20.5.3.2 Inductive induced lightning surges

As shown in Figure 20.11, mutual inductance L_{ab} exists between the virtual conductor a and the real conductor b, so that inductive surge voltage $v(t) = L_{ab}\cdot di(t)/dt$ is caused on conductor b by the lightning inrush current $i(t)$. The resulting flashover by the voltage is called the **inductive induced lightning surge**.

If we additionally install grounding wire g, L_{ab} can be reduced in the same way as we studied in Chapter 1. Therefore the grounding wire also can reduce the electromagnetic induced voltage.

Through the above study, we can imagine in regard to the grounding wire that the installation of OGWs can actually reduce L and increase C of the conductors so that the surge impedance $\sqrt{L/C}$ is also effectively reduced. This is discussed further in the next chapter.

21
Insulation Coordination

We have studied various kinds of overvoltage phenomena in the previous chapters from the viewpoint of their appearance. Now we need to study the total concept of insulation design of transmission lines, stations and all the installed equipment including the protection schemes for various overvoltages, namely the '**insulation coordination** of a power system' and the '**protection scheme for overvoltages**' as essential practical engineering of power systems.

We can imagine the problems our predecessors had when struggling with repeated failures caused by casual, irregular, over- or under-insulation design in the early twentieth century under the conditions of poor theoretical background, poor materials and application technology, poor experimental data, and with no commonly shared concept or guidelines to coordinate insulation strength as in today's standards or recommendations. Insulation coordination is a combined concept of 'physical phenomena', 'practical engineering and technology', as well as 'economy' based on much of the experience accumulated over a hundred years by our predecessors.

21.1 Overvoltages as Insulation Stresses

21.1.1 Conduction and insulation

Typical **conduction materials** are copper, aluminium and some other metals or alloys, and typical **insulation materials** are air, oil, SF_6 gas, porcelain, fibreglass, paper, plastic, cross-linked polyethylene, etc. Every small part of a power system is composed of a combination of conduction materials and insulation materials whose characteristics are very different. To obtain and control 'electricity' means to prepare an extremely large container called a power system, every part of which is made of a skilful **combination of different materials**.

In all materials, conduction is caused by the migration of charged particles. Conductors have large numbers of relatively free electrons, which will drift in an applied electric field. On the other hand, insulation materials (insulators) have very few free electrons. When electric stress in an insulation material is increased to a sufficiently high level, the resistivity along a path through the material will change from a high value to a value comparable with that of conductors. This change is the so-called **breakdown**.

In order to achieve our purpose, we need to have the following concepts from a practical engineering viewpoint:

- Magnitudes and characteristics of resulting overvoltages and over-currents.
- Required insulation strength of all the members comprising the power system.

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- Countermeasures to reduce various overvoltages and to protect the insulation of lines and station equipment.

Insulation coordination is the total concept as well as the practical guideline for system insulation design, combining the above three concepts that are based on accumulated and firm theoretical, experimental, technical as well as economic data.

Each part of a power system has to have estimated possible impinging overvoltage levels and the required insulation withstanding voltage levels as set values in order to realize continuous long-life operation of individual equipment. Accordingly, we need to have the concept of **insulation strength** as the another feature of overvoltages, which is always a key theme of practical high-voltage engineering activities, although a detailed description of individual insulation strength is far beyond the purposes of this book.

The essential philosophy as well as concrete recommended practices based on practical data for insulation design have been issued as international and/or national standards (IEC, IEEE, ANSI, JEC, etc.), which include the characteristics of impinging overvoltages and methods to protect insulation as important guidelines of practical insulation design engineering. Today, insulation coordination is an established concept throughout the world, so the contents of standards issued by different organizations can be said to be substantially identical to each other.

21.1.2 Classification of overvoltages

We have studied overvoltages in the previous chapters mainly from the viewpoint of the mechanisms arising. Lightning is an overvoltage generated by external events, while the overvoltages generated by switching operations, fault occurrences, as well as ferro-resonance, load rejection, loss of ground, etc., are generated by internal events. Now we need to classify them again from the viewpoint of impinging overvoltages on the individual insulation structures.

Insulation strength of individual parts of the system or equipment is closely affected by the following factors of the impinging overvoltages:

- Characteristics of overvoltages (magnitude, shape, duration, polarity of the applied voltages)
- Insulation design of the electric field distribution in the insulation
- Type of insulation (gaseous, liquid, solid or a combination)
- Physical state of the insulation (temperature, pressure, mechanical stress, etc.)
- Operation and maintenance history of individual insulation materials.

The impinging overvoltages may exceed permissible insulation levels of individual parts of the system, so that these overvoltages should be reduced within permissible levels, or the insulated equipment should be safely protected against such overvoltages. This is essential to avoid insulation damage of equipment or to prevent possible undesirable system performance.

The **magnitudes**, the **wave shapes** (steepness of the voltages) and the **time duration** of overvoltages are important factors in regard to the stress on the insulation. Taking such important factors into account, the characteristics of overvoltages are generally classified into the following categories by authorities like the IEC and/or national standards bodies:

a) **Maximum continuous** (**power frequency**) **overvoltage**: U_s (MCOV). This can originate from the system under normal operating conditions.

b) **Temporary overvoltage** (TOV). This can originate from faults, switching operations such as load rejection, resonance conditions, non-linearity (ferro-resonance), or by a combination of these.

c) Slow-front overvoltages. These can originate from switching operations and direct lightning strikes to the conductors of overhead lines.

d) Fast-front overvoltages. These can originate from switching operations, lightning strikes or faults.

e) Very fast-front overvoltages. These can originate from faults or switching operations in gas-insulated switchgear (GIS).

The overvoltages of items (c)–(e) are the transient overvoltages. Now we examine these items below.

21.1.2.1 Maximum continuous (power frequency) overvoltages: $\mathbf{U_s}$

Under normal operation, the power frequency voltages differ from one point of the system to another and vary in magnitude over time. For purposes of insulation design and coordination, continuous overvoltages should be considered the possible highest operating voltages under normal operation, which is usually defined as 'highest system voltage (the symbol U_s (kV)'.

In usual practice, U_s is presumed to have a value of 1.04–1.1 times system normal voltage. For example,

normal voltage phase-to-phase	highest system voltage U_s	representative TOV $(U_{rp} = k \cdot (1/\sqrt{3}) U_s)$
230 kV (phase-to-phase)	245 kV (phase-to-phase)	212 kV (phase to earth, k = 1.5)
500 kV	550/525 kV	476/455 kV (k = 1.5)
735 kV	765 kV	662 kV (k = 1.5)

21.1.2.2 TOVs and representative TOVs: $\mathbf{U_{rp}}$

Typical situations that may give rise to TOVs include:

- Single line-to-ground faults
- Ferro-resonance
- Load rejection
- Loss of ground
- Long unloaded transmission lines (Ferranti rise)
- Coupled line resonance
- Transformer line inrush.

TOVs caused by all these situations are the result of some internal events caused by circuit connection changes, so the voltages tend to show unstable, slow transient behaviours to some extent and often include somewhat lower order harmonics or d.c. components, usually lasting a period of hundreds of milliseconds or longer, say a few minutes.

The highest TOVs for each of the above situations have to be presumed as the representative TOV U_{rp}, considering system configuration and operating practices at present and in the future. However, as a minimum, the overvoltages due to line-to-ground faults should be addressed, because they are typically the most significant and probably the highest TOVs for most cases. Single line-to-ground faults probably cause the largest power frequency overvoltages among various fault modes.

Courtesy of TEPCO

Double circuit lines, nominal voltage: 1000 kV, maximum voltage: 1100 kV
Conductor wire ACSR 810 mm² (diameter 38.4 mm)
- 1258 A (continuous, in summer)
- eight-bundled conductors (w = 400 mm)
- maximum temperature 90°C

OPGW: double circuit 500 mm² × 2
Suspension insulator (320 mm type)
- double size 2 × 40 series connection
- horn gap: 6.3 m

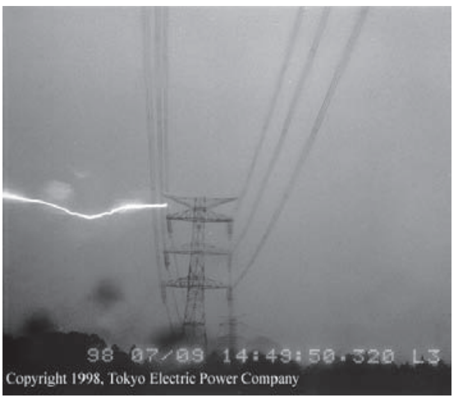
Towers (average weight: 310 tons)
- average height: 111 m, height of lowest arm: 65 m
- phase-to-phase within same circuit: 20 m
- phase-to-phase between different circuits 33 m
- conductor height >32 m
- span average 632 m, maximum 1056 m

TOV (Temporary Overvoltage)
- 1 LG: 1.1 pu, LR (load rejection): 1.5 pu

LIWV (Lightning Impulse Withstand Voltage)
- GIS 2250 kV, Tr 1950 kV

SIWV (Switching Impulse Withstand Voltage)
- GIS 1550 kV, Tr 1425 kV

Figure 21.1 1000 kv design Double-Circuit Transmission Line (TEPCO, Kita-tochigi line, 427km)



Direct strike to the upper conductor (1998)

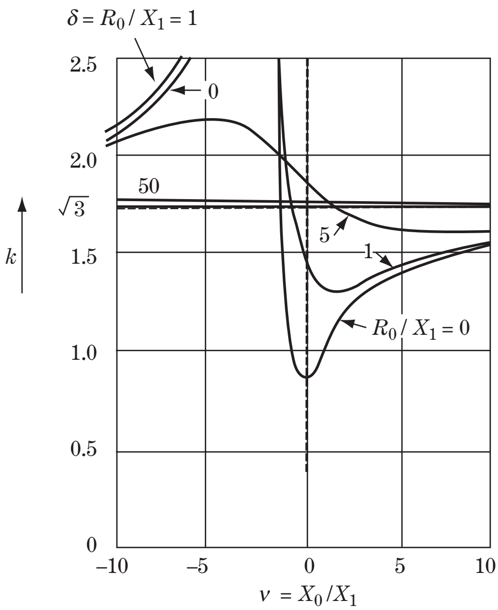


Figure 21.2 Overvoltage ratio of unfaulted phases during line-to-ground fault: k

21.1.2.2.1 Single line-to-ground faults Figure 21.2 is the enlarged curve of Figure 8.1 which is derived by fault analysis with power frequency. As in Chapter 8, the magnitudes of TOVs appearing on unfaulted phases during a line-to-ground fault are related closely to system grounding conditions (namely, $\delta = {}_fR_0/{}_fX_1$, $\nu = {}_fX_0/{}_fX_1$) and can be estimated by 'earth–fault factor k' (or 'coefficient of grounding', COG).

The factor k may be typically 1.5 for a solidly neutral grounding system, or $\sqrt{3}$ or higher for a resistance/reactance neutral grounding system.

21.1.2.2.2 Load rejection If a large load were suddenly separated (by the bus protective relay tripping in a large substation, for example), overvoltage by **load rejection** would be caused and continue, say, 10–200 s until voltage regulation by the AVR systems of power stations/substations is completed. Although the amplitudes of overvoltages depend on rejected load size and the network configuration, the amplitudes would be 1.2 pu or less in moderately extended systems due to the quick response characteristics of AVRs. However, overvoltages are apt to become larger in power systems of rather extended configurations. When full load rejection is caused at the receiving substation of a long transmission line end, the voltage may rise by a magnitude close to 1.5, because the Ferranti effect at the open receiving end would be added.

21.1.2.2.3 Loss of ground In high-resistive/reactive neutral grounded systems, special consideration is required. If one phase-to-ground fault occurs in the system and the neutral grounded transformer is tripped for some reason (by a back-up relay tripping, for example), this means **loss of ground** (assuming another neutral grounding transformer does not exist in the same system). As the potential of the system neutral point becomes free of the earth potential, unfaulted phase voltages become at least $\sqrt{3}$ pu during one phase to earth fault ($1\phi G$). Furthermore, even after one-phase-fault condition is removed, three-phase voltages as well as neutral point voltages fluctuate waywardly. In these conditions, numbers of installed arresters would probably be broken, which means extended new faults at arrester points, and/or earth faults would be caused at various locations.

Above all, loss of ground should be absolutely avoided. In high-resistive/reactive neutral grounding systems, neutral groundings by as many transformers as possible (at least by two transformers installed at different stations in one service area) are preferable.

The TOV caused by one phase to ground faults may be mostly presumed to be the representative largest power frequency overvoltage U_{rp} (which is usually expressed by rms kV value, phase-to-earth). Then

$$\text{representative TOV} = U_{rp} = k \cdot \left(1/\sqrt{3}\right) U_s$$

As a typical example,

$$\begin{aligned} \text{nominal voltage } 230\,\text{kV} \quad & k = 1.5 \quad \text{TOV} = 1.5 \cdot \left(1/\sqrt{3}\right) \cdot 245 = 212\,\text{kV} \\ 735\,\text{kV} \quad & k = 1.5 \quad \text{TOV} = 1.5 \cdot \left(1/\sqrt{3}\right) \cdot 735 = 662\,\text{kV} \end{aligned}$$

21.1.2.3 Slow-front overvoltages

Slow-front overvoltages have durations of some tens to thousands of microseconds and tail durations of the same order of magnitude, and are oscillatory by nature. They arise generally in the occasions below:

- Line energization/re-energization (breaker closing/opening)
- Fault occurring/fault clearing
- Switching of capacitive/inductive current
- Load rejection
- Distant lightning strike (lightning strike wavefront flattened by travelling).

The voltage-arising mechanisms of all these have been studied in the previous chapters.

The representative voltage stress is characterized by the representative voltage amplitude and wave shape. Therefore, the **representative switching impulse voltage of 250/2500** μs (time to peak 250 μs, and time to half-value on the tail 2500 μs, see Table 21.3 below) has been standardized as the common general concept throughout the world to represent standardized slow-front overvoltages.

Although the voltage-arising mechanisms of all the above have been studied in previous chapters, here are some additional comments.

For the switching of capacitive/inductive current, we have studied overvoltages caused by current chopping in Section 19.4, caused when the current power factor is almost zero. In particular, the following switching operations require special attention:

- Switching of unloaded cables or capacitor banks.
- Inductive current tripping (transformer magnetizing current tripping, for example).
- Arc-furnace load switching.
- Interruption of currents by high-voltage fuses.

The most useful and commonly used method of limiting the slow-front overvoltages is to adopt the resistive tripping/closing breakers we have studied already. Surge arrester protection against slow-front overvoltages will be discussed later.

21.1.2.4 Fast-front overvoltages

Typical fast-front overvoltages are of course lightning strikes, although they can originate also from switching operations. We have already learned that lightning overvoltages can be classified into direct strikes, back-flashovers and induced lightning strikes.

The induced lightning surges occur generally below 400 kV and so are of importance only for lower voltage systems of 100 kV. Back-flashover voltages are less probable on UHV systems of 500 kV or more, due to the high insulation withstanding values.

The representative wave shape of fast-front overvoltages is the well-known 1.2/50 μs wave (see Table 21.3 below).

21.1.2.5 Very fast-front overvoltages

Very fast-front overvoltages can originate from switching operations or from faults within GIS due to the fast breakdown of the gas gap and nearly undamped surge propagation within GIS, where the average distance between two adjacent transition points in the same GIS is very short. (If 7.5 m distance and $u = 300\,\mathrm{m}/\mu\mathrm{s}$ is assumed, the resulting travelling surges repeat 20 times every 1 μs, so the natural frequency is 20 MHz, and the first-front wavelength (quarter cycle) is 0.0125 μs.) However, the amplitudes of the surges would be rapidly dampened and flattened on leaving the GIS, so they are relieved to some extent at the external circuit of the GIS bushings.

The overvoltage shape is characterized by a very fast increase of the voltage to nearly its peak value, resulting in a front time below 0.1 μs. For switching operations this front is typically followed by an oscillation with frequencies of 1–20 MHz. The duration of very fast-front overvoltages would be less than 2–3 ms; however, 20 MHz and 3 ms means 60 000 times of beating stresses. Furthermore, they may occur several times. The magnitudes of overvoltage amplitude depend on the structure of the disconnector and on the adjacent structure of station equipment.

Very fast-front overvoltages can and have to be dampened/flattened to some extent, and the typical countermeasure is application of gap-less arresters, which means insertion of a non-linear high resistance in parallel across the phases and earth. The maximum amplitudes to 2.5 pu can be assumed to be achievable.

Due to faults within GIS, the connected equipment, in particular a transformer, is stressed by the overvoltages, which would contain frequencies up to 20 MHz, and the amplitude may exceed the breakdown voltages of the transformer without effective countermeasures.

21.2 Fundamental Concept of Insulation Coordination

21.2.1 Concept of insulation coordination

What is usually meant is the coordination or correlation of the transmission line insulation with that of the station apparatus, and perhaps the correlation of insulation of various pieces of apparatus and parts of the substation. The coordination of substation and equipment insulation is the protection of service and apparatus from overvoltages in excess of specified insulation withstanding values at optimum economy and reliabilities.

It is obvious that various kinds of reliable knowledge and accurate data based on a lot of experience and advanced technology had to be required in order for a reasonable concept of insulation coordination to be established, as follows:

- Investigation of mechanisms of various different overvoltages.
- Estimation of possible overvoltages on the line or in the substation.
- Countermeasures to reduce various overvoltages, including development of protective devices.
- Specification of insulation withstanding values for a transmission line.
- Specification of insulation withstanding values of the station apparatus, and so on.

Today, it is fair to say that the descriptions of all the standards relating to insulation coordination have converged on the same criteria. This great result has been achieved through a long process of evolution in the past hundred years. We study such this worldwide common concept in this chapter.

21.2.2 Specific principles of insulation strength and breakdown

The basic concept of the coordination or correlation of transmission line insulation and that of the station apparatus can be summarized as follows.

21.2.2.1 Insulation design criteria of the overhead transmission line

The basic criteria are:

- Flashovers caused by lightning strikes are allowed as fatal phenomena, while damage (damage to conductors, cracks in insulators, etc.) to the transmission line should be avoided.
- Technically as well as economically balanced insulation distance (clearance) is to be assured in the fundamental design, allowing some extent of failure rate caused by lightning strikes. Also, countermeasures should be adopted as much as possible to reduce the influence and frequency of effects on a substation.
- Flashover should not be caused by switching surges or by any sustained lower frequency overvoltages.

