Advanced Diploma of Industrial Automation

Module 21

Rev 1

ENGINEERING INSTITUTE OF TECHNOLOGY

Acknowledgments

We would like to acknowledge the following references used as materials for students.

Reference Book Title: J&P Transformer book
Author: Martin J, Heathcote
Edition Number & Year of Publication: 12th Edition 1998

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4 Transformer construction

Introduction

Power transformer construction follows similar principles for units rated from a few kVA up to the largest sizes manufactured, but as the unit size increases a greater degree of sophistication becomes justified. Many manufacturers subdivide their construction activities into 'distribution' and 'large power', although exactly where each one makes this division varies widely. Usually the dividing line depends on the weight of the major components and the type and size of handling facilities which are required in the factory. Manufacturers of distribution transformers rated up to between 1 and 2 MVA often utilise roller conveyors and runway beams for the majority of their handling. Large-power transformers require heavy lifting facilities such as large overhead cranes. Those manufacturers who produce the largest sizes may further subdivide their operations into 'medium power' and 'large power' sections. Since the largest transformers require very heavy lifting facilities – up to 400 tonnes capacity including lifting beams and slings is not uncommon – it is usual to restrict the use of these very expensive facilities exclusively to the largest units so that the medium construction factory may only possess lifting facilities of up to, say, 30 tonnes capacity.

These subdivided construction arrangements often coincide with divisions of design departments so that design practices are frequently confined within the same boundaries.

In the following descriptions of transformer design and constructional methods, the aim will generally be to describe the most developed 'state of the art' even though in some instances, for example for distribution transformers, more simplified arrangements might be appropriate. In Chapter 7, which describes specialised aspects of transformers for particular purposes, aspects in which practices might differ from the norm will be highlighted.

A note on standards

The practices of transformer design and construction adopted in the UK have evolved in an environment created by British Standard 171 *Power Transformers*. With the move towards acceptance of international standards, the governing document for power transformers throughout most of the world has become IEC 76, which is now very similar to BS 171. IEC 76 was for some time a five-part document but was reduced to four parts with the issue of the second edition in 1993, by the incorporation of Part 4 into Part 1. However, at the time of writing, January 1996, IEC 76 Part 3, which refers to insulation levels and dielectric tests, has not been officially adopted in the UK since there is still some small area of disagreement with the international body. The ruling document for insulation levels and dielectrics tests in the UK remains therefore BS 171-3:1987, which differs in some respects from IEC 76-3. The CENELEC Harmonisation Document covering power transformers is HD 398 and it is hoped that with the issue of HD 398-3 in the near future, which will include amendments to IEC 76-3, the UK will come into line. In general, throughout this book where reference is made to standards the aim will be to follow the practices recommended in the IEC documents. However there are practices, particularly with regard to insulation design and dielectric testing which have grown up because that was the requirement of BS 171. These practices are continuing and are likely to continue for many years, although they might no longer strictly be a requirement of the governing standard. Because they remain current practice in the UK, it is these practices which this chapter describes and, except where specifically indicated to the contrary, throughout this chapter the transformer standard referred to will be BS 171.

4.1 CORE CONSTRUCTION

Design features

Chapter 3 has described the almost constant developments which have taken place over the years to reduce the specific losses of core material. In parallel with these developments manufacturers have striven constantly to improve their core designs in order to better exploit the properties of the improved materials and also to further reduce or, if possible, eliminate losses arising from aspects of the core design. Superficially a core built 30 years ago might resemble one produced at the present time but, in reality, there are likely to be many subtle but significant differences.

Core laminations are built up to form a limb or leg having as near as possible a circular cross-section (*Figure 4.1*) in order to obtain optimum use of space within the cylindrical windings. The stepped cross-section approximates to a circular shape depending only on how many different widths of strip a