The above concept is based on the characteristics in atmospheric air of so-called insulation and cooling materials of infinite natural circulation type, so that, once broken, insulation would be restored (recovered) whenever the surge source disappears (**self-restoring insulation characteristics**).

The principal countermeasures are:

- To reduce the probability of lightning strike, and to limit the faulted circuits (in the case of multiple circuit transmission lines) and faulted phases as much as possible. (Adoption of overhead grounding wires and any other effective countermeasure.)
- To reduce the probability of back-flashover caused by lightning strikes on the overhead grounding wires or on the towers (surge impedance reduction of towers and overhead grounding wires).
- Countermeasures to relieve the travelling waves to some extent before reaching the substation terminal point.
- Insulation-level withstanding against switching surges from the substation.
- The idea to reduce the probability of simultaneous faults on plural circuits of the same route.
- Adoption of reclosing.

21.2.2.2 Insulation design criteria of the substation and substation apparatus

The basic criteria are that insulation of the station and the station apparatus should be protected to withstand lightning surges and switching surges, so that insulation failure of the apparatus and loss of station services over a long time should be avoided.

The principal countermeasures are:

- Countermeasures to reduce direct lightning strikes to the station as much as possible.
- To protect the substation and the station apparatus against transmitted lightning surge voltages from the overhead transmission lines without damage (arresters).
- To reduce switching surge levels and to protect the station apparatus against switching surges without damage. That is, flashover or insulation failure of equipment should not be caused by switching surges or by any sustained lower frequency overvoltages in the substation.

21.2.2.3 Insulation design criteria of the power cable line

The basic criteria are to protect the power cable against lightning surges, switching surges as well as fundamental frequency overvoltages. The power cable does not have the characteristics of self-restoring insulation, so it should be protected entirely from overvoltages in the same way as transformers or other substation apparatus.

All above criteria are just substantial images of the fundamentals of our power systems, and thus are the objectives of insulation coordination.

21.3 Countermeasures on Transmission Lines to Reduce Overvoltages and Flashover

We list in this section the major countermeasures usually adopted as concrete design for high-voltage transmission lines and substations in order to meet the design criteria described above. We have already studied in previous chapters the reasons why the adoption of each listed countermeasure is technically effective and can be justified.

Readers are requested to refer to the literature for those details exceeding the purposes of this book.

21.3.1 Countermeasures

21.3.1.1 Adoption of a possible large number of overhead grounding wires (OGWs, OPGWs)

The following effects are expected from the adoption of OGWs:

- By locating the **OGW** at the top of a tower, the probability that lightning directly strikes the phase conductors can be reduced (**shielding effect**, Section 20.5). A lightning strike directly on the OGW may occur, but most of the energy can be bypassed as strike current though the OGW and towers to earth.
- The surge impedance $\sqrt{L/C}$ of the line conductors can be reduced. (L is decreased largely and C is increased largely by installation of OGW and with increasing numbers of OGWs, Chapters 1, 3, 20). The absolute magnitude of induced surge voltages appearing on the phase conductors can be reduced so that the probability of phase faults by back-flashover or induced strikes is improved (Section 20.5). Also, alleviation of steep wave-front (surge-front flattening) of the lightning surge can be expected (Chapter 18).
- The time constants $T = 2L/r$ of the line can also be reduced so that the attenuation of traveling–wave or transient oscillation terms can be accelerated (Chapters 18, 19).
- Positive-, negative- and zero-sequence inductance $L_1 = L_2, L_0$ can be reduced, an important feature.
- Power frequency voltage drop by line reactance can be reduced (Chapters 1, 3).
- The stability limit as well as the power circle diagram can be improved (Chapters 12, 14).

Today, **OPGW** (grounding wire with optical fibre) has prevailed as OGW with the function of communication channel media.

21.3.1.2 Adoption of reasonable allocation and air clearances for conductors/grounding wires to assure insulation withstanding level and reduction of surge impedances

The phase conductors as well as the OGWs have to have sufficient clearance from each other so that the necessary insulation withstanding strength is maintained within some margin against the predicted largest short-duration overvoltages and switching surges coming from neighbouring substations. Of course, this is a trade-off between the probability of flashover failure and the construction cost of larger towers. The allocation of conductors and grounding wires is decided to result in a reasonably phase-balanced smaller L and smaller $\sqrt{L/C}$ within the necessary margin against **physical movement** (caused by **wind**, **heat expansion**, **galloping**, **sleet jumping**, etc.).

Details of **standard lightning impulse-withstanding voltages** are examined in Section 21.5.

21.3.1.3 Reduction of surge impedance of the towers

The **surge impedance of the towers** has to be reduced as much as possible, in that induced surge voltages on the transmission lines can be reduced, or the probability of striking can be reduced. In particular,

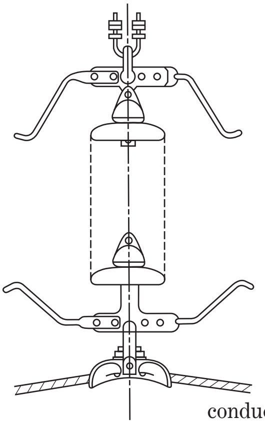


Figure 21.3 Arcing horn

back-flashover caused by direct strikes on the top of the towers or the grounding wires may be effectively reduced (Section 20.5). The magnitude of the tower surge impedance is related to the height of the tower and the resistivity of the ground, and may typically be 20–100 Ω for EHV class lines.

21.3.1.4 Adoption of arcing horns (arcing rings)

Arcing horns are a kind of air gap having self-restoring insulation characteristics. They are arranged in parallel with each insulator as a single body on every tower. Figure 21.3 is an example for a single conductor line. The duty and the purpose of arcing horns can be summarized as follows:

- Flashing overvoltages can be controlled by selection of the shape and air-gap length of the arcing horns so that the magnitudes of travelling surge voltages caused by the lightning can be limited by the flashover voltage of the arcing horn. The arcing horn may be said to be the intentionally arranged weak point of a conductor's insulation (**controllable limitation of surge voltage magnitude**).
- An arcing horn assembled together with an insulator can improve the potential gradients of the insulator in that the voltage distribution by series-connected individual porcelain pieces can be unified (**improving withstand voltage**).
- Flashover by arcing horn can avoid flashover along the surface of insulators, so that the insulators can be protected from damage against thermal shock (**protection of porcelain insulators**).

On the other hand, arcing horns have the limitation below:

- Arcing horns as well as any other part of the overhead transmission lines should not experience flashover by switching surges coming from adjacent substations.

The air-gap lengths of each arcing horn on the series-connected towers are generally arranged with equal lengths, while that on the first to third towers of outgoing feeder lines from the station may exceptionally be arranged with a little smaller gap length. Lightning overvoltage caused by striking the transmission line point within one or two spans from the station appears as impinging travelling surges to the station without attenuation or shape flattening. Accordingly, in order to alleviate such severe surges caused by a close-point direct strike, the arcing horns at the first few spans may be arranged with somewhat smaller gap lengths.

21.3.1.5 Adoption of unequal circuit insulation (double circuit line)

Line failure caused by lightning striking the transmission lines cannot be avoided; however, we need to reduce the situation first. Primarily, simultaneous double circuit faults should preferably be reduced. Furthermore, numbers of faulted phases of the same circuit should also be reduced as much as possible.

For this purpose, so-called **unequal circuit insulation** or **unequal phase insulation** has been partly applied, in that horn-gap lengths of a specified single circuit or phase are intentionally arranged within a short distance.

21.3.1.6 Adoption of high-speed reclosing

Automatic high-speed reclosing is an important practice to reduce the influence of lightning failure. Its essence is studied here, although it may not be directly related to insulation coordination.

In single phase reclosing for a phase a to ground fault (aϕG) (for example), immediately after the phase a conductor to earth fault occurs, the phase a pole contactors of both line terminal breakers are tripped within say 2–6 cycles (operating time of the relay + the breaker), that is single phase tripping. Then, although the phase a conductor is already separated from the station buses, the voltage v_a on the phase a conductor still remains, so the arc would also continue for some small duration (arcing time, say, 0.2–0.5 s), because in addition to the initial trapped potential charge (d.c.) the electrostatic (C-coupled) voltage is induced from unfaulted parallel phase voltages v_b, v_c. However, due to the outstanding self-restoring characteristics of natural air, the arc will soon be extinguished. Accordingly, if the phase a poles of the breakers of both terminals are reclosed after arc extinction, the faulted line again continues three-phase operation successfully.

The time from the one-pole tripping to reclosing is customarily called '**dead-voltage time**', which is a set value in the primary protective relay equipment for the line. The arcing time is apt to become longer for higher voltage systems, in particular UHV systems of 500 kV or more, so that dead-voltage time as a relay setting value has to be set longer. There is some presumption that in 1000 kV class power systems, self-arc extinction cannot be expected within a short time, so automatic reclosing may not be available without applying forced grounding switches.

For the classification of high-speed reclosing, let us assume double circuit line 1 (a,b,c) and line 2 (A,B,C):

- **Single phase reclosing**: This occurs on one line-to-ground fault (phase a reclosing against phase a fault).
- **Three-phase reclosing**: When a fault occurs on line 1, the three phase a–b–c poles are tripped and reclosed regardless of the fault modes on the line. This is obviously a practice which can be applied only for double circuit lines, or at least 'assured loop-connected lines'.
- **Multi-phase reclosing**: In the case of a fault of 'line 1 phase a and line 2 phase B', for example, reclosing is conducted for these two poles, because all phases A, b, C are still soundly connected even though this is s double circuit fault. This is quite an effective method in comparison with three-phase reclosing, because it can minimize line out of service, or relieve system instability. Also very accurate fault phase-detection by protective relays (preferably by phase differential protection) is required for this practices.

Incidentally, reclosing is allowed only when both terminal buses across the faulted line are operating in synchronization. Furthermore, possible sending power during the dead-voltage time has to be noted. Single phase reclosing on a single circuit line for example means one phase opening mode condition, as shown in Table 3.2 and Table 8.2 [8] [10]. Then reclosing should be allowed only when the presumed power flow during the dead-voltage time is within the stability limit under the condition of only the sound phases (refer to Section 14.4).

Also, we need to pay attention to the undesirable electrical and mechanical effects on the thermal generators caused by the appearance of negative- and zero-sequence currents during the reclosing dead-time.

21.4 Overvoltage Protection at Substations

Now we need to examine aspects of overvoltages at the substations and the various countermeasures to protect them against overvoltages or to reduce the stresses.

21.4.1 Surge protection by metal–oxide surge arresters

21.4.1.1 The principle of surge protection by arresters

Arresters are key devices to protect substations and station devices against lightning surges and whose surge protective capability actually decides the required insulation levels of the power system network. **Gap-less arresters** may also have the capability to reduce switching surges.

Typical high-voltage arresters are **metal–oxide surge arresters** having metal–oxide resistive disc elements with excellent non-linear *v–i* characteristics and thermal energy withstanding capability. The metal–oxide resistive elements are composed of a number of lapped disc elements, each of which is made from zinc oxide (ZnO) powdered material with some specially mixed inclusions, and, similar to pottery or porcelain is produced through a high-temperature baking procedure.

We examine first the fundamental principles of the arrester in Figure 21.4. In Figure 21.4a, the arrester is installed at point a, which is a transition point because the arrester impedance is at least

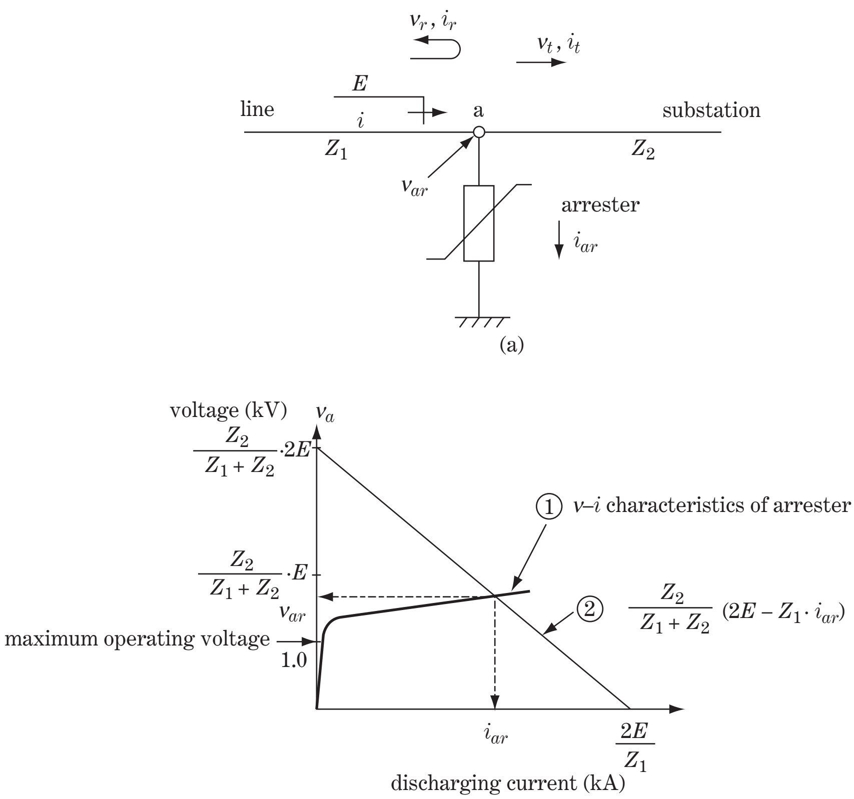


Figure 21.4 Principle of surge protection by arrester

connected. Now, incident overvoltage wave E comes from the left and passes through point a from the left-side line Z_1 to the right-side circuit Z_2 towards the station bus. The behaviour of the travelling waves at point a can be written by the following equations:

$$\left.\begin{array}{lrl} E+v_r=v_{ar}=v_t & \text{where} & E: \text{incidental surge voltage from the left-side line} \\ i-i_r=i_t+i_{ar} & v_{ar}, i_{ar}: & \text{the terminal voltage and the current of the arrester at} \\ & & \text{arrester point a} \\ E=Z_1 i & Z_{ar}: & \text{non-linear resistance of the arrester} \\ v_r=Z_1 i_r & v_t, i_t: & \text{the transmitted voltage and current at point a to the substation} \\ v_t=Z_2 i_t & Z_1: & \text{the surge impedance of the transmission line} \\ v_{ar}=Z_{ar}\cdot i_{ar} & Z_2: & \text{the surge impedance of the substation gateway at point a} \end{array}\right\} \tag{21.1a}$$

The relation between the arrester voltage v_{ar} and current i_{ar} is shown as the non-linear curve ① in Figure 21.3b, which represents the *v–i* characteristics of the arrester.

Eliminating v_r, i_r in the above equation, and by modification,

$$v_{ar}=v_t=\frac{Z_2}{Z_1+Z_2}(2E-Z_1 i_{ar}) \tag{21.1b}$$

This equation is written as the straight line ② in Figure 21.4b. The actual voltage v_{ar} at point a and the arrester current i_{ar} are given as those at the intersection of curve ① and the straight line ②. If the arrester does not exist, the voltage v_{ar} at point a would become $\{2Z_1/(Z_1+Z_2)\}\cdot 2E$ (then, E under the condition $Z_1=Z_2$), or $2E$ maximum under the condition $Z_2=\infty$ (i.e. the case when the feeding terminal is opened). However, if the arrester with appropriate non-linear *v–i* characteristics is installed at point a, surge voltage v_{ar} can be reduced to a smaller value than the original impinging surge value E and of course smaller than $2E$. On the other hand, in order to realize the above condition, the arrester is required to withstand the resulting extremely large thermal energy $\left(\int v_{ar}\cdot i_{ar}dt\right)$ without losing the original *v–i* characteristics of the resistive elements and without breaking. It is furthermore required to restore immediately the original electrical operating condition before the impinging surge.

Now, as a natural consequence of the above, the arrester has to have the following characteristics as its inherent duty requirement:

- Under power frequency operation with voltage U_s (MCOV) as well as of U_{rp} (representative TOV), the arrester has to have quite a high resistivity so that the arrester resistive elements can withstand the thermal stress caused by the small leakage current (**continuous current of arrester**, usually not more than a few milliamps, and probably 1 mA or less). For example, for a power system of nominal voltage 230 kV, $U_s=245\,\text{kV}$, $U_{rp}=k\cdot\left(1/\sqrt{3}\right)U_s=1.5\times\left(1/\sqrt{3}\right)\cdot 245=212\,\text{kV}$. Then, the arrester continuous current, assuming $i_{at}\leqq 1\,\text{mA}$ at $U_{rp}=212\,\text{kV}$, is $v_{ar}=212\,\text{kV}$, $i_{ar}\leqq 1\,\text{mA}$, $Z_{ar}\geqq 212\,\text{M}\Omega$ and the arrester thermal loss (stress) is $v_{ar}\cdot i_{ar}=212\,\text{kV}\cdot 1\,\text{mA}=212\,\text{W}$
- When the surge voltage and current arrive at the arrester point a, the arrester has to discharge surge current immediately without delay, and next, the voltage v_{ar} arising should be limited within a specified upper limit (**residual voltage**, or **discharge voltage**, U_{res}) during the passage of discharge. Then, as a result, the station equipment can be protected against the overvoltage.
- The arrester should withstand the thermal energy caused by the surge current through the arrester elements (**arrester discharge current**, say 10–100 kA), with thermally stable *v–i* characteristics.
- Immediately after the discharge current through the arrester disappears (probably within 50–100 μs), the following current i_{ar} by continuing power frequency voltage v_{ar} should become the original small leakage current within a few milliamps. Or, in other words, immediately after the surge voltage and current disappear, all the electrical characteristics have to be restored.

- The arrester has to have **switching surge discharging capability** within the specified levels. (The switching surge duty on metal–oxide arresters increases for higher system voltages.)

At the substation where the arrester is installed at the junction point with the transmission line, the appearing surge voltage (caused by the impinging lightning surge from the same line) can be limited to the **arrester discharge voltage** so that the station equipment having an insulation level exceeding the arrester discharge voltage (or residual voltage) can be protected by the arrester. This is the principle of station equipment protection by surge arrester.