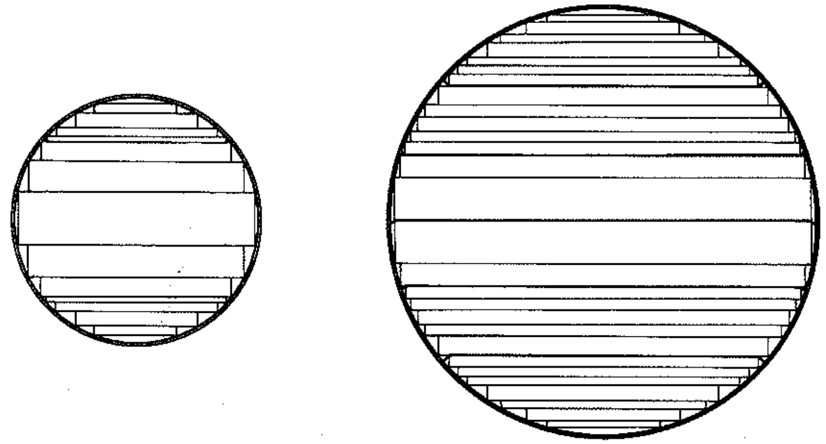


Figure 4.1 Core sections. Seven step, taped (left); and 14 step, banded (right)

manufacturer is prepared to cut and build. For smaller cores of distribution transformers this could be as few as seven. For a larger generator transformer, for example, this might be 11 or more. Theoretically, these fill from just over 93% to over 95%, respectively, of the available core circle. In reality the actual utilisation is probably slightly less than this since the manufacturer aims to standardise on a range of plate widths to cover all sizes of cores, or he may buy in material already cut to width, in which case he will be restricted to the standard range of widths provided by the core steel manufacturer, usually varying in 10 mm steps. In either circumstance it will be unlikely that the widths required to give the ideal cross-section for every size of core will be available.

Transformer manufacturers will normally produce a standard range of core cross-sections – they often refer to these as *frame sizes* – with each identified by the width in millimetres of the widest plate. These might start at 200 mm for cores of small auxiliary transformers and progress in 25 mm steps up to about 1 m, the full width of the available roll, for the largest generator transformers. This cylindrical wound limb forms the common feature of all transformer cores. The form of the complete core will, however, vary according to the type of transformer. Alternative arrangements are shown in *Figure 4.2*; of these, by far the most common arrangement is the three-phase, three-limb core. Since, at all times the phasor sum of the three fluxes produced by a balanced three-phase system of voltages is zero, no return limb is necessary in a three-phase core and both the limbs and yokes can have equal cross-section. This is only true for three-phase cores, and for single-phase transformers return limbs must be provided. Various options are available for these return limbs, some of which are shown in *Figure 4.2*; all have advantages and disadvantages and some of these will be discussed in greater depth

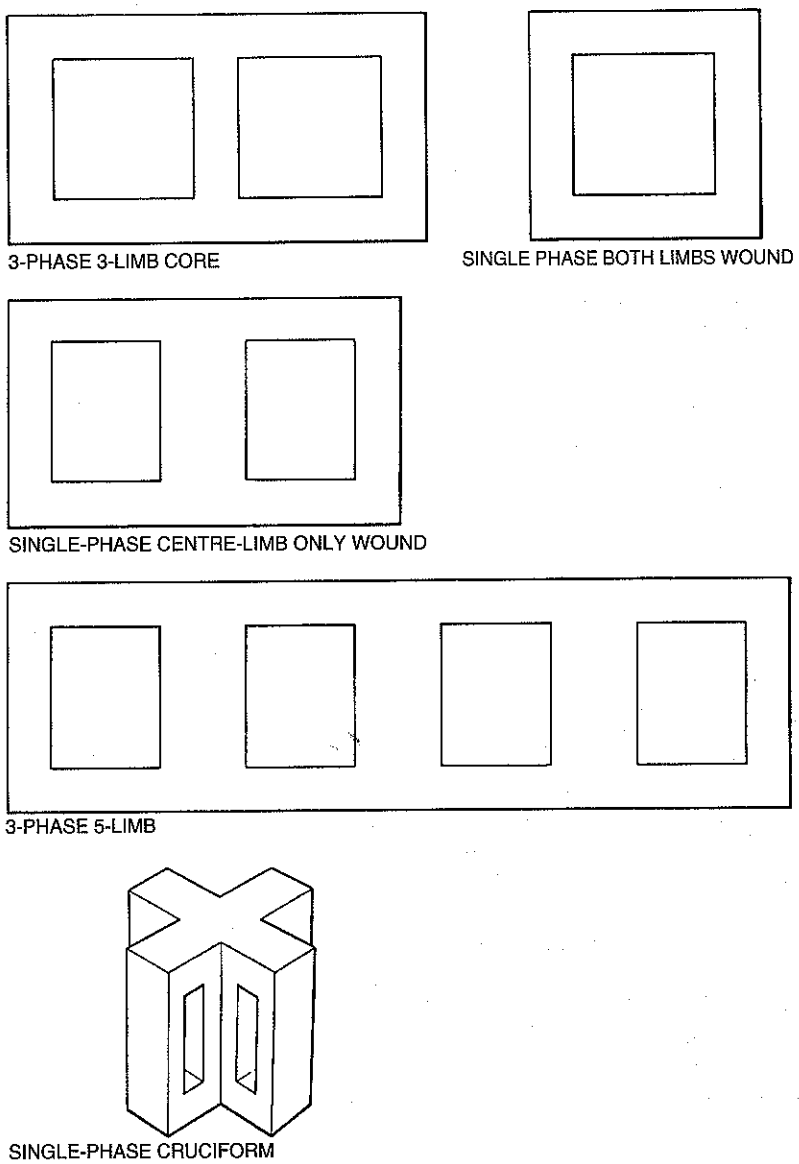


Figure 4.2 Typical core forms for single- and three-phase transformers

in Section 1 of Chapter 7, which deals with generator transformers. Generator transformers represent the only occasion where single-phase units are used on three-phase systems although in some countries they are used for large interbus transformers or autotransformers. The main reason for the use of single-phase units is from transport considerations, since the largest generator

transformers can be too large to ship as three-phase units. The use of single phase units also has advantages where very high reliability is required as, for example, in the case of large generator transformers. This aspect will be considered in greater depth in Section 1 of Chapter 7 which deals with generator transformers. *Figure 4.2* also shows a three-phase, five-limb core which is another arrangement used mainly for large three-phase generator transformers and interbus transformers in order to reduce transport height. This configuration enables the yoke depth to be reduced by providing a return flux path external to the wound limbs. In the limit the yokes could be half that which would be required for a three-phase, three-limb arrangement so the saving in height can be considerable. The 'cost' is in the provision of the return limbs which add significantly to the size of the core and to the iron losses. Of course, if transport height considerations permit, the yoke depth need not be reduced to half the limb width. If the yokes are provided with a cross-section greater than half that of the limbs the flux density in the yokes will be reduced. This will result in a reduction in specific core loss in the yokes which is greater than the proportional increase in yoke weight compared to that of a half-section yoke, hence a reduction in total core loss is obtained. This will be economic if the capitalised cost of the iron loss saved (see Section 2 of Chapter 8) is greater than the cost of the extra material. The only other occasion on which a three-phase, five-limb core might be necessary is when it is required to provide a value of zero-sequence impedance of similar magnitude to the positive sequence impedance as explained in Chapter 2.

The first requirement for core manufacture is the production of the individual laminations. Most manufacturers now buy in the core material already cut to standard widths by the steel producer so it is necessary only for them to cut this to length. Production of the laminations is one of the areas in which core manufacture has changed significantly in recent years. As explained in Section 2 of Chapter 3, the specific loss of core steel is very dependent on the nature and level of stress within the material. It is therefore necessary to minimise the degree of working and handling during manufacture. Cutting of the laminations is, of course, unavoidable but this operation inevitably produces edge burrs. Edge burrs lead to electrical contact between plates and the creation of eddy-current paths. Until the end of the 1980s British Standard 601 *Steel Sheet and Strip for Magnetic Circuits of Electrical Apparatus* laid down acceptable limits for these burrs which generally meant that they had to be removed by a burr-grinding process. Burr grinding tends to damage the plate insulation and this damage needs to be made good by an additional insulation application. Each of these operations involves handling and burr grinding in particular raises stress levels, so an additional anneal is required. Modern cutting tools enable the operation to be carried out with the production of the very minimum of edge burr. This is to some extent also assisted by the properties of the modern material itself. Typically burrs produced by 'traditional' tools of high-quality tool steel on cold-rolled grain-oriented material of the 1970s might be up to