21.4.1.2 Metal–oxide arresters

The fundamental configuration of these arresters can be classified as follows:

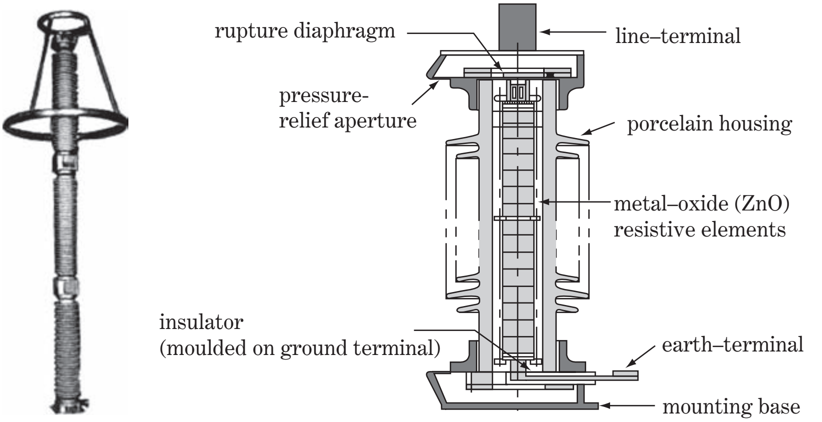
- **Gap-less arresters**
- **Series-gapped arresters**
- **Shunt-gapped arresters.**

Figure 21.5 shows a typical example of a porcelain-type metal-oxide gap-less arrester for high-voltage station use.

Before the appearance of gap-less arresters, probably around 1980, the configuration of highvoltage arresters was of the series-gap type without exception and arranged in series with non-linear resistive elements. It was mainly because the thermal duty of the resistive elements at the time was rather small that the elements could not withstand the thermal energy of continuous flowing leakage current caused outside the series gap by power frequency terminal voltages. Accordingly, the series-gap types used to be indispensable continuous devices to avoid thermal damage to resistive elements caused by continuous leakage current flowing through the elements.

However, it may be fair to say that nowadays most arresters for high-voltage systems are of the gapless type, in which the non-linear resistive element block is always directly charged by power frequency phase voltages so that the minute leakage currents flow through the arrester's resistive elements continuously. Of course, the resistive elements need to withstand the thermal stress caused by continuous thermal energy $\left(\int v_{ar} \cdot i_{ar} dt\right)$ from the leakage current and to maintain thermally stable v–i characteristics.

Incidentally, in the case of series-gapped arresters, the arcing ignition across the series gap is initiated by the surge voltage. Therefore arcing extinction across the gap immediately after the surge



Courtesy of Toshiba

Figure 21.5 Arrester for station use (porcelain type) current disappears under charging conditions by the continuing power frequency voltage is another important requirement for this arrester (**following current tripping duty**).

The technology to produce resistive discs with outstanding *v–i* characteristics and thermal energy withstanding duty has advanced remarkably in the last twenty years, and enabled the realization of gapless arresters with outstanding capabilities. It is worth remembering that, because this realization is based on advanced disc production skills, a drastic **decrease in the insulation levels** of transmission lines as well as of station equipment has been achieved.

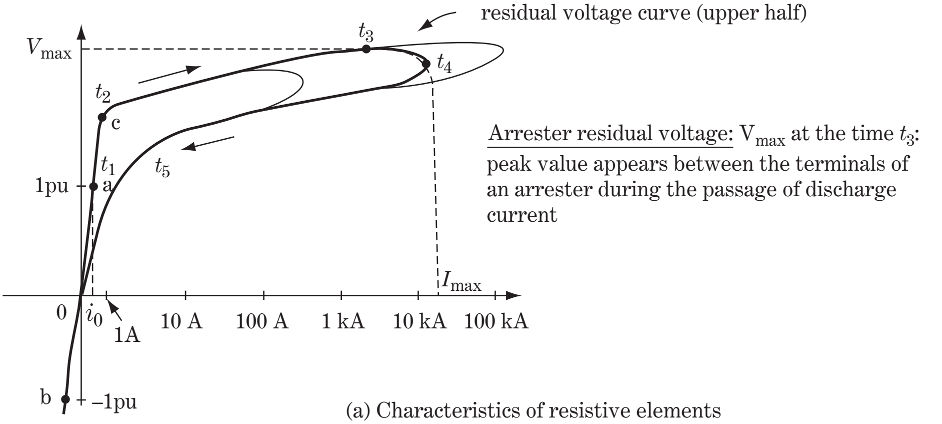
Figure 21.6a shows the ***v-i* characteristic curve of arresters**. Under normal conditions, the *v-i* operating point goes back and forth between points a and b every power frequency cycle, where the largest current i_0 at a or b (the leakage current) has a very small value of 1–3 mA or even, say, 100–500 μA. If impinging surge voltage E arrives at the arrester terminal point at time t_1, the operating point (tracing along the curve) immediately goes past knee point c (c is the so-called '**initial discharging point**') at time t_2, then reaches the point of maximum voltage V_{max} at t_3 (the maximum voltage is called the '**residual voltage**'), and, next, arrives at the point of maximum current at t_4. As the surge current decreases, the voltage will decrease back towards the pre-surge level. In other words, as soon as the surge current disappears (possibly within 100 μs) the operating point moves to the points 0 and b. The return path is a little different to the outward path, because the characteristics of the resistive element are affected by the thermal effect.

Through all the above tracing process, a large discharging voltage of one polarity arises during time t_2–t_3–t_4–t_5 (the largest value of the discharge voltage at t_3 is the residual voltage).

Figure 21.6b is the arrester's '**residual voltage characteristics curve**' showing the protective level of the arrester. The vertical axis voltage is the residual voltage peak value which corresponds to the value of V_{max} at t_3 in Figure 21.6a. This curve actually indicates the protection capability of the arrester.

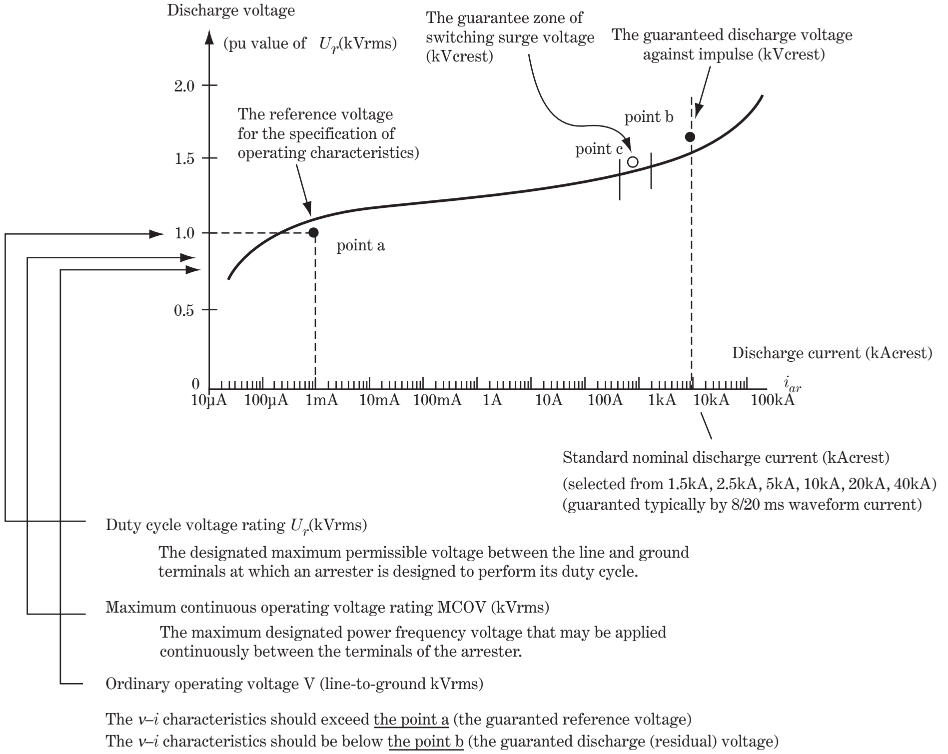
When lightning surge voltage and current arrive at the arrester terminal point, the surge voltage level is reduced by the arrester; however, discharge voltage (residual voltage) given by Figure 21.6b still arises. The value of the discharge voltage depends on the magnitude of discharge current (impulse current) flowing through the arrester i_{ar}.

The **surge current** through i_{ar} would mostly have values of 5–20–50 kA, while the presumed largest discharge current values could be 150 kA for the 400–500 kV, 100 kA for the 275–300 kV, 80 kA for the 160–230 kV, 60 kA for the 110–160 kV and 30 kA for the 60–90 kV class. Figure 21.6b is an example of quite advanced characteristics, by which the residual voltage would be distributed within a voltage range of 1.5 times the power frequency operating voltage for most cases, whereas larger discharge voltage of over two times, say 2–3.5 times, the operating voltage could also appear if quite a large impulse discharge current flows through the arrester.

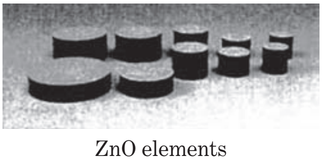


(a) Characteristics of resistive elements

Figure 21.6 Arrester characteristics

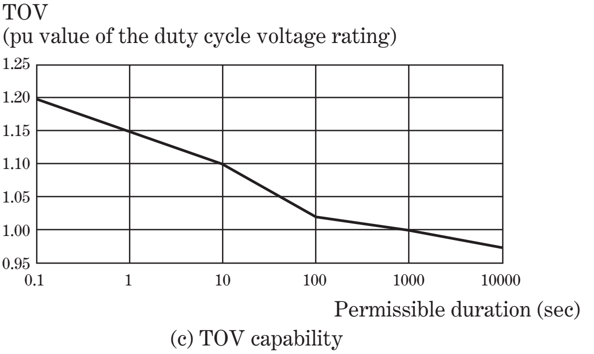


(b) Arrester *v–i* characteristic curve



ZnO elements

Courtesy of Toshiba



(c) TOV capability

Figure 21.6 (*Continued*)

Taking all the above into consideration, it may be concluded that, for insulation coordination of a substation and its equipment:

- Against impinging lightning surge to the station it is necessary:
  - to limit the arising station surge voltage within the **presumed largest residual voltages of the adopted arresters**
  - to realize the **withstanding insulation level of the station equipment** against the largest discharge voltage (residual voltage) for the presumed largest surge current.
- For power frequency voltages, the arrester should withstand the thermal stress of continuous leakage current caused by MCOV as well as TOV and of switching surge energy.

Figure 21.6c shows the typical TOV capability of arresters.

21.4.1.3 Classification and selection of arresters by ratings

We present here the general concept in regard to a guaranteed method for the characteristics of an arrester and the method of selection, although the detailed description of arrester standards may differ among national standards bodies.

Referring to Figure 21.6b, the arrester's essential *v–i* characteristics are guaranteed by the two points a and b.

Point a is the **reference voltage and current** (v_a (kVrms), i_a (mArms)) which are indicated by the supplier. It should be guaranteed that the *v–i* characteristics of an individual arrester at the reference current i_a (typically 1 mA) exceed the guaranteed reference voltage v_a, and the arrester has to withstand the thermal energy of the guaranteed power frequency continuous current i_a.

Point b is the **standard nominal discharge voltage and current** (v_b (kVcrest), i_b(kAcrest)). The standard nominal current i_b is specified as standard values like 1.5 kA, 2.5 kA, 5 kA, 10 kA, 20 kA, 40 kA for the purpose of arrester classification and for guaranteeing the discharge voltage characteristics. The discharge voltage of an individual arrester should be smaller than the guaranteed standard nominal discharge voltage v_b at the standard nominal discharging current i_b (tested typically with the 8/20 μs standard impulse wave current).

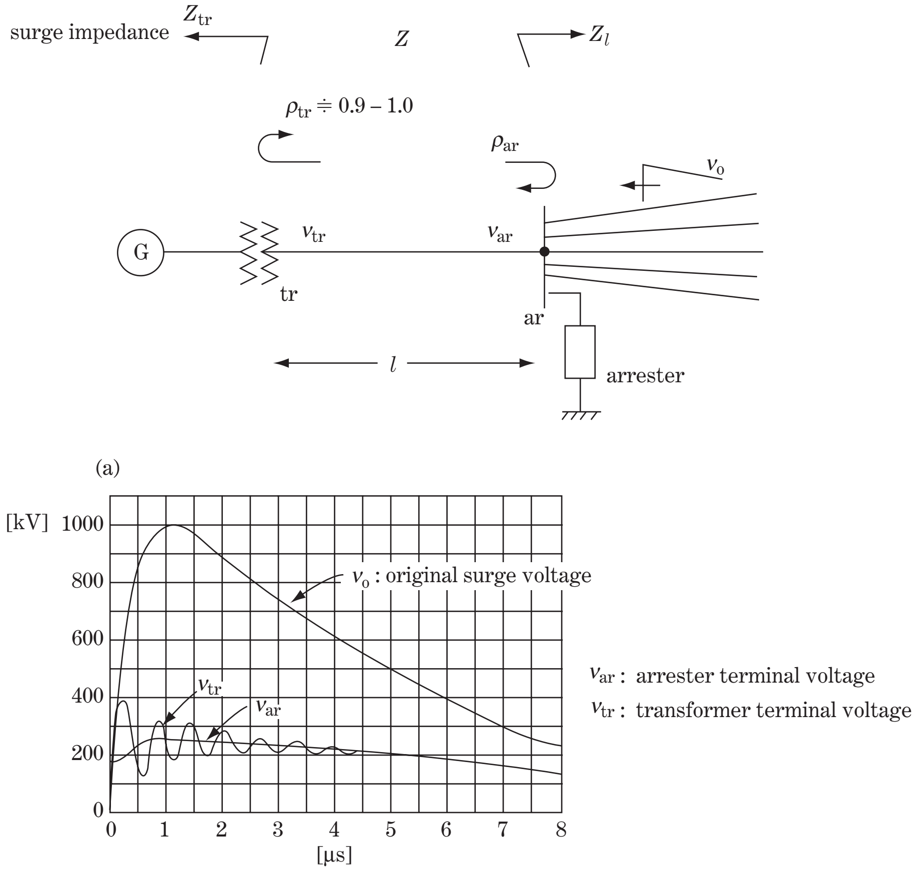
In practical engineering, individual arresters should be selected so that the guaranteed reference voltage (or the duty cycle voltage) exceeds the MCOV or the maximum TOV (U_{rp}).

High-voltage arresters of EHV/UHV classes are also assigned **switching surge durability** by the standards for the arresters, in that generally the **thermal energy absorbing capability** (kJ) at the specified discharge current is type tested. The guaranteed value can be additionally written as point c in Figure 21.6b.

In regard to switching surges, point c can be written as the guaranteed standard nominalbdischarge voltage and current (v_c (kVcrest), i_c (kA crest)) for the switching surges. The standard nominal currents for the switching surges are specified as standard values like 0.5 kA, 1 kA, 2 kAcrest. (A detailed description of the arrester's switching surge durability is omitted.)

21.4.2 Separation effects of station arresters

In regard to surge phenomena, the induced time-changing overvoltage of an arbitrary point is different from that of any other point in the same substation. Accordingly, the voltage at the transformer terminal (or at any other equipment) is different from that of the station arrester terminal. Generally we need to consider that the voltage at the protected insulation may possibly be higher than that at the arrester



(b) surge voltages at a arrester point and a transformer terminals. (simulation)

(c) Typical values of surge impedances

overhead lines : 300–500 Ω
cable lines : 20–60 Ω
transformers : 1000–10 000 Ω
rotating machinery : 500–1500 Ω

Figure 21.7 Separation effects of arresters

terminals due to the travelling distance on connecting leads and the conductor circuit. This rise in voltage is called the **separation effect of an arrester**. This effect obviously lessens the surge protection performance of arresters. Referring to Figure 21.7a, the separation effects can be explained as the behaviour of travelling waves which is deeply linked with (1) the increasing rate of rise of incoming surge μ (kV/μs), (2) the distance l between the arrester and protective equipment (a transformer), and (3) the reflection factor ρ_{tr} of the equipment. The phenomena can be roughly calculated by the equation below, referring to Figures 21.7a, b and c.

We image that the transmitted lightning surge voltage at the arrester point with the time front $T_{front} \fallingdotseq 1.2\mu$ s and the initial steepness of α (kV/μ s typically 200–500 kV/μ s) appears on $t = 0$, so the voltage during the initial small time interval of $0 < t < T_{front}$ ($\fallingdotseq 1.2\mu$ s) can be written $V_{ar}(t) = \alpha \cdot t$(kv). The surge voltage begins to travel to the transformer terminal (distance l) on $t = 0$ and arrive on $t = l/u \equiv T$, so that the surge voltage at the transformer terminal (reflection factor ρ_{tr}) appears on $t = l/u$ (that is $t' = t - l/u = 0$) as is the voltage form of $V_{tr}(t') = (1 + \rho_{tr}) \cdot \alpha \cdot t'$ (kV), where attenuation is neglected.

Assuming $l = 60\,\text{m}, \text{u} = 300\,\text{m}/\mu\,\text{s}$, namely $T = 60/300 = 0.2\,\mu$ s for one way traveling.
$Z_1 = 300\,\Omega$ (for station conductors), $Z_2 = 5000\,\Omega$ (for a transformer)

$$\rho_{tr} = (5000\text{–}300)/5000+300) = 0.9 \text{ (reflection factor)}$$

$$V_{ar}(t) = \alpha \cdot t\,(\text{kV}) \quad \text{for } 0 < t < 2T = 0.4\ \mu\,\text{s}$$

$$V_{ar}(t') = (1+\rho_{tr}) \cdot \alpha \cdot t'\ (\text{kV}) = 1.9\,\alpha \cdot t'\ (\text{kV}) \quad \text{for } 0 < t' < 2T = 0.4\mu\,\text{s}$$

The equation shows that the transformer terminal voltage $V_{tr}(t')$ build up by $1+\rho_{tr} = 1.9$ times of steepness from that of the arrester terminal voltage $V_{ar}(\text{tr})$ during the initial time of up to $2T = 0.4\mu$ s.