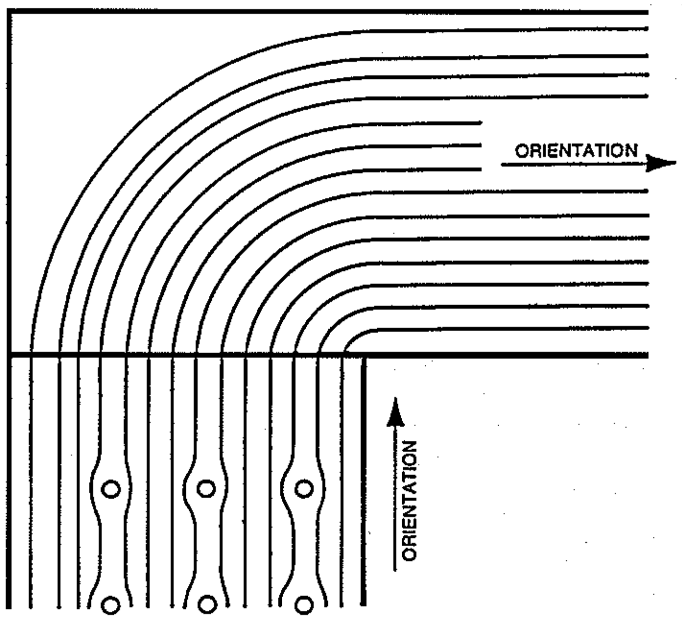
0.05 mm in height as permitted by BS 601. These could be reduced by a burr-grinding operation to 0.025 mm. With HiB steel and carbide-steel tools, burrs less than 0.02 mm are produced so that all of the burr-grinding, additional insulating and annealing processes can now be omitted.

It is perhaps appropriate at this stage to look a little further into the subject of plate insulation. The quality of this insulation was defined in BS 601, Part 2, which stated that 80% of a specified number of insulation resistance measurements made on a sample of the core plate should be greater than 2 Ω and 5% should be greater than 5 Ω. As indicated in Section 2 of Chapter 3 the purpose of this insulation is to prevent the circulation of eddy currents within the core. Preventing these currents from flowing does not, however, prevent the induced voltages from being developed. The induced voltage is proportional to the plate width and it was generally considered that plate insulation complying with the requirements of BS 601 was acceptable for plates of up to about 640 mm wide. For cores of a size which would require a plate width greater than this there are the options of subdividing the cross-section so that each part individually meets the 640 mm maximum requirement or, alternatively, additional insulation could be provided. It is often necessary to subdivide large cores anyway in order to provide cooling ducts, so that this option could normally be selected without economic penalty. It should be noted that some manufacturers had long considered that the BS 601 requirement to achieve 2 Ω was a rather modest one. When they intended to apply additional insulation anyway there was no pressing need for change to the British Standard and the issue only came to the fore when this additional coating was dropped. At about this time BS 601, Part 2 was superseded by BS 6404 : Section 8.7 : 1988 *Specification for grain-oriented magnetic steel sheet and strip*, which stated that the insulation resistance of the coating should be agreed between the supplier and the purchaser. Manufacturers were thus able to take the opportunity to apply their own specifications for the material and these generally called for a higher resistance value. There also remained the question as to what was required of the remaining 20% of the readings. These could, in theory, be zero and dependent on the coating process control they could be located in a single area of the steel strip. Reputable transformer manufacturers in this situation issued their own individual specifications usually stipulating that the physical location of the 20% low-resistance value readings occurred randomly throughout the samples, i.e. it was not acceptable that all of these should be located in the same area of the sample. As indicated in Section 2 of Chapter 3 many of the modern steels are provided with a high-quality insulation coating which is part of the means of reducing the specific loss. With these steels it is not normally necessary to provide additional coating regardless of the size of the core and the resistance measurements obtained are invariably considerably better than the minimum requirements of the old BS 601.

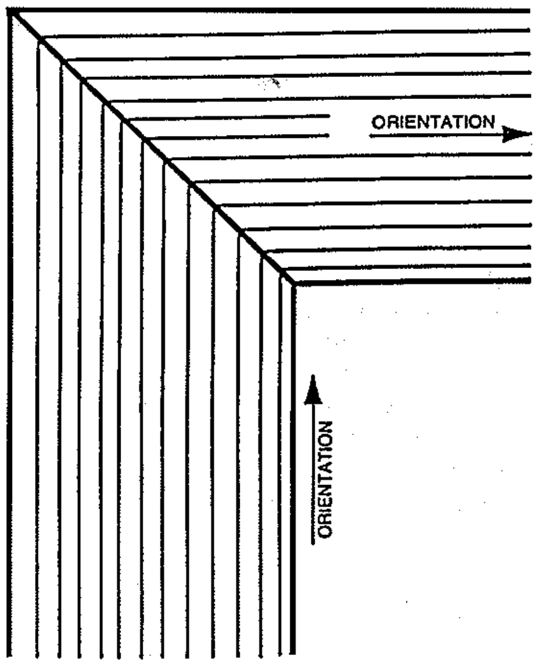
One of the disadvantages of grain-oriented core steels is that any factor which requires the flux to deviate from the grain direction will increase the core loss and this becomes increasingly so in the case of the HiB range of core steels. Such factors include any holes through the core as shown in *Figure 4.3* as well as the turning of the flux which is necessary at the top and bottom corners of the core limbs. This latter effect is noticeable in that a tall, slim core will have a lower loss than a short, squat core of the same weight and flux density since the former arrangement requires less deviation of the flux as illustrated in *Figure 4.4*. The relationship between the core loss of a fully assembled core and the product of core weight multiplied by specific loss is known as the *building factor* for the core. The building factor is generally about 1.15 for a well-designed core of grain-oriented steel. Expressed in terms of building factor the tall core discussed above has a better (i.e. lower) building factor than the squat core. In order to limit the extent to which the flux path cuts across of the grain direction at the intersection of limbs and yokes corners of laminations are cut on a 45° mitre. The core plates at these mitred corners must be overlapped so that the flux can transfer to the adjacent face rather than cross the air gap which is directly in its path (*Figure 4.5*). These mitred corners were, of course, not necessary for cores of hot-rolled (i.e. non-oriented) steel. It was also normally accepted practice for cores of hot-rolled steel for the laminations to be clamped together to form the complete core by means of steel bolts passing through both limbs and yokes. With the advent of grain-oriented steel it was recognised that distortion of the flux by bolt holes through the limbs was undesirable and that the loss of effective cross-section was also leading to an unnecessary increase in the diameter of the core limb. Designers therefore moved towards elimination of core bolts replacing these on the limbs by bands of either steel (with an insulated break) or glass fibre. In the former case the insulated break was inserted in the steel band to prevent current flow in the band itself and additionally it was insulated from the core to prevent shorting out individual laminations at their edges. Core bolts had always needed to be effectively insulated where they passed through the core limbs and yokes for the same reasons. The top and bottom yokes of cores continued to be bolted, however, since the main structural strength of the transformer is provided by the yokes together with their heavy steel yoke frames. *Figure 4.6* shows a three-phase core of cold-rolled grain-oriented steel with banded limbs and bolted yokes.

In the latter part of the 1970s increasing economic pressures to reduce losses, and in particular the core loss since it is present whenever the transformer is energised, led designers and manufacturers towards the adoption of totally boltless cores. The punching of holes through core plates has the additional disadvantage that it conflicts with the requirement to minimise the working of the core steel, mentioned above, thus increasing the loss in the material. Both these factors together with the marginal reduction in core weight afforded by a boltless core, were all factors favouring the elimination of bolt holes.

With modern steels having a very high degree of grain orientation the loss penalty for deviation of the flux from the grain direction is even more significant so that manufacturers are at even greater pains to design cores entirely without bolts through either limbs or yokes. On a large core this calls for

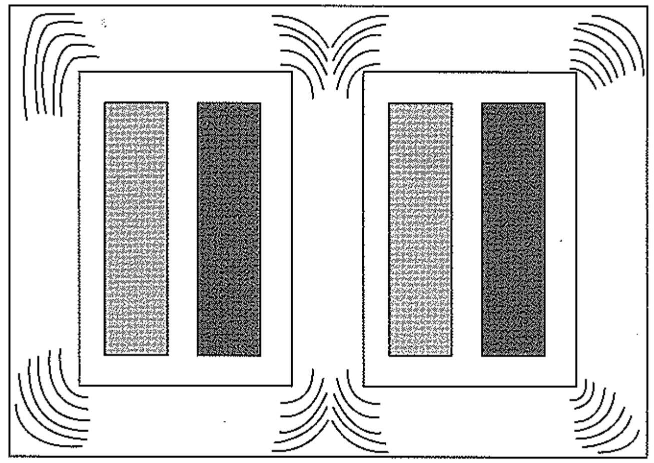


(a) Square cut (bolted)