Furthermore, V_{ar} continues to increase the magnitude until the interval of $t = T_{front} = 1.2\,\mu$ s and reaches the maximum residual voltage, so that $V_{tr}(t')$ also continues to increase the magnitude for the interval of wave front $1.2\,\mu$ s, while the waveform would become oscillatory mode after $t > 2T = 0.4\mu$ s because the negative reflection waves soon come back from the arrester point. As the result, the transformer terminal voltage $V_{tr}(t)$ could become totally larger magnitude than the arrester terminal voltage V_{ar} and be oscillatory mode by almost doubled steepness.

The above results indicate that the overvoltage stress to the transformer may be more severe than the arrester's protective level because of the separation effect by large destance l or by the large traveling time $T = 1/u$. On the contrary, if the distance l is small, such a severe stress would not appear at the transformer terminal, because the negative reflected waves would soon come back from the arrester point and the V_{tr} and V_{ar} become almost the same.

In practical engineering, the arresters installed very close to the junction tower of a transmission line are quite meaningful as gateway barriers to protect entire substations against lightning surges from the transmission lines. However, these arresters may not be able to protect properly the transformers or any other facilities because of the separation effects.

This is the reason why important transformers or other equipment (including cables) at large stations are preferably protected by exclusively and closely installed **bespoke arresters**. Such arresters for individual transformers would also be very effective at reducing the overvoltage stresses caused by repeated switching surges or rare cases of direct lightning strikes on the station.

Figure 21.8 shows a typical example of GIS for a 500 kV out-door substation with double bus system, the breakers of which are of one-point-breaking type. The arresters are installed at each main transformer terminals as well as at transmission line feeding points.

21.4.3 Station protection by OGWs, and grounding resistance reduction

21.4.3.1 Direct lightning strike on the substation

Surge arresters are generally installed at the gateway point of the substation very close to the first tower of each feeding transmission line and well protect the substation against impinging lightning surges from the transmission lines. However, direct lightning strikes on the substation may be

Courtesy of TEPCO

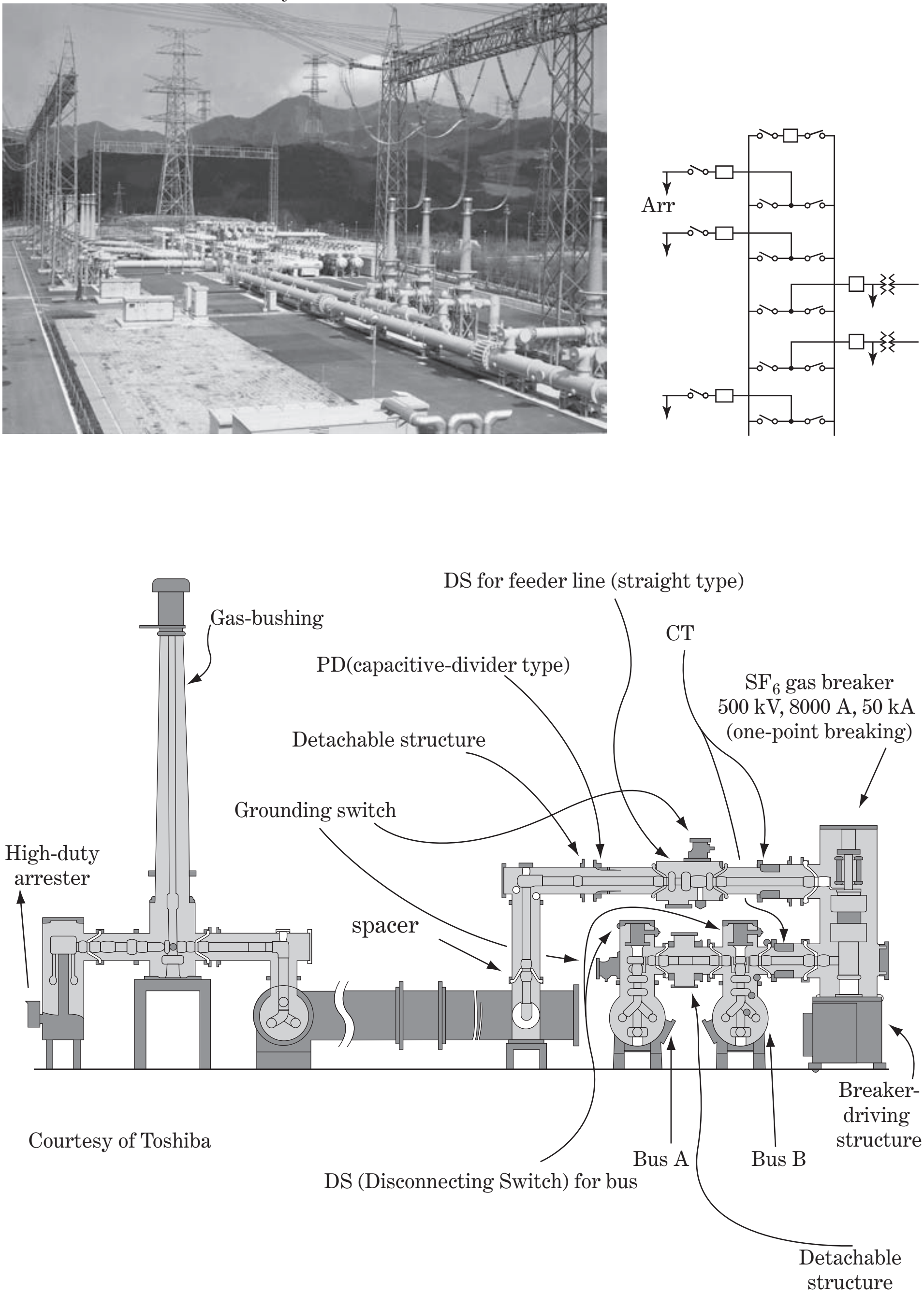


Figure 21.8 GIS substation (500 kV, 8000 A, 63 kA, double bus system)

possible. We cannot prevent such occasions, but can and need to minimize the probability of a strike on the station, or at least protect internal insulation failure (inner insulation failure) as much as possible.

Direct lightning strike on a substation would cause more severe aspects in comparison with a lightning strike on a transmission line, although the probability of occurrence may be smaller.

In case of lightning striking a transmission line, first, arcing horns would limit the magnitude of the surge voltage. Secondly, attenuation of the travelling waves before arrival at the substation would be expected for most cases. The arresters installed at the tower junction point and other points in the substation would appropriately protect the substation and the station apparatus.

On the contrary, the situation of a direct strike on the substation is quite different:

- It is impossible to protect perfectly external (air) insulation failure. Furthermore, the striking point and its aspects cannot be anticipated or limited because the physical configuration as well as electric circuit condition (distribution of surge impedance, for example) are quite complicated.
- Attenuation through long-line travelling cannot be expected.
- Various transition points and surge impedances exist within a narrow area, so unexpected voltage enlargement like the separation effect of arresters may occur.
- The surge energy may exceed the arrester's duty, which probably means cascade failure of the arrester or insulation parts of other equipment.

21.4.3.2 OGWs in station area

The major purpose of OGWs in the area of the substation is to reduce the probability of direct lightning strike on the substation as much as possible, although they cannot be a perfect countermeasure to protect a direct strike on the conductors. The OGWs can reduce the probability of a direct strike on the conductors by the shielding effect, first. Through and ample OGWs can reduce the probability of inverse flashover, second, because the surge impedance is also reduced or the stricken current would be bypassed.

21.4.3.3 Reduction of station grounding resistance and surge impedance

The reduction of the surge impedance at the substation is a vitally important countermeasure to protect or to reduce every kind of surge stress, in particular against direct lightning strike on the substation as well as switching surges and travelling surges from the lines. The reduction of **station surge impedance** is the most important step to reduce the surge impedance Z_2 in Figure 21.4 against incidental travelling surges, for example.

As a numerical check, assuming $Z_1 = 300\Omega$ as the line surge impedance, then the arrester terminal voltages are

$$\begin{aligned} \frac{2Z_2}{Z_1+Z_2}E &= 0.5\,E\,(\text{for}\,Z_2 = 100\Omega) \\ &= 0.28\,E\,(\text{for}\,Z_2 = 50\Omega) \\ &= 0.18\,E\,(\text{for}\,Z_2 = 30\Omega) \end{aligned} \tag{21.3a}$$

The above check clearly indicates the large effect of reducing surge impedance at the station.

In the case of a direct strike on the OGW in the substation, we can apply correspondingly the Equation 20.6 in Figure 20.10 and

$$R_{\text{eff}} = \frac{1}{\dfrac{1}{Z_0} + \dfrac{1}{Z_{\text{OGW}}} + \dfrac{1}{Z_2}} \tag{21.3b}$$

where Z_0: surge impedance of the lightning surge itself ($Z_0 \fallingdotseq 400\Omega$)
Z_{OGW}: total equivalent surge impedance of all the OGW wires
Z_2: surge impedance of the substation

Therefore, R_{eff} can obviously be reduced by a reduction in the station surge impedance Z_2, and accordingly back-flashover would also be reduced.

The resistive ohm value of the substation ground system should also be kept within a specified value for human safety. If ground resistance $R = 1\,\Omega$ and $I_g = 1000\,\text{A}$ are assumed, the induced voltage on the earth conductor could be $V_g = 1\,\text{kV}$, which is too large for the human body to withstand.

Typical practices for the ground system in substations involve **ground pilings or pipes/rods, ground conductors**, **ground meshes**, **ground mats** or a combination of these, and the resistive ohm values are designed to stay within specified values, say 0.5–1Ω by which the surge impedance of the station is also reduced.

Incidentally, some apparatus may be adopted in practical engineering where the external insulation (mostly the bushings) is designed to be a little weaker than the internal insulation in order to avoid internal faults.

21.5 Insulation Coordination Details

We have reached the stage where we can study the details of insulation coordination, taking all the above into account, and including material in previous chapters.

21.5.1 Definition and some principal matters of standards

21.5.1.1 The definition

First of all, the definitions of '**insulation coordination**' by the IEEE and IEC are as follows:

- The selection of insulation strength consistent with expected overvoltages to obtain an acceptable risk of failure (IEEE 1313.1-1996, Standard for insulation coordination).
- The selection of the dielectric strength of equipment in relation to the voltages which can appear on the system for which the equipment is intended and taking into account the service environment and the characteristics of the available protective devices (IEC 71-1, 1993, Insulation coordination).

The meanings of both definitions are the same, although the expressions are different. Needless to say, any other existing national standards (ANSI, JEC, etc.) would have definitions with the same meanings.

Table 21.1 summarizes the criteria in regard to the principal goals of insulation coordination which are probably recognized as common worldwide, and of course is a more concrete expression of the above definitions by the IEEE and IEC as well as by any other standards body with the same engineering expertise.

'Standards' may generally be a kind of engineering consensus or practical policy guidelines for industrial applications by their nature, but they are substantially based on expert detailed theories and technical facts obtained from a great deal of engineering experience in the field of practical application. This is the reason why all the standards in regard to power system insulation are almost the same for principal matters, although the terminology may differ. Readers should appreciate our intention to introduce 'the worldwide consensus and its technical background' instead of the quoted items from the IEEE or IEC standards, although the terminology and the figures are quoted mainly from these two representative standards.

21.5.2 Insulation configuration

Some important definitions for insulation configuration from the IEEE and IEC standards are as follows:

Insulation configuration:

- The complete geometric configuration of the insulation, including all elements (insulating and conducting) that influence its dielectric behaviour. Examples of insulation configurations are **phase-to-ground insulation**, **phase-to-phase insulation** and **longitudinal insulation** (IEEE 1313, similarly IEC 71-1).

Longitudinal insulation:

- An insulation configuration between terminals belonging to the same phase, but which are temporarily separated into two independently energized parts (open-switch device) (IEEE).
- An overvoltage that appears between the open contact of a switch (IEC).

External insulation:

- The air insulation and the exposed surfaces of solid insulation of equipment, which are both subject to dielectric stresses of atmospheric and other external conditions such as contamination, humidity, vermin, etc. (IEEE).
- The distances in atmospheric air, and the surfaces in contact with atmospheric air of solid insulation of the equipment which are subject to dielectric stresses and to the effects of atmospheric and other external conditions, such as pollution, humidity, vermin, etc. (IEC).

Internal insulation:

- Internal insulation comprises the internal solid, liquid, or gaseous elements of the insulation of equipment, which are protected from the effects of atmospheric and other external conditions such as contamination, humidity, and vermin (IEEE, IEC similarly).

Self-restoring insulation:

- Insulation that completely recovers its insulating properties after a disruptive discharge caused by the application of a test voltage; insulation of this kind is generally, but necessarily, **external insulation** (IEEE).

Table 21.1 The basic concept of insulation design criteria (general consensus)

	Overhead transmission lines			Substation and substation equipment		
	Lightning overvoltages	**Switching overvoltages**	**Temporary overvoltages**	**Lightning overvoltages**	**Switching overvoltages**	**Temporary overvoltages**
Matters of policy	*Insulation failures are unavoidable, but should be within reasonable failure rate *Flashover passes should be limited only through arcing horns so that damage to conductors or insulators is avoided	*Insulation failure should be entirely avoided without exception		*Insulation failures caused by lightning strikes to the overhead lines should be entirely avoided *Lightning strikes directly to the stations may be unavoidable, but countermeasures to minimize the probability of insulation failure, and in particular to protect internal insulation, are required as much as possible	*Insulation failures should be entirely avoided	
				*Power cables have to be entirely protected		
Major countermeasures	*OGW *Arcing horns *Tower impedance reduction	*Mainly dependent by the countermeasure at the substation. *The gap lengths of arcing horns should be set to avoid discharge by the switching surges or by any temporary overvoltages.		*OGW *Arresters *Station neutral grounding system (meshes, counterpoise)	*Arresters (gap-less) *Breakers (resistive tripping/closing) *Station neutral grounding system (meshes, counterpoise)	*Shunt reactors and capacitors *Tap-changing transformers *AVR, AQR, V–Q control (local) * Load dispatching (total system) * V–Q control (total system)
Principles for withstanding voltages	*The discharge probability function of a self–restoring insulation based on a Gaussian cumulative frequency distribution is adopted. In typical practice, the voltages of nominalized deviation 3σ value based on the 50% discharge voltages are applied			*Station equipment has to be guaranteed by the withstanding insulation levels specified by the authorized standards such as in Tables 3.2A, B, C		

- Insulation which completely recovers its insulating properties after a disruptive discharge (IEC).

Non-self-restoring insulation:

- An insulation that loses its insulating properties or does not recover them completely after disruptive discharge caused by the application of a test voltage; insulation of this kind is generally, but not necessarily, **internal insulation** (IEEE).
- Insulation which loses its insulating properties, or does not recover them completely, after a disruptive discharge (IEC).

21.5.3 Insulation withstanding level and BIL, BSL

We have studied in the previous sections various kinds of overvoltages and the countermeasures to mitigate them or to protect insulation, in particular to protect internal insulation. Taking all this into consideration, concepts of **insulation strength** and **insulation-withstanding level**, and, furthermore, clear guidelines for the latter, that is **standard withstand voltages** or **standard insulation levels**, are introduced.

The guideline criteria have to be able to offer the important role of **the measures for insulation strength and withstanding level** at least for the following four important engineering procedures:

a) Measures **to determine or to select entire levels of insulation strength** of transmission lines and substations belonging to the same operating voltages of the individual power system.

b) Measures **to mitigate the actual possible overvoltage stresses** within the selected insulation levels.

c) Measures **to specify the required insulation strength** of individual equipment or facilities.

d) Measures **to prove the specified insulation-withstanding strength** by which required insulation levels of equipment or facilities can be tested and guaranteed.

The insulation strength or insulation withstanding voltage levels are expressed in terms of three representative categories of overvoltages, namely BIL, BSL and **the highest power frequency voltages,** as principal concepts of insulation coordination.

The IEEE definitions of BIL and BSL are as follows:

- **BIL** (Basic Lightning Impulse Insulation Level): The electrical strength of insulation expressed in terms of the crest value of a standard lightning impulse under standard atmospheric conditions. BIL may be expressed as either statistical or conventional.
- **BSL** (Basic Switching Impulse Insulation Level): The electrical strength of insulation expressed in terms of the crest value of a standard switching impulse. BSL may be expressed as either **statistical** or **conventional**.
- **Conventional BIL**: The crest value of a standard lightning impulse for which the insulation shall not exhibit disruptive discharge when subjected to a specific number of applications of this impulse under specified conditions, applicable specifically to non-self-restoring insulations.

- **Conventional BSL**: The crest value of a standard switching impulse for which the insulation does not exhibit disruptive discharge when subjected to a specific number of impulses under specified conditions, applicable to non-self-restoring insulations.

21.5.4 Standard insulation levels and their principles

Tables 21.2 A, B, C show the standard withstand voltages for power systems specified by IEEE 1313 and IEC 71-1.

The IEEE as well as IEC standards are divided into the following two parts:

a) IEEE, for the specified standard highest voltages:

- Class I: Medium (1–72.5 kV) and high (72.5–242 kV) voltages
- Class II: EHV and UHV: 242 kV

b) IEC, for specified highest voltages for equipment:

- Range I: Above 1 kV to 245 kV
- Range II: Above 245 kV

The contents as well as the background reasons of both standards can be said to be the same, although the terminologies are slightly different. Therefore, we show the above Class I by the IEEE and Range I by the IEC together in the same Table 21.2A. Tables 21.2B and C are their standards for voltages above 245 kV.

The standard withstanding BIL and BSL of equipment have to be proved also by overvoltage tests with the same specified wave shapes. The IEC and IEEE definitions for **the standard wave shapes for BIL and BSL tests** are the same, and are given in Table 21.2C:

- **Standard lightning impulse: 1.2/50 μs.**

  The wave shape having a time to peak of 1.2 μs and a time to half-value of 50 μs.

- **Standard switching impulse: 250/2500 μs.**

  The wave shape having a time to peak of 250 μs and a time to half-value of 2500 μs.

It is fair to say that the above outlines of insulation coordination by the IEC and IEEE standards including Tables 21.2A–D are almost the same as each other and it is because the above described outlines are based on theory and facts that they have been recognized as the worldwide consensus. Any other national standards or recommendations would actually also be equivalent.

21.5.5 Comparison of insulation levels for systems under and over 245 kV

In Table 21.2A for the systems under 245 kV, standard short-duration power frequency withstand voltages as well as standard lightning impulse withstand voltages are assigned. On the other hand, in Tables 21.2B and C for systems over 245 kV, standard switching impulse withstand voltages are assigned instead of short-duration power frequency withstand voltages. The reason for this may be explained as follows.

Table 21.2A IEC 71-1 and IEEE 1313: Standard withstand voltages for power systems of up to 245 kV

IEC 71-1, 1993-12 Insulation coordination Part 1: Definitions, principles and rules. Range 1: $1\,\text{kV} < U_m \le 245\,\text{kV}$					
Highest voltage for equipment U_m		Standard short-duration power frequency withstand voltage		Standard lightning impulse withstand voltage	
IEEE 1313.1-1996 Insulation Coordination – Definitions, principles and rules, Class 1: $15\,\text{kV} < V_m \le 242\,\text{kV}$					
Maximum system voltage (phase-to-phase) V_m		**Low-frequency, short-duration withstand voltage (phase-to-ground)**		**Basic lightning impulse insulation level (phase-to-phase) BIL**	
kV, rms value		**kV, rms value**		**kV, crest value**	
IEC	**IEEE**	**IEC**	**IEEE**	**IEC**	**IEEE**
3.6		10		20, 40	
7.2		20		40, 60	
12		28		60, 75, 95	
17.5	15	38	34	75, 95	95, 110
24	26.2	50	50	95, 125, 145	150
36	36.2	70	70	145, 170	200
52	48.3	95	95	250	250
72.5	72.5	140	95	325	250
			140		350
			140		350
123	121	(185)	185	450	450
		230	230	550	550
		(185)		(450)	
145		230	230	550	450
	145	275	275	650	550
			325		650
		(230)	230	(550)	550
170	169	275	275	650	650
		325	325	750	750
		(275)	275	(650)	650
		(325)	325	(750)	750
245	242	360	360	850	825
		395	395	950	900
		460	480	1050	950, 1050

21.5.5.1 Systems under 245 kV (Table 21.2A)

1a) There is a tendency for TOVs to be quite high. In particular, many lower voltage systems are non-effective neutral grounding systems, so the **ground–fault factor** (the ratio of the highest power frequency voltage on an unfaulted phase during a line-to-ground fault to the phase-to-ground power frequency voltage without the fault) is generally large.

Table 21.2B IEC 71-1: Standard withstand voltages for power system of over 245 kV, Range 2: $U_m > 245$ kV

Highest Voltage for equipment U_m **kV (rms value)**	**Standard switching impulse withstand voltage**			**Standard Lightning impulse withstand voltage kV (peak value)**
	Longitudinal insulation kV (peak value)	**Phase-to-earth kV (peak value)**	**Phase-to-phase ratio to the phase-to-earth peak value**	
300	750	750	1.5	850, 950
		850		950, 1050
362	850	850	1.5	950, 1050
		950		1050, 1175
420	850	850	1.6	1050, 1175
	950	950	1.5	1175, 1300
		1050	1.5	1300, 1425
525	950	950	1.7	1175, 1300
		1050	1.6	1300, 1425
		1175	1.5	1425, 1550
765	1175	1300	1.7	1675, 1800
		1425	1.7	1800, 1950
		1550	1.6	1950, 2100

Furthermore, especially in systems under 36 kV, various temporary and irregular electrical conditions (e.g. neutral terminal opening, conductor crossing from higher voltage systems) as well as mechanical damage to network facilities are apt to occur so that some reasonable margin for a.c. voltages may be required for social and security purposes. These are the reasons why the standard short-duration power frequency withstand levels are assigned relatively larger values.

1b) Switching impulse withstand levels (BSL) have no meaning in this class because they can be covered by BIL. Next, the ratio of $\mathrm{BIL}/\left\{\left(\sqrt{2}/\sqrt{3}\right)U_m\right\}$ for this voltage class is larger than that for the higher voltage class of Tables 21.2B and C.

As a numerical check of $\mathrm{BIL}/\{(\sqrt{2}/\sqrt{3})U_m\}$:

$$\text{Table 21.2A}: \; 200/\{(\sqrt{2}/\sqrt{3})36\} = 6.80, \quad 325/\{(\sqrt{2}/\sqrt{3})72.5\} = 5.49,$$

$$850/\{(\sqrt{2}/\sqrt{3})245\} = 4.25$$

$$\text{Tables 21.2B, C}: \; 850/\{(\sqrt{2}/\sqrt{3})300\} = 3.47, \quad 1300/\{(\sqrt{2}/\sqrt{3})525\} = 3.03,$$

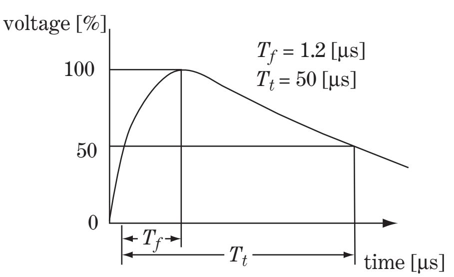
$$1800/\{(\sqrt{2}/\sqrt{3})800\} = 2.76$$

In other words, BILs of class 1 systems are relatively high, so BSLs must be relatively high. On the other hand, switching overvoltages are obviously proportional to the system operating voltages so that switching surges would be lower for the systems of this class. These are the reasons why BSL is omitted for the lower voltage systems of class 1.

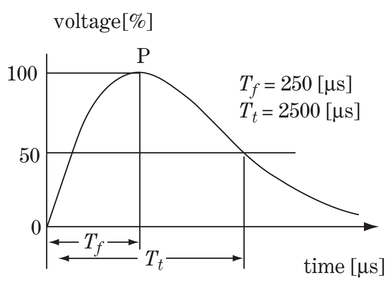
Table 21.2C IEEE 1313: Standard withstand voltages for power systems of over 242 kV, Class 2: $V_m > 242$ kV.

Maximum system voltage (phase-to-phase) V_m kV, rms	Basic switching impulse insulation level (phase-to-ground) BSL kV, peak	Basic lightning impulse insulation level (phase-to-ground) BIL kV, peak
362	650 750 825 900 975 1050	900 975 1050 1175 1300
550	1300 1425 1550 1675 1800	1175 1300 1425 1550
800	1300 1425 1550 1675 1800	1800 1925 2050

T_f: the time-to-crest value (virtual time)
T_t: the time-to-half value (virtual time)



Standard lightning impulse test voltage 1.2/50 impulse



Standard switching impulse test voltage 250/2500 impulse

21.5.5.2 Systems over 245 kV (Tables 21.2B and C)

2a) Low-frequency short-duration withstand voltage is omitted, because, nowadays, all EHV and UHV class systems are treated under the solidly neutral grounding method (probably without exception) so that the ground fault factor is rather small.

2b) On the other hand, BSL is strictly assigned to this class. The reason can be explained by the inverse of item (1b) above. That is, first, BIL has been considerably reduced (due to the advance of arrester properties in particular) in EHV and UHV. Secondly, switching surges are proportional to the operating voltages and so are relatively large in comparison with the insulation levels. These are the reasons why BSL has to be assigned quite important items. In IEC 71-1, phase-to-phase and longitudinal BSL as well as phase-to-earth BSL are also assigned.

As a numerical check of BSL(phase-to-earth)/$\{(\sqrt{2}/\sqrt{3})U_m\}$:

	(i)BIL/$\{(\sqrt{2}/\sqrt{3})U_m\}$	(ii)BSL(phase-to-earth)/$\{(\sqrt{2}/\sqrt{3})U_m\}$
300 kV :	$(850-1050)/\{(\sqrt{2}/\sqrt{3})300\} = 3.47-4.29$,	$(750-850)/\{(\sqrt{2}/\sqrt{3})300\} = 3.06-3.48$
525 kV :	$(1175-1550)/\{(\sqrt{2}/\sqrt{3})525\} = 2.74-4.15$,	$(950-1175)/\{(\sqrt{2}/\sqrt{3})525\} = 2.22-2.74$
800 kV :	$(1800-2050)/\{(\sqrt{2}/\sqrt{3})800\} = 2.76-3.14$,	$(1300-1800)/\{(\sqrt{2}/\sqrt{3})800\} = 1.99-2.76$

This leads to the following:

- The BIL ratio to the operating voltage is remarkably low for EHV and UHV systems, which means the reduction of the insulation level has been realized; this owes much to advanced arrester technology to lower the protective levels.
- The BSL ratio is very close to the BIL ratio. Accordingly, the following points have to be carefully confirmed as essentially important for EHV and UHV systems:

– **EHV and UHV arresters** are required to limit the lightning impulse level to lower values, while withstanding the large switching surge (the capability to absorb large switching surge energy).

– **EHV and UHV breakers** are required to limit the switching surges to within the specified levels. For example, UHV breakers of 500 kV class are required to limit the BSL to within typically 2.5 by adopting resistive closing/tripping.

– Of course, all the other **EHV and UHV station equipment** has to guarantee the insulation levels specified in Tables 21.2B and C, including BSL.

21.5.5.3 Evaluation of degree of insulation coordination

The degree of coordination can be measured or evaluated by the **protective ratio (PR)** (IEEE PC62.22). That is,

$$\text{PR} = (\text{insulation withstanding level})/(\text{voltage at protected equipment})$$

or the ratio of the insulation strength of the protected equipment to the overvoltages appearing across the insulation. The **voltage at protected equipment** is equal to **arrester protective level**, if the **separation effect** is insignificant:

$$\text{PR}_l = \text{BIL/LPL} \qquad (\text{ratio for BIL : acceptable level } 1.15)$$

where LPL is the lightning impulse protective level

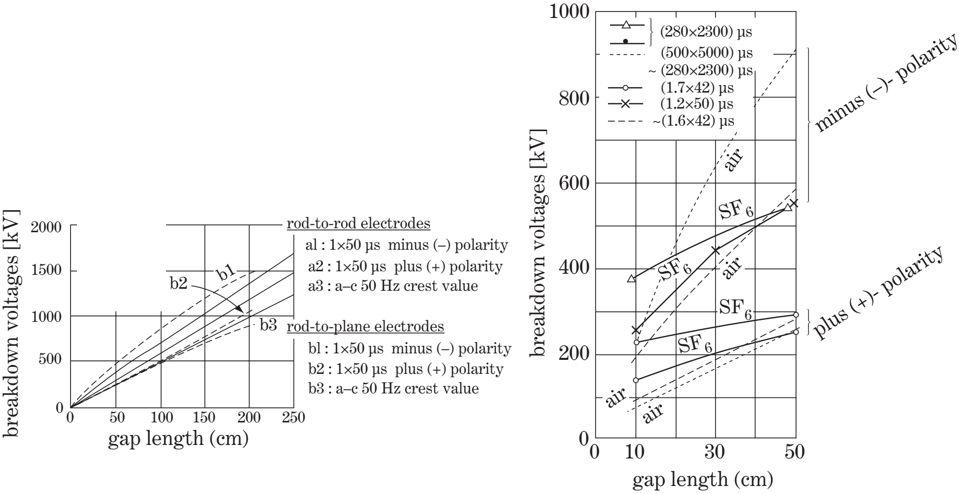
$$\text{PR}_s = \text{BSL/SPL} \qquad (\text{ratio for BIL : acceptable level } 1.2)$$

where SPL is the switching impulse protective level

The denominators of the above ratios are expected largest voltages possible. The insulation coordination of a power system can be evaluated by deriving the PR of individual lines and equipment by detailed overvoltage analysis.

Note that, besides BIL and BSL, a **chopped-wave overvoltage test** may be additionally introduced as an optional requirement for special conditions.

The standard chopped-wave impulse voltage shape is a standard lightning impulse that is intentionally interrupted at the tail by sparkover of a gap or other chopping equipment. Usually the time to chop is 2–3 μs. (Further details are omitted as the matter is beyond this book.)



(a) Lightning impulse and a.c. flashover characteristics (P. Jacottet and W. Weicker, ETZ 1940)

(b) SF_6 gas flashover characteristics (T. Takuma, IEEE 1972)

Figure 21.9 Flashover characteristics (typical example) ((b) courtesy of Mr T. Takuma)

21.5.5.4 Breakdown voltage characteristics

All the transmission lines as well as station facilities and equipment have to be designed to satisfy the applied standard insulation levels. For this purpose, breakdown voltage characteristics under various conditions are required as **essential data of insulation characteristics**. The **breakdown characteristics** of any insulation materials (air, gas, oil, paper, porcelain, etc.) are affected by the **shapes of electrodes** and the **atmosphere** (pressure, humidity, temperature, etc.) so a huge amount of valuable experimental data has been accumulated from the past. Figure 21.9 is an example showing the breakdown characteristics of air and SF_6 gas.

21.5.5.5 Cable insulation

The power cables contain non-self-restoring insulation, so any breakdown of cable insulation would require extensive outage time for repairs at a high cost. Therefore insulation failure of power cables for network lines as well as for power station use should be avoided.

Cable circuits have a low surge impedance of typically 40–50 Ω so that surges coming from overhead lines will be reduced significantly at the line–cable junction. However, switching surges originating in the vicinity of cable lines at a substation may be largely reflected at the junction point to the transmission lines (see Section 18.7). Switching surge phenomena in the substation area are generally quite complicated. Metal–oxide gap-less arresters can provide excellent cable protection, while the arrester is required to absorb the relatively large thermal energy $\left(\frac{1}{2}CV^2\right)$ caused by the high-frequency oscillatory overvoltages.

Regardless, insulation coordination of the power system network including cable lines of large capacitance have to be carefully examined. The overvoltage behaviour of cable lines will be discussed again in Section 23.6.

Today, all the facilities of the primary circuit of a power system (transmission lines, power cable lines, power station/substation equipment and related configurations) are planned, specified, designed, manufactured, tested, installed and operated based on the insulation coordination standards and the related individual standards. In other words, all the engineering activities in regard to power systems are deeply correlated with the above concept of insulation coordination.

21.6 Transfer Surge Voltages Through the Transformer, and Generator Protection

If lightning surges or switching surges were to flow into the high-tension bushing terminal of a transformer, it could put **serious stress on the transformer windings**. Furthermore, an **induced transfer overvoltage** would appear at the low-tension bushing terminal and threaten the insulation of lower voltage side equipment. This is a serious problem in low-tension-side insulation coordination. In particular, in the case of generating plant, the rated voltage of the low-tension side of the generator terminals are relatively low (say, 10–30 kV) (in other words, a large transformation ratio) so that the insulation level is quite low in comparison with that of the high-tension side. Appropriate countermeasures to protect generators and other low-voltage equipment are therefore required. We study this problem in this section.

21.6.1 Electrostatic transfer surge voltage

The transformer we studied in Chapter 5 just had the characteristics of the power frequency based on inductance L of the coil windings, neglecting stray capacitances C. We need to consider that the behaviour of the transformer in the surge frequency zone is dominated by stray capacitance C instead of inductance L of the coil windings. Accordingly, we need to establish some equivalent circuit of the transformer based on capacitance C.

21.6.1.1 Equivalent circuit (single phase transformer)

Figure 21.10a shows a typical equivalent circuit for high-frequency phenomena. This is a distributed circuit with elemental capacitances from the high-tension (HT) coil to the low-tension (LT) coil as well as that from the LT coil to grounding earth, where the capacitances from the HT coil to grounding earth are not written because they can be treated as part of the connected HT outer circuit. As we are not applying powerful computers in this book, we need to simplify Figure 21.10a to 21.10b where the distributed capacitances are concentrated by C_{12} (total capacitance from the HT to LT coil) and C_2 (total capacitance from the LT coil to grounding earth). Then we examine two different cases in Figure 21.10b:

- Circuit 1 (switch S_1 closed, S_2 open): This is the case where terminals m and n are connected together, while the earth terminal is open. In the figure, the scale x shows the distributed coil position

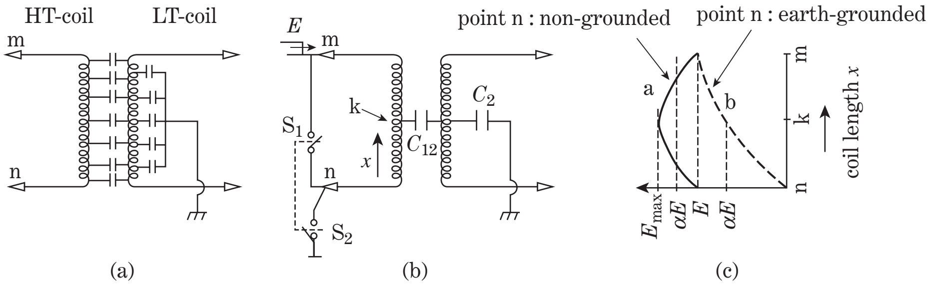


Figure 21.10 Equivalent circuit of transformer for high-frequency phenomena

being measured from point n to point m. When the surge voltage travelling from the HT line arrives at the connected points m and n as the magnitude of E, the surges then rush into the primary coil simultaneously from m and n and frontally meet point k so that the voltage at k obtains the largest value $E_{max}(=2E$ if attenuation is neglected). The initial voltage distribution in the HT coil is shown as curve a in Figure 21.9c. The averaged value of the distributed voltage can be written as $E = \alpha E$, where $\alpha = 1.4 - 1.6 > 1.0$.

- Circuit 2 (switch S_1 open, S_2 closed): This is the case where terminal n is connected only to the earth and the surge E is injected at point m. The surge voltage E travels through the HT coil from point m to n so that the initial voltage distribution is as curve b in Figure 21.9c. The averaged value of the distributed voltage can be written as $E = \alpha E$, where $\alpha = 0.5 - 0.7 < 1.0$.