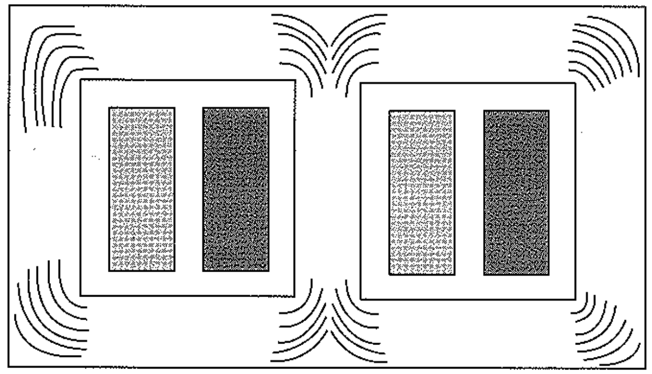


(b) Mitred

Figure 4.3 Effect of holes and corners on core flux



(a) Flux paths in tall slim single-phase core



(a) Flux paths in squat core

Figure 4.4 Cross flux at corners forms greater portion of total flux path in short squat core than in tall slim core

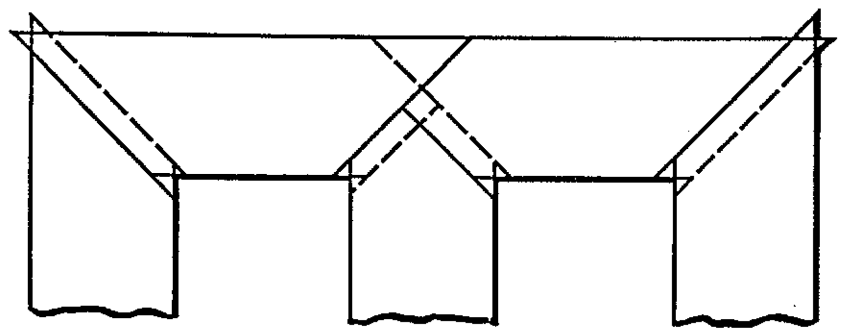


Figure 4.5 45° mitre overlap construction

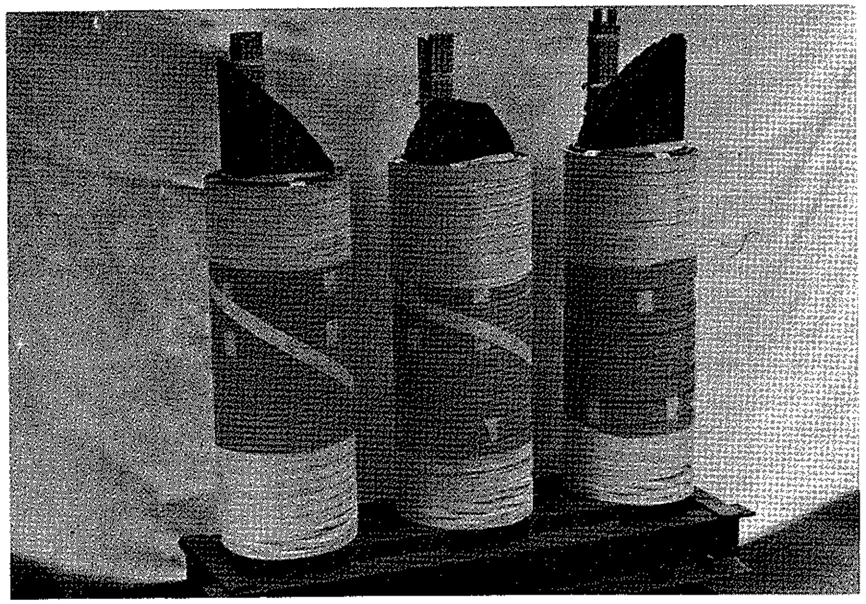


Figure 4.6 Three-phase mitred core of a 150 MVA 132/66 kV 50 Hz transformer showing the banding of the core limb laminations (Bonar Long Ltd.)

a high degree of design sophistication to ensure that the necessary structural strength is not sacrificed. *Figure 4.7* shows a large modern core having totally boltless construction.