As a result, we can presume that the averaged voltage αE is injected at point k of circuits 1 and 2 as follows:

$$\left.\begin{aligned}&\text{for circuit 1}: \alpha = 1.4 - 1.6\\&\text{for circuit 2}: \alpha = 0.5 - 0.7\end{aligned}\right\} \tag{21.4}$$

21.6.1.2 Calculation of electrostatic transfer surge voltage (single phase transformer)

Our problem is shown in Figure 21.11a, where surge voltages $E_m(t)$ and $E_n(t)$ arrive at terminals m and n simultaneously, and the equation for deriving the electrostatically induced voltage at the LT coil terminal is as follows:

$$\left.\begin{aligned}{}_1v_m &= E_m\\{}_1v_n &= E_n\end{aligned}\right\} \tag{21.5a}$$

Now we can transform the set voltages E_m and E_n into the line-to-ground travelling wave $E'(t)$ and line-to-line travelling wave $E''(t)$, and Figure 21.11a can be divided into the Figures 21.11b and c:

$$\left.\begin{aligned}E_m &= E' + E''\\E_n &= E' - E''\end{aligned}\right\}① \quad \left.\begin{aligned}&\text{line-to-ground travelling wave} \quad E' = \frac{1}{2}(E_m + E_n)\\&\text{line-to-line travelling wave} \quad E'' = \frac{1}{2}(E_m - E_n)\end{aligned}\right\}② \tag{21.5b}$$

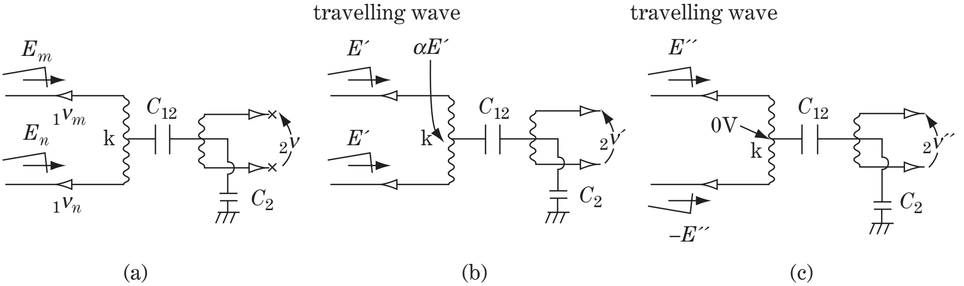


Figure 21.11 Transfer voltage from HT coil to LT coil

For Figure 21.11b, we can quote the result of circuit 1: that is, voltage $\alpha E'$ is charged at point k:

$$_2v' = \frac{C_{12}}{C_{12}+C_2}\cdot \alpha E' = \frac{\alpha}{2}\cdot\frac{C_{12}}{C_{12}+C_2}(E_m+E_n) \tag{21.6a}$$

For Figure 21.11c, voltages E'' and $-E''$ are injected at points m and n respectively, so the frontal voltage at k becomes zero. In other words, this is a special case of $\alpha = 0$ on circuit 2. Namely,

$$_2v'' = \frac{C_{12}}{C_{12}+C_2}\cdot 0 = 0 \tag{21.6b}$$

Thus the solution of Figure 21.11a is derived as the addition of the results by Figures 21.11b and c. Namely,

$$_2v = {_2v'} + {_2v''} = \frac{C_{12}}{C_{12}+C_2}\cdot \alpha E' = \frac{\alpha}{2}\cdot\frac{C_{12}}{C_{12}+C_2}(E_m+E_n) \tag{21.6c}$$

21.6.1.3 Calculation of electrostatic transfer surge voltage (three-phase transformer)

Figure 21.12 is a typical connection diagram of a main transformer for thermal or hydro-generating plants.

The surge voltages $E_a(t)$, $E_b(t)$, $E_c(t)$ arrive simultaneously at the HT terminal bushings. Our problem is to calculate the transfer surge voltages induced by the generator-side LT terminal bushings.

We can quote the result of Equation 21.6 for the initial transfer surge voltage induced at the LT side:

$$\left.\begin{aligned} &\text{phase a} \quad {_2v_a} = \frac{\alpha}{2}\cdot\frac{C_{12}}{C_{12}+C_2}(E_a + {_1v_n}) \\ &\text{phase b} \quad {_2v_b} = \frac{\alpha}{2}\cdot\frac{C_{12}}{C_{12}+C_2}(E_b + {_1v_n}) \\ &\text{phase c} \quad {_2v_c} = \frac{\alpha}{2}\cdot\frac{C_{12}}{C_{12}+C_2}(E_c + {_1v_n}) \end{aligned}\right\} \tag{21.7}$$

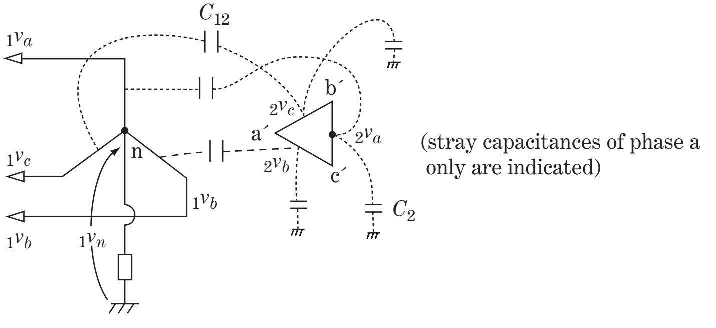


Figure 21.12 Stray capacitance between HT and LT windings

where ${}_1v_n$: surge voltage at the neutral point n
or by symmetrical components

$$\left.\begin{aligned} {}_2v_0 &= \frac{1}{3}\left({}_2v_a + {}_2v_b + {}_2v_c\right) \\ &= \frac{\alpha}{2}\cdot\frac{C_{12}}{C_{12}+C_2}\left(E_0 + {}_1v_n\right) \\ {}_2v_1 &= \frac{\alpha}{2}\cdot\frac{C_{12}}{C_{12}+C_2}\cdot E_1 \\ {}_2v_2 &= \frac{\alpha}{2}\cdot\frac{C_{12}}{C_{12}+C_2}\cdot E_2 \end{aligned} \quad \text{where} \quad \begin{bmatrix} E_0 \\ E_1 \\ E_2 \end{bmatrix} = \frac{1}{3}\begin{bmatrix} 1 & 1 & 1 \\ 1 & a & a^2 \\ 1 & a^2 & a \end{bmatrix}\cdot\begin{bmatrix} E_a \\ E_b \\ E_c \end{bmatrix}\right\} \tag{21.8}$$

Equations 21.7 and 21.8 are the resulting general equations giving the transfer surge voltages appearing at the bushings of the LT side, while the LT bushing terminals are in the open condition.

By applying the above derived equations, the equations for calculating the transfer voltages under different terminal conditions can be derived, which are summarized in Table 21.3.

Regarding the derivation processes shown in Table 21.3, the neutral point n should be carefully treated as a surge transition point. The transmittal wave coefficient at point n is obviously different for each case. For example, the coefficient of the voltage, v_n at the neutral point n is 2 for case 5, 2/3 for case 6, 4/3 for case 7, and so on, as explained in the table.

We can find the equation for calculating the transfer surge voltages appearing on the LT side under various different conditions on the HT voltage side using the above general equation and Table 21.3. The resulting equations for the seven cases can be summarized as the general equation below.

The transfer surge voltage from the HT to the LT side (the LT side is in open mode) is

$${}_2v = k\alpha\cdot\frac{C_{12}}{C_{12}+C_2}E \tag{21.9}$$

where α : given by Equation (21.4)
k : transfer coefficient given in Table 21.3

For example, for case 6 phase a, $k = 5/6$, and for phase b, $k = 2/6$, with

$$k\alpha\cdot\frac{C_{12}}{C_{12}+C_2} \ : \ \text{transfer voltage ratio}$$

Incidentally, the transfer voltages for each case with $C_{12} = 6000\,\text{pF}$, $C_2 = 3000\,\text{pF}$, $\alpha = 0.5$ (for the solidly neutral grounding system) and $\alpha = 1.5$ (for the neutral ungrounded system) are shown in the table as supplemental references. In the case of the high-resistive neutral grounding system, α is initially between 1.5 and 0.5, and soon (after the wavefront passes) becomes very close to 0.5.

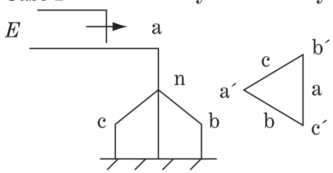
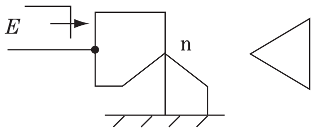
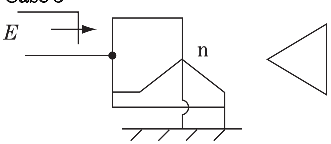
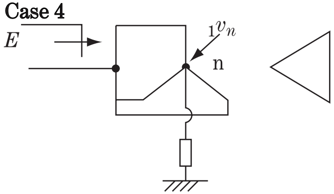
Let us consider a trial calculation for a 275 kV class power station, with main transformer 275 kV/24 kV, y–Δ windings, solidly neutral grounding system. Then

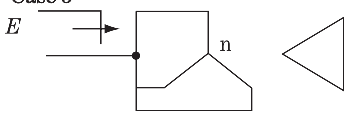
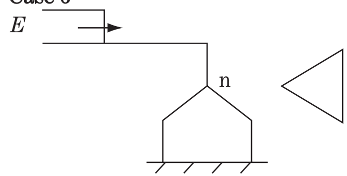
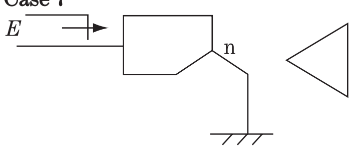
$$C_{12} = 6000\,\text{pF}, \quad C_2 = 3000\,\text{pF}, \quad \alpha = 0.7, \quad k = \frac{1}{2}$$

From Equation 21.9

$${}_2v_a = \frac{\alpha}{2}\cdot\frac{6000}{6000+3000}E = \frac{1}{3}\alpha E = 0.23E \tag{21.10}$$

Table 21.3 Electrostatic transfer voltage (calculation formula)

	Surge voltage (conditions)	Transfer voltage from HT side to LT side (phase voltages, neutral voltage) $v = k\alpha \cdot \frac{C_{12}}{C_{12}+C_2}E$	Transfer voltage $\left(C_{12} = 6000[\text{pF}],\ C_2 = 3000[\text{pF}],\ k = \frac{C_{12}}{C_{12}+C_2} = \frac{2}{3}\right)$
Case 1 Primary secondary 	$_1v_a = E,\ _1v_b = {_1v_c} = 0$ $_1v_n = 0$	$_2v_a = \frac{\alpha}{2} \cdot \frac{C_{12}}{C_{12}+C_2}E,$ $_2v_b = {_2v_c} = 0$ $\therefore {_2v_0} = \frac{\alpha}{6} \cdot \frac{C_{12}}{C_{12}+C_2}E$ $(k = 1/2)$	$\alpha = 0.5$ $_2v_a = \frac{0.5}{2} \cdot \frac{2}{3}E = \frac{1}{6}E$ $_2v_0 = \frac{0.5}{6} \cdot \frac{2}{3}E = \frac{1}{18}E$
Case 2 	$_1v_a = {_1v_c} = E, {_1v_b} = 0$ $_1v_n = 0$	$_2v_a = {_2v_c} = \frac{\alpha}{2} \cdot \frac{C_{12}}{C_{12}+C_2}E,$ $_2v_b = 0$ $\therefore {_2v_0} = \frac{\alpha}{3} \cdot \frac{C_{12}}{C_{12}+C_2}E$ $(k = 1/2, 1/3)$	$\alpha = 0.5$ $_2v_a = {_2v_c} = \frac{1}{6}E$ $_2v_0 = \frac{1}{9}E$
Case 3 	$_1v_a = {_1v_b} = {_1v_c} = E$ $_1v_n = 0$	$_2v_a = {_2v_b} = {_2v_c} = \frac{\alpha}{2} \cdot \frac{C_{12}}{C_{12}+C_2}E$ $\therefore {_2v_0} = \frac{\alpha}{2} \cdot \frac{C_{12}}{C_{12}+C_2}E$ $(k = 1/2)$	$\alpha = 0.5$ $_2v_a = {_2v_b} = {_2v_c} = \frac{1}{6}E$ $_2v_0 = \frac{1}{6}E$
Case 4 	$_1v_a = {_1v_b} = {_1v_c} = E$ $_1v_n$: voltage at point n $(E_n \neq 0)$	$_2v_a; = {_2v_b} = {_2v_c}$ $= \frac{\alpha}{2} \cdot \frac{C_{12}}{C_{12}+C_2}(E + {_1v_n})$ $\therefore {_2v_0} = \frac{\alpha}{2} \cdot \frac{C_{12}}{C_{12}+C_2}(E + {_1v_n})$ $(k = 1/2)$	early duration: the same as in case 5 later duration: the same as in case 2

Case 5 	${}_1v_a = {}_1v_b = {}_1v_c = E$ ${}_1v_n = 2E$ reflection factor at point n is 2	${}_2v_a = {}_2v_b = {}_2v_c = \frac{3\alpha}{2} \cdot \frac{C_{12}}{C_{12}+C_2} E$ $\therefore {}_2v_0 = \frac{3\alpha}{2} \cdot \frac{C_{12}}{C_{12}+C_2} E$ $(k = 3/2)$	$\alpha = 1.5$ ${}_2v_a = {}_2v_b = {}_2v_c = \frac{3 \times 1.5}{2} \cdot \frac{2}{3} E$ $= 1.5E$
Case 6 	${}_1v_a = E, {}_1v_b = {}_1v_c = 0$ $Z_1 : Z_2 = 2:1$ then ${}_1v_n = \frac{2Z_2}{Z_1+Z_2} E = \frac{2}{3} E$ Z_1, Z_2: surge impedance before and after point n	${}_2v_a = \frac{\alpha}{2} \cdot \frac{C_{12}}{C_{12}+C_2}\left(E + \frac{2}{3}E\right)$ $= \frac{5\alpha}{6} \cdot \frac{C_{12}}{C_{12}+C_2} E$ ${}_2v_b = {}_2v_c$ $= \frac{\alpha}{2} \cdot \frac{C_{12}}{C_{12}+C_2}\left(0 + \frac{2}{3}E\right)$ $= \frac{2\alpha}{6} \cdot \frac{C_{12}}{C_{12}+C_2} E$ $\therefore {}_2v_0 = \frac{\alpha}{2} \cdot \frac{C_{12}}{C_{12}+C_2} E \, (k = 1/2, 5/6, 1/3)$	${}_2v_0 = 1.5E$ $\alpha = 1.5$ ${}_2v_a = \frac{5}{6} E.$ ${}_2v_b = {}_2v_c = \frac{1}{3} E$ ${}_2v_0 = \frac{1}{3} E$
Case 7 	${}_1v_a = {}_1v_c = E, \ {}_1v_b = 0$ $Z_1 : Z_2 = 1:2$ then ${}_1v_n = \frac{2Z_2}{Z_1+Z_2} E = \frac{4}{3} E$	${}_2v_a = {}_2v_c$ $= \frac{\alpha}{2} \cdot \frac{C_{12}}{C_{12}+C_2}\left(E + \frac{4}{3}E\right)$ $= \frac{7\alpha}{6} \cdot \frac{C_{12}}{C_{12}+C_2} E$ ${}_2v_b = \frac{\alpha}{2} \cdot \frac{C_{12}}{C_{12}+C_2}\left(0 + \frac{4}{3}E\right)$ $= \frac{4\alpha}{6} \cdot \frac{C_{12}}{C_{12}+C_2} E$ ${}_2v_0 = \alpha \frac{C_{12}}{C_{12}+C_2} E \quad (k = 1/2, 7/6, 4/6, 1)$	$\alpha = 1.5$ ${}_2v_a = {}_2v_c = \frac{1}{6} E$ ${}_2v_b = \frac{2}{3} E$ ${}_2v_0 = \frac{2}{3} E$

Note: k: coefficient of transfer voltage $\quad k\alpha \cdot \frac{C_{12}}{C_{12}+C_2}$; ratio of transfer voltage $\quad \alpha$: refer to Equation 21.3.

The crest value of the normal phase voltage of the 275 kV side is $(\sqrt{2}/\sqrt{3}) \cdot 275 = 224.5$ kV and that of the normal phase voltage of the 24 kV side is $(\sqrt{2}/\sqrt{3}) \cdot 24 = 19.6$ kV.

Now, assuming a surge of twice the value of 224.5 kV, then ${}_2v_a = 2.0 \times 224.5 \times 0.23 = 103$ kV. The derived magnitude is indeed 5.3 times 19.6 kV and certainly threatens the insulation of the generator side without effective countermeasures, although the equation is derived under the LT bushings open condition.

We also need to recognize that each case in the table is often caused by real surge modes. In the case of lightning striking the connected transmission line, the incidental surges E_a, E_b, E_c come together at the station regardless of the fault phases modes, so they include the equal components E which correspond to case 3 or 4 or 5, for example. Another example is switching surges caused by the first pole closing of a breaker, which corresponds to case 1. All the cases shown in Table 21.3 are realistic phenomena.

21.6.1.4 Transfer voltage arriving at the generator terminal

Next, we need to calculate the transfer voltages arriving at the generator terminals when a generator (the surge impedance Z_g) is connected to the transformer. The circuit is shown in Figure 21.13a, where the incidental voltage is $k\alpha \cdot e(t)$ from Equation 21.9.

The circuit equations in the Laplace domain are

$$\begin{aligned} &\{k\alpha \cdot e(s) - {}_2v(s)\} \cdot sC_{12} = i(s) = i_2(s) + i_g(s) \\ &{}_2v(s) = \frac{i_2(s)}{sC_2} = i_g(s) \cdot Z_g \quad (Z_g \text{ : surge impedance on the generator}) \end{aligned} \tag{21.11}$$

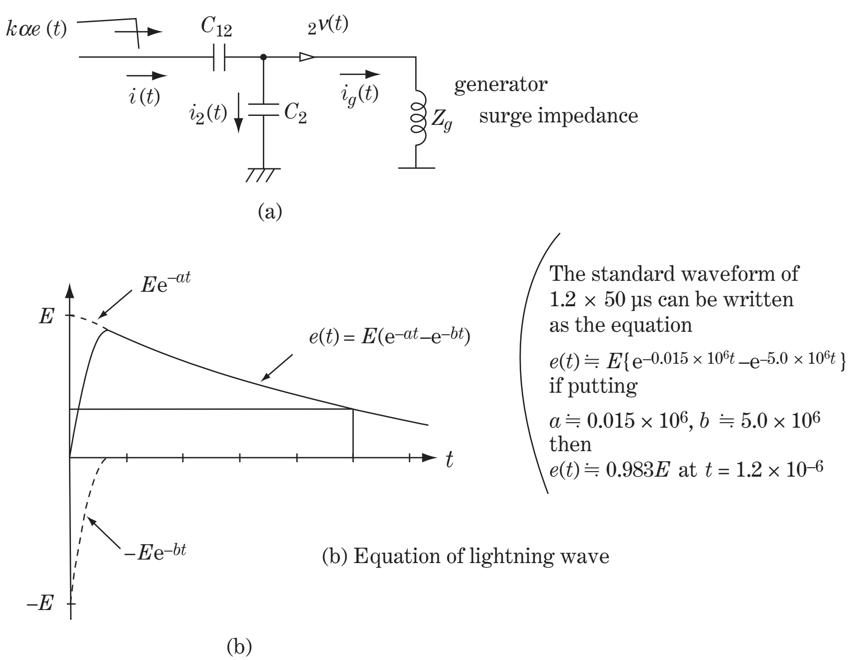


Figure 21.13 Equivalent circuit of LT coil with the generator side for high-frequency phenomena

$$\left.\begin{aligned} \therefore\ {}_2v(s) &= \frac{sC_{12}\cdot k\alpha\cdot e(s)}{s(C_{12}+C_2)+\dfrac{1}{Z_g}} = \frac{C_{12}}{C_{12}+C_2}\cdot\frac{s}{s+\delta}\cdot k\alpha\cdot e(s) \\ \text{where}\quad \delta &= \frac{1}{(C_{12}+C_2)Z_g} \end{aligned}\right\} \tag{21.12}$$

Next, the incidental surge voltage coming from the HT side $e(t)$ may have a waveform similar to the virtual standard waveform, which is expressed by $e(t) = E\left(e^{-at} - e^{-bt}\right)$ as shown in Figure 21.13b.

The incident surge is

$$\left.\begin{aligned} e(t) &= E\left(e^{-at} - e^{-bt}\right) \\ e(s) &= E\left(\frac{1}{s+a} - \frac{1}{s+b}\right) \end{aligned}\right\} \tag{21.13}$$

We calculate the LT side voltage ${}_2v(s)$, ${}_2v(t)$:

$$\begin{aligned} {}_2v(s) &= k\alpha E\frac{C_{12}}{C_{12}+C_2}\cdot\left\{\frac{1}{s+a}\cdot\frac{s}{s+\delta} - \frac{1}{s+b}\cdot\frac{s}{s+\delta}\right\} \\ &= k\alpha E\frac{C_{12}}{C_{12}+C_2}\cdot\left\{\frac{1}{\delta-a}\left(\frac{\delta}{s+\delta} - \frac{a}{s+a}\right) - \frac{1}{\delta-b}\left(\frac{\delta}{s+\delta} - \frac{b}{s+b}\right)\right\} \\ &= k\alpha E\frac{C_{12}}{C_{12}+C_2}\left\{\frac{(a-b)\delta}{(\delta-a)(\delta-b)}\cdot\frac{1}{s+\delta} - \frac{a}{\delta-a}\cdot\frac{1}{s+a} + \frac{b}{\delta-b}\cdot\frac{1}{s+b}\right\} \end{aligned} \tag{21.14}$$

The transfer surge voltage at the generator bushing terminals is

$$\begin{aligned} {}_2v(t) &= k\alpha E\frac{C_{12}}{C_{12}+C_2}\left\{\frac{(a-b)\delta}{(\delta-a)(\delta-b)}e^{-\delta t} - \frac{a}{\delta-a}e^{-at} + \frac{b}{\delta-b}e^{-bt}\right\} \\ &= k\alpha E\frac{C_{12}}{C_{12}+C_2}\left\{\frac{-\delta}{a-\delta}e^{-\delta t} + \frac{a}{a-\delta}e^{-at} - e^{-bt}\right\}, \quad \text{where } a, \delta \ll b \end{aligned} \tag{21.15}$$

where $a \fallingdotseq 0.015\times 10^6$, $b \fallingdotseq 5.0\times 10^6$ for the virtual standard waveform of $1.2\times 50\,\mu\text{s}$ as the incidental voltage (see Figure 21.11b).

In addition, as is shown in the trial calculation below, δ is smaller than a. Accordingly, $\delta < a \ll b$.

Therefore, at the time around the crest value, $t = 1$–$1.2\,\mu\text{s}$, we can put $e^{-at} \to 1$, $e^{-\delta t} \to 0$, $e^{-bt} \to 0$ and $\delta/(a-\delta) \to 0$.

The transfer surge voltages from the HT side at the generator terminals are

$$\left.\begin{aligned} {}_2v(t) &\fallingdotseq k\alpha E\frac{C_{12}}{C_{12}+C_2}\left\{\frac{a}{a-\delta}\cdot e^{-at} - \frac{\delta}{a-\delta}e^{-\delta t}\right\} \\ \text{where}\quad a &\gg \delta = \frac{1}{(C_{12}+C_2)Z_g} \end{aligned}\right\} \tag{21.16a}$$

Therefore

$$\left.\begin{aligned} {}_2v(t) &= k\alpha E\frac{C_{12}}{C_{12}+C_2}\cdot e^{-at} && ① \\ &\fallingdotseq k\alpha E\frac{C_{12}}{C_{12}+C_2} \quad \text{for} \quad t=0+ && ② \\ \text{where} & \\ \alpha &= 0.5 \text{ for solidly neutral grounded system} \\ &= 1.5 \text{ for neutral ungrounded system} \\ &= \text{between } 0.5 \text{ and } 1.5 \text{ for high-resistive neutral grounded system} \\ k &= 1/3, 1/2, 2/3, 5/6, 1, 7/6 \text{ depending on the transformer neutral} \\ & \quad \text{connection see Table 21.3)} \\ E\,&(\text{kVcrest}): \text{surge transmitted voltage from HV bushing terminal} \end{aligned}\right\} \quad (21.16b)$$

As a trial calculation for a surge impedance of generators typically $Z_g = 30 - 300\,\Omega$ (Z_g is smaller for generators of larger MVA capacity), putting $C_{12} = 6000$ pF, $C_2 = 3000$ pF($C_{12} + C_2 = 9000 \times 10^{-9}$ F) and $Z_g = 30$–$300\,\Omega$, then $\delta \fallingdotseq 3000 - 300\ (< a \ll b)$.

Accordingly,

$${}_2v(t) = 0.67k\,\alpha E\frac{6000}{6000+3000} = 0.447k\,\alpha E$$

The resulting Equation 21.16b means that the transfer voltages from the HT side cannot be reduced by connection of the generator surge impedance. Appropriate countermeasures to protect the LT-side insulation against the transfer voltage are essential.

21.6.2 Generator protection against transfer surge voltages through transformer

An appropriate countermeasure is required to protect generators against the transfer surge voltage coming from the HT side, because the rated voltages of the generators are perhaps 10–35 kV, so the insulation level of the LT side against surge is relatively lower.

The typical countermeasure is to install a so-called '**surge absorber**' **at the LT bushing terminal** of the transformer for each phase, which is a parallel circuit of a capacitor and arrester, as shown in Figure 21.14.

By adding capacitance C as the surge absorber, Equation 21.16 is modified as $C_2 \to C_2 + C$ so that

$$\frac{C_{12}}{C_{12}+C_2} \to \frac{C_{12}}{C_{12}+(C_2+C)} \quad (21.17)$$

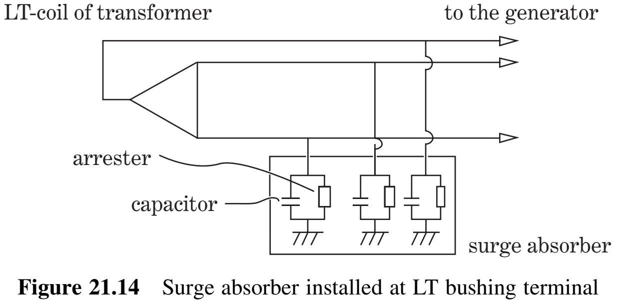


Figure 21.14 Surge absorber installed at LT bushing terminal

Accordingly, the transfer voltages can be considerably reduced by adding C of value larger than that of C_{12}, C_2 (namely, $C \gg C_{12},\ C_2$). Typically, $C = 0.1 - 0.5\,\mu F$ may be a reasonable range. The arrester in parallel is to relieve the steep wavefront.

21.6.3 Electromagnetic transfer voltage

A transfer voltage to the LT side by electromagnetic coupling also arises. Voltages caused by magnetic coupling can be written simply as *Mdi/dt*, so the transfer voltage would be roughly proportional to the transformation ratio, T-ratio $= n_{HT}/n_{LT}$, the turns ratio of the HT and LT coils of the transformers.

Considering a virtual standard waveform of $1.2 \times 50\,\mu s$ as the incident voltage E at the HT side and a current surge with a similar waveform, the transfer voltage to the LT side in an initial time duration of $t = 0\text{–}1.2\,\mu s$ would be roughly (T-ratio)$\cdot E$.

In the case of a 275 kV/24 kV transformer (T-ratio 11.5) as an example, the incidental surge voltage E on the HT-side line would be transferred to the LT side by $(1/11.5)E = 0.087E$. A less serious threat arises from the absolute value as well as the relatively gradual wave shape in comparison with the electrostatic transfer voltages. Electrostatic, rather than electromagnetic, coupling plays the lead role in transfer voltage phenomena.

21.7 Internal High-frequency Voltage Oscillation of Transformers Caused by Incident Surge

A transformer is a very compact piece of apparatus in that HT, medium-tension (MT) and LT coils are concentrically and tightly arranged surrounding the laminated silicon steel core and each coil is composed of a number of winding sections. Therefore, when we examine transient voltage and current phenomena on the internal coils, we need to establish the equivalent circuit as a distributed circuit with a number of L and C elements. Internal transient oscillations would obviously be caused by the impact of an external surge voltage wave, so effective suppression of such transient oscillatory voltages as well as effective insulation design for each sectional part of the coils are vital. This is a unique problem that transformer engineers have to overcome in the total design engineering of coil allocation and coil insulation strength.

21.7.1 Equivalent circuit of transformer in EHF domain

Figure 21.15 shows sketches of a typical large-capacity transformer. As seen in Figures 21.15a and b, the LT, MT and HT coils are concentrically arranged in this order as cylinder coils. Each coil is assembled from a numbers of series-connected sectional windings. These figures also show typical coil winding structures named 'multi-layer cylindrical windings' and 'disc windings'.

Now we examine the voltage behaviour of a transformer's internal coil when an incident surge voltage E arrive at the HT bushing terminal. Figure 21.16a is the equivalent circuit of the HT coil against surge or high oscillatory frequency phenomena, although resistance as well as susceptance are ignored and the mutual couplings with MT and LT coils are not written. In the case of a disc-winding coil, each section of the ladder circuit means each disc winding is connected in series.

21.7.2 Transient oscillatory voltages caused by incident surge

The initial conditions of the circuit just after the surge arrives can be very accurately expressed by the Figure 21.16b, which is equivalent to Figure 21.16a in regard to the C distribution, although

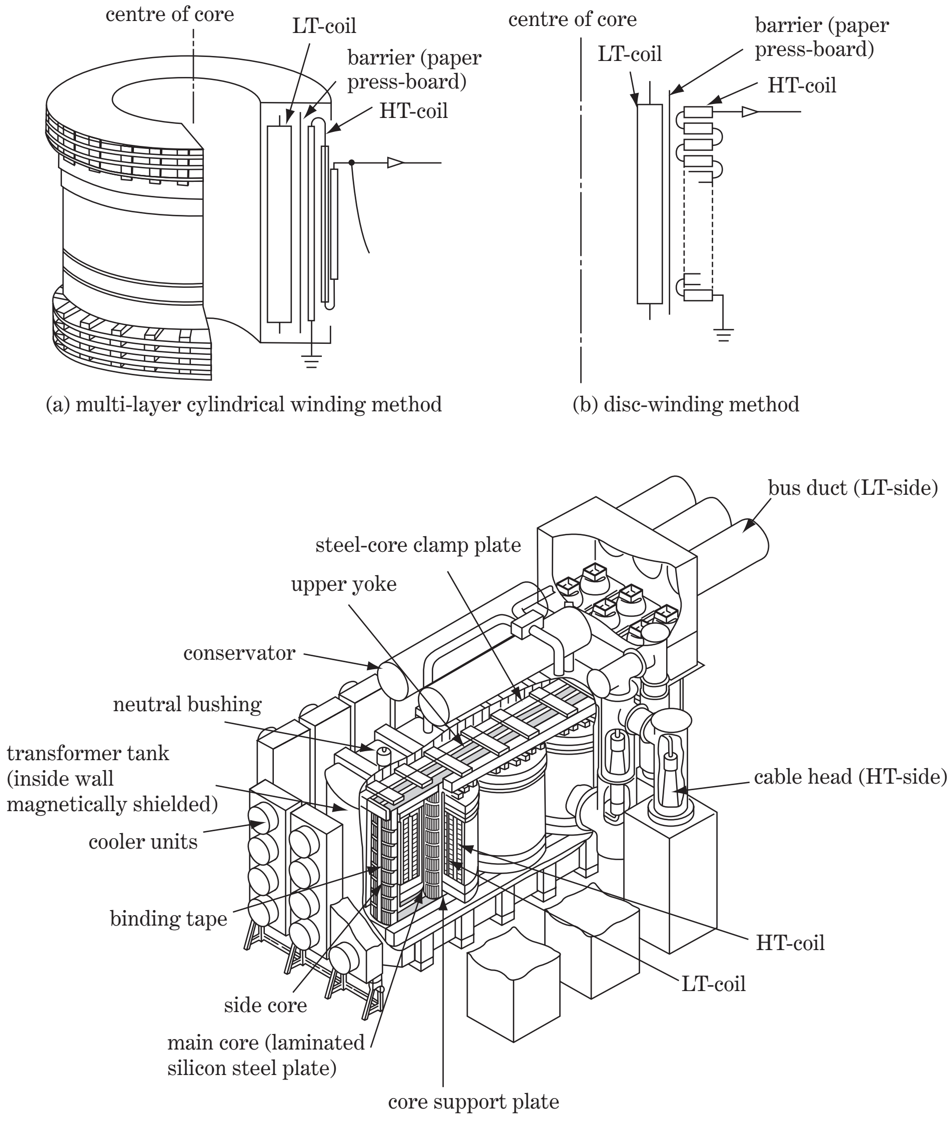


Figure 21.15 Large-capacity transformer for power station use (y–Δ connection)

all the inductances are ignored. On the other hand, the final conditions after the transient terms disappear would be expressed as the circuit (a) but ignoring all the C elements, namely by Figure 21.16d.

Figure 21.16b is a very accurate equivalent circuit of the HT coil for EHF (extra high frequency) phenomena. Now we will calculate the surge voltage distribution of this circuit where

C: the total capacitance from the HT coil to earth-ground
Cdx: the capacitance of one disc(between x and $x + dx$) to earth-ground
K: the total series capacitance of HT coil from the HT terminal bushing to neutral terminal
K/dx: the series capacitance across disc(between x and $x + dx$)
i_c: the current flowing through Cdx
i_k: the current flowing through K/dx

The equation at the winding section x to $x + dx$ is

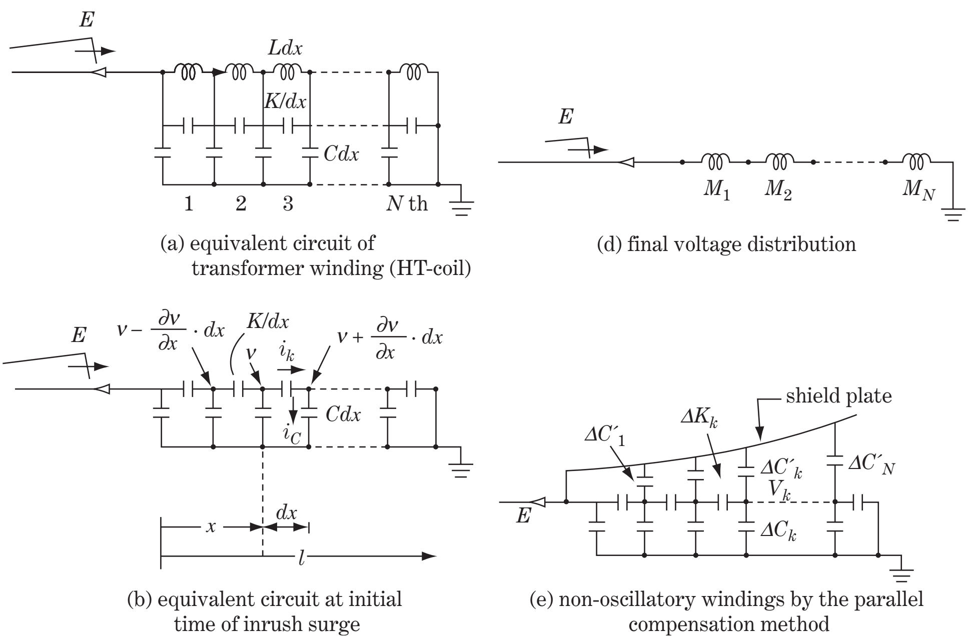
$$\left.\begin{aligned} i_k &= \frac{K}{dx}\cdot\frac{\partial}{\partial t}\Bigg\{ \underbrace{v}_{\text{voltage at } x} - \underbrace{\left(v + \frac{\partial v}{\partial x}\right)}_{\text{voltage at } x+dx} \Bigg\} & \therefore\quad i_k &= -K\frac{\partial^2 v}{\partial t \partial y} \\ \underbrace{-\frac{\partial i_k}{\partial x}\cdot dx}_{\text{reduced current of } i_k \text{ through } dx} &= \underbrace{Cdx\cdot\frac{dv}{dt}}_{\text{leakage current through } Cdx} & \therefore\quad -\frac{\partial i_k}{\partial x} &= C\frac{\partial v}{\partial t} \end{aligned}\right\} \tag{21.18}$$

Eliminating i_k from both equations,

$$K\frac{\partial}{\partial t}\cdot\frac{\partial^2 v}{\partial x^2} = C\frac{\partial v}{\partial t} \tag{21.19}$$

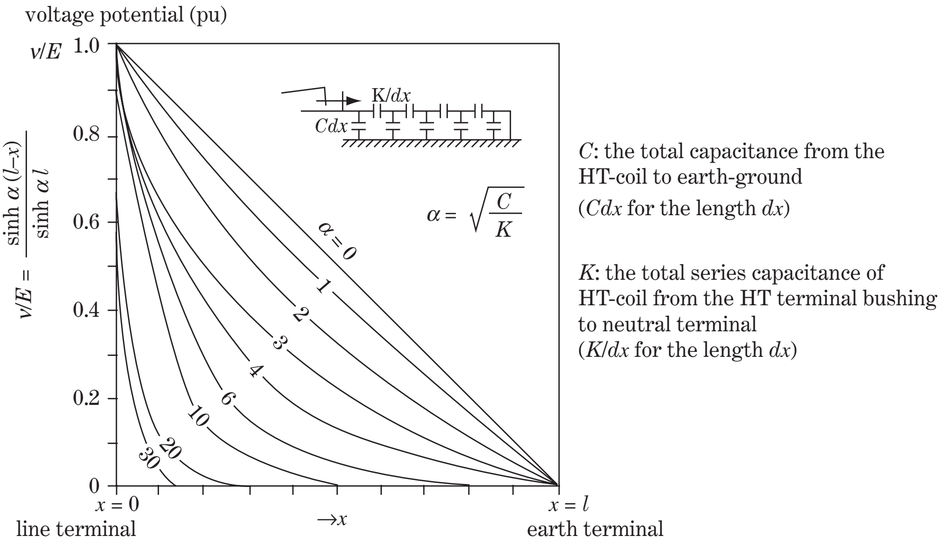
then

$$\left.\begin{aligned} K\frac{\partial^2 v}{\partial x^2} &= Cv \\ \frac{\partial^2 v}{\partial x^2} &= \alpha^2 v \qquad \text{where} \quad \alpha = \sqrt{\frac{C}{K}} \end{aligned}\right\} \tag{21.20}$$

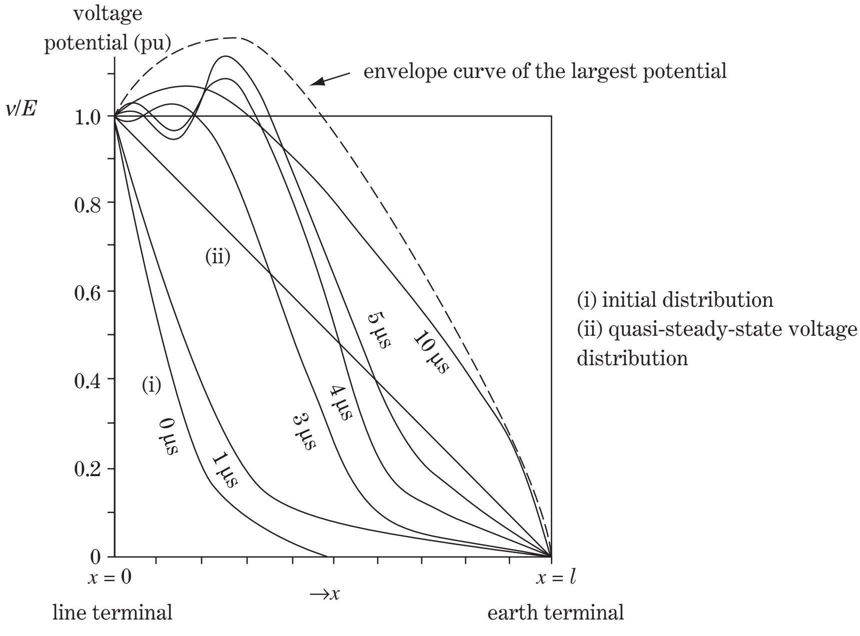


(a) equivalent circuit of transformer winding (HT-coil)
(d) final voltage distribution
(b) equivalent circuit at initial time of inrush surge
(e) non-oscillatory windings by the parallel compensation method

Figure 21.16 Internal surge behaviour of transformer



(c1) initial surge voltage distribution



(c2) surge voltage oscillation

Figure 21.16 (*Continued*)

The general solution of the above partial differential equation is

$$v = A\cosh \alpha x + B\sinh \alpha x \tag{21.21}$$

where A and B are determined by the terminal conditions (refer to the supplement for the proof).

The terminal conditions are

$$\left.\begin{aligned} v = 0 \quad \text{at} \quad x = l \qquad &\therefore\ 0 = A\cosh \alpha l + B\sinh \alpha l \\ &\qquad = \frac{A+B}{2}\mathrm{e}^{\alpha l} + \frac{A-B}{2}\mathrm{e}^{-\alpha l} \\ v = E \quad \text{at} \quad x = 0 \qquad &\therefore\ E = A\cosh 0 + B\sinh 0 = A \end{aligned}\right\} \tag{21.22}$$

$$\begin{aligned} &\therefore A = E \\ &B = -E\frac{\mathrm{e}^{\alpha l} + \mathrm{e}^{-\alpha l}}{\mathrm{e}^{\alpha l} - \mathrm{e}^{-\alpha l}} = -E\frac{\cosh \alpha l}{\cosh \alpha l} \end{aligned} \tag{21.23}$$

Substituting Equation 21.23 into 21.21 and modifying,

$$\left.\begin{aligned} v(x,0) &= E\frac{\mathrm{e}^{\alpha(l-x)} - \mathrm{e}^{-\alpha(l-x)}}{\mathrm{e}^{\alpha l} - \mathrm{e}^{-\alpha l}} = E\frac{\sinh \alpha(l-x)}{\sinh \alpha l} \\ \alpha &= \sqrt{\frac{C}{K}} \end{aligned}\right\} \tag{21.24}$$

This equation gives the initial voltage distribution of the HT coil at $t = 0$, where $v(x,t)$ means the transient voltage on coil position x and at time t. Figure 21.16c1 shows the curve of the initial voltage distribution $v(x,0)$ at $t = 0$ along with the coil position x and with the parameter α, which was derived from Equation 21.24.

The figure indicates that at initial time $t = 0$, surge voltage E cannot be uniformly charged to each coil section except only in the exceptional case under the condition $\alpha = 0$ (unrealistic case of $C = 0$). If α is larger (larger C and smaller K), most of the surge voltage is charged unequally on the coil sections close to the HT bushing. If $\alpha = 10$, for example, quite a large voltage stress of approximately $0.8E$ is placed on the first 20% of coil sections close to the line terminal bushing.

The potential gradient is

$$\frac{\partial v}{\partial x} = -E\frac{\alpha\cosh \alpha(l-x)}{\sinh \alpha l} \tag{21.25}$$

and

$$\left.\frac{\partial v}{\partial x}\right|_{x=0} = -E\frac{\alpha\cosh \alpha l}{\sinh \alpha l} \tag{21.26}$$

The equation shows the potential gradient of the initial distribution is largest at the high-voltage terminal.

Let us investigate the behaviour a little more. Just after the incident surge of probably triangular waveform with a short wavefront and long wave-tail (say, 1.0×50 to $200\ \mu$s, for example) is charged on the HT coil, the voltage at each position $v(x,t)$ begins to oscillate from the initial distribution $v(x,0)$. Figure 21.16c2 shows the oscillatory behaviour of the voltage distribution just after the initial condition. The surge voltage at each coil section would repeat some over-swing oscillation across the straight line (ii). Soon, the transient voltage oscillation would be decreased over time and disappear, so the voltages converge to the final distribution of the straight line (ii), which is called the **quasi-steady-state voltage distribution**.

The incident voltage E can be treated as the d.c. component in the time duration of the long wave-tail so that $dv/dt \to 0$ at this time, although v is still large. Accordingly, at the end of the long voltage wave-tail, $Cdv/dt \to 0$ and $K/(dv/dt) \to \infty$, or in other words the capacitive elements act as like open-circuit elements. Therefore all the C and K branched circuits in Figure 21.16a can be ignored during this time and the final voltage distribution can be derived from Figure 21.16d. The voltage distribution during this time converges to a uniform distribution, that is the distribution of (ii) in Figure 21.16c2.

The final distribution by in Figure 21.16d is expressed by the equation below, for disc coil number 1–N,

$$\begin{aligned} E &= \sum_{k=1}^{N} \Delta v_k & ① \\ \Delta v_k &= \left(\sum_{j=1}^{N} L_{kj} \right) \frac{di}{dt} \equiv M_k \frac{di}{dt} & ② \end{aligned} \tag{21.27}$$

where

$k = 1$–N: the number of disc windings
i: the current flowing through each winding (the same current)
Δv_k: shared voltage by kth disc winding
L_{kj}: mutual inductance between kth and jth disc windings
$M_k = \sum_{j=1}^{N} L_{kj}$: total summation of self- and mutual inductances of kth disc winding (the specific value of each disc winding)

At the time of the final distribution condition (quasi-steady-state voltage distribution), the voltage oscillation is terminated so that the shared voltage of each disc Δv_k has a constant value, and accordingly di/dt is constant from Equation ②. In other words, at the time of the quasi-steady-state voltage distribution, the surge voltage oscillation is terminated, whereas **the surge current *i* is still increasing** at constant speed. Although this surge current continues to increase a little, sooner or later it will stop and soon disappear because it is resistively attenuated.

It can be concluded from Figure 21.16c2 that the surge voltage distribution $v(x, t)$ of the coil sections initiates internal oscillation from the initial distribution of Figure 21.16c1 and would repeat oscillatory over-swing across the quasi-steady-state voltage distribution line (ii) and soon terminate voltage oscillation, while the surge current still continues to increase and then disappears. The envelope curve of the voltage oscillatory distribution is also shown in Figure 21.16c2. In all events, transformer design with excess non-uniform initial voltage distribution should be avoided in order to avoid excess concentrated stress on a few coil sections around the HT bushing side and to reduce oscillatory surge voltage.

21.7.3 Reduction of internal oscillatory voltages

We need to make the initial distribution curve as much as possible coincident with the final distribution curve (the straight line of $\alpha = 0$) by reducing α; in other words, by decreasing C or by increasing K.

However, to decrease C is actually impossible because enlarging the distance from the winding to the core/tank/other windings cannot be done realistically. K also cannot be increased because the winding discs are already very closely arranged.

The widely applied effective countermeasure to reduce oscillation is known as the **parallel compensation method** of stray capacitances.

21.7.3.1 Non-oscillatory windings by the parallel compensation method

In Figure 21.16e, the shield ring plate with the voltage potential of the HT bushing terminal (often called the **rib shield**) is additionally arranged. The distance between each winding disc and the rib shield is closer near the HT terminal and far apart around the neutral terminal (i.e. $\Delta C_1' > \cdots > \Delta C_k' > \cdots > \Delta C_N'$) (parallel capacitance compensation method).

At the initial time when surge E is charged, the current through ΔC_k is supplied directly through $\Delta C_k'$, so the current through direct capacitance ΔK_k for each disc becomes uniform, which means the initial shared voltage by each disc winding is kept almost equal. We can write this symbolically as follows.

At the initial timing, for the parallel charging currents for each winding disc,

$$\Delta C_k' \frac{\partial (E - v_k)}{\partial t} = \Delta C_k \frac{\partial v_k}{\partial t} \qquad \text{(where} \quad k = 1.2, \cdots \text{N)}$$

$$\therefore \quad \frac{\partial v_k}{\partial t} = \frac{1}{\dfrac{\Delta C_k}{\Delta C_{k'}} + 1} \cdot \frac{\partial E}{\partial t} \equiv \delta_k \cdot \frac{\partial E}{\partial t} \tag{21.28}$$

Then, if the δ_k at each winding disc are designed almost equal for each other ($\delta_1 \fallingdotseq \delta_2 \fallingdotseq \cdots \fallingdotseq \delta_k \fallingdotseq \cdots$), the initial voltage rising velocity at each winding disc ($\delta v_k / \delta t$) can be kept in almost equal, so that the initial surge voltage is uniformly distributed.

The oscillatory voltage behaviour on the transformer winding is closely affected by the required insulation of the transformer. However, the phenomenon is not usually included in the concept of the term ‘insulation coordination’ because it is not necessarily related to the insulation coordination of the power system.

In other words, this is considered as an insulation matter which the transformer engineers or the suppliers have to solve for individual transformer products.

21.8 Oil-filled Transformers Versus Gas-filled Transformers

Power transformers of large capacity (say, over 60 MVA) are usually oil-filled transformers because they have been utilized as the standard type in many years, although small-capacity transformers (say, typically 0.5–40 MVA) have been utilized as of dry type (air or SF_6 gas insulation/coolant) as well as oil-filled type. This is because oil is available as the one material having the characteristics of ‘superior electrical insulation’ and ‘superior thermal loss discharging (coolant)’. Accordingly, it was believed for a long time that oil could not be technically replaced by SF_6 gas for large-transformer applications, because SF_6 gas has quite poor characteristics for thermal capacity (or thermal conductivity), in spite of its outstanding insulation characteristics. This fact is in stark contrast to the engineering history of circuit-breakers, because SF_6-gas-type breakers have been widely used in place of oil-filled type/air-blast-type breakers over the last half century.

However, SF_6-gas-filled transformers with a large capacity of 300 MVA · 275 kV were first utilized in the mid-1990s in the underground EHV substations of high buildings in Tokyo, whose fundamental structures were of the familiar disc windings but filled with gas instead of oil. These achievements led to a breakthrough by changing the conservative concept of large-capacity/EHV transformers only as of oil-filled type.

Needless to say, oil-filled transformers have one major weak point: that is, the severe damage which would inevitably be caused to the transformer if a breakdown fault of the internal coil were to occur, and furthermore possibly influence the installed surroundings.

Whenever a short circuit occurs in the coil insulation of oil-insulation-type transformers, liquid oil around the fault is immediately gasified by the arcing temperature, so the internal gauge pressure of the tank would be rapidly increased. The oil–gas pressure increase would continue until fault tripping by the related breakers is completed, so the accumulated pressure may reach a very high level, even exceeding the mechanical withstanding strength of the tank within just a few cycles (50–100 ms), in particular the critical strength of the tank-cover cramping. As a result, a hot-oil-blasting overflow or even fire from the oil burning may be caused in the worst cases. Of course, the shape of the coils would be deeply distorted.

On the contrary, in the case of a short-circuit fault in an SF_6-gas-insulated transformer, the physical damage caused by the internal short-circuit fault would probably be limited to a narrow spot on the coil where a breakdown arcing pass would be produced, so the concentrically arranged HT/MT/LT coils might not be badly deformed, although carbonization of insulation tapes/press-board barriers and the copper conductor melting in a limited area would be caused. In other words, SF_6 gas transformers do not produce serious blasting or fire even in the case of an internal short-circuit fault. This is obviously a big advantage from the safety point of view, in particular for receiving substations located in city areas, whether the substations are outdoors or in-house, or under high-rise buildings.

The reasons why the above breakthrough was realized can be summarized by the following three points:

a) Extensive study of thermal discharge (conductivity) characteristics as well as insulation/breakdown characteristics of SF_6 gas under various shapes of coils, gas-flowing passes and gas-flow speed based on a detailed mathematical simulation approach and experimental model tests, and finally well-investigated smart coil structure and gas-flow pass design.

b) Adoption of 0.4 MPa (4 atm) SF_6 gas pressure.

c) Application of class F insulation materials (maximum temperature 130°C) instead of class A (105°C) for coil taping based on polyethylene teraphthalate (PET) films.

Oil flow depends on its liquid viscosity characteristics, so oil may not flow easily through very narrow passes, while SF_6 gas can flow easily through even narrow passes because its gas-flow distribution does not depend on viscosity. This characteristic can be said to be an advantage of SF_6 gas in comparison with oil. However, SF_6 gas may flow through various passes in an unbalanced way, in contrast to a joule-loss distribution which should be cooled at a continuous withstanding temperature (130°C). This is obviously a disadvantage of SF_6 gas in comparison with oil as a coil coolant material.

Furthermore, the thermal capacity of SF_6 gas under 1 atm pressure is only $1/200 = 0.5\%$, while that under 4 atm pressure is still only $2.4/200 = 1.2\%$ in comparison with that of oil. (see Table 21.4). These are the reasons why accurate analysis and careful gas-flow pass design are required. Adoption of 4 atm pressure gas is also quite a valuable improving factor in order to realize effective cooling by SF_6 gas, although its thermal capacity is still much smaller than that of oil.

Figure 21.17 shows 400 MVA/330 kV/132 kV SF_6 gas transformers installed in Australia. Large-capacity SF_6 gas insulation transformers can be realized only by fully involving the above three countrermeasures. As can be seen in Figure 21.17, cylindrical tanks, instead of the traditional box type, are generally advantageous for gas transformers because the tank needs to withstand the high pressures. Gas insulation transformers also have a smaller volume and can save on installation space in comparison with oil insulation transformers of the same capacity, in particular their height, because the oil conservator, oil-absorbable saucer, fireproof barrier, etc., can be removed.

Large-capacity SF_6 gas transformers should rapidly prevail in the near future, in particular as large transformers installed in urban areas, regardless of the location of the transformers. SF_6-gas-filled

Table 21.4 Comparison of thermal capacity

	Ratio of thermal capacity
Oil	200
SF_6 gas: 0.125 MPa-g	1
SF_6 gas: 0.40 MPa-g)	24

Note: 0.1 MPa-g (megapascal to gravity) = 1 atm = 1000 mb approximately.

Courtesy of Transgrid (Australia)/Toshiba

Haymarket substation (Australia)

primary: 330 kV ± 10% (21 taps) 400 MVA
secondary: 138.6 kV 400 MVA
tertiary: 11 kV 20 MVA
rated gas pressure: 0.43 MPa-g (20°)
heat exchanges: gas-to-water cooling

Figure 21.17 A 400 MVA SF_6-gas-insulated transformer (with On-load-tap-changer)

shunt reactors of 150 MVA have also been utilized, and should prevail for the same reasons for gas transformers.

21.9 Supplement: Proof that Equation 21.21 is the solution of Equation 21.20

Differentiating v of Equation 21.21 twice,

$$v = A\cosh\alpha x + B\sinh\alpha x = \frac{A}{2}(e^{\alpha x} + e^{-\alpha x}) + \frac{B}{2}(e^{\alpha x} - e^{-\alpha x})$$

$$\therefore \quad \frac{\partial v}{\partial x} = \frac{A+B}{2}\alpha e^{\alpha x} - \frac{A-B}{2}\alpha e^{-\alpha x}$$

$$\therefore \quad \frac{\partial^2 v}{\partial x^2} = \frac{A+B}{2}\alpha^2 e^{\alpha x} + \frac{A-B}{2}\alpha^2 e^{-\alpha x} = \alpha^2 v$$

Therefore Equation 21.21 satisfies Equation 21.20.