Core building

The core is built horizontally by stacking laminations, usually two or three per lay, on a jig or stillage. The lay-down sequence must take account of the need to alternate the lengths of plates to provide the necessary overlaps at the mitred corners as shown in *Figure 4.5*. *Figure 4.8* shows a large core being built in the manufacturer's works. The clamping frames for top and bottom yokes will be incorporated into the stillage but this must also provide support and rigidity for the limbs until the core has been lifted into the vertical position for the fitting of the windings. Without clamping bolts the limbs have little rigidity until the windings have been fitted so the stillage must incorporate means of providing this. The windings when assembled onto the limbs will not only provide this rigidity, in some designs the hard synthetic resin-bonded paper (s.r.b.p.) tube onto which the inner winding is wound provides the clamping for the leg laminations. With this form of construction the leg is clamped with temporary steel bands which are stripped away progressively as the winding is lowered onto the leg at the assembly stage. Fitting of the windings requires that the top yoke be removed and the question can be asked as to why it is

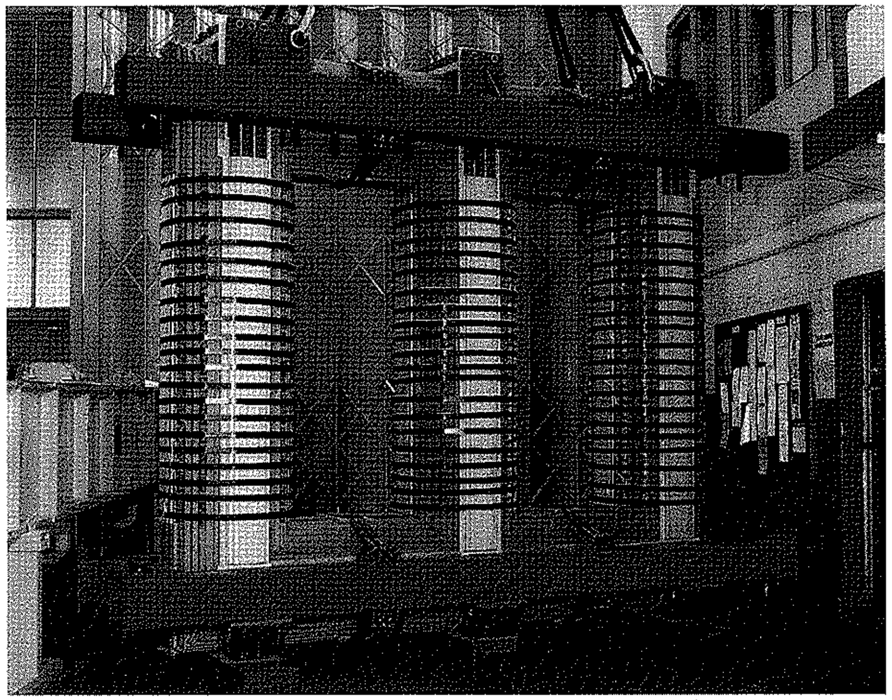


Figure 4.7 Three phase three limb boltless core. Three flitch plates (tie bars) are used each side of each limb and are visible at the top of each limb below the upper frame. The temporary steel bands clamping the limbs will be cut off as the winding assemblies are lowered onto the limbs (GEC Alsthom)

necessary to build it in place initially. The answer is that some manufacturers have tried the process of core building without the top yokes and have found that the disadvantages outweigh the saving in time and cost of assembly. If the finished core is to have the lowest possible loss then the joints between limbs and yokes must be fitted within very close tolerances. Building the core to the accuracy necessary to achieve this without the top yoke in place is very difficult. Once the windings have been fitted the top yoke can be replaced, suitably interlaced into the projecting ends of the leg laminations, followed by the top core frames. Once these have been fitted, together with any tie bars linking top and bottom yokes, axial clamping can be applied to the windings to compress them to their correct length. These principles will apply to the cores of all the core-type transformers shown in *Figure 4.2*.

Step-lapped joints

The arrangement of the limb to yoke mitred joint shown in *Figure 4.5* uses a simple overlap arrangement consisting of only two plate configurations.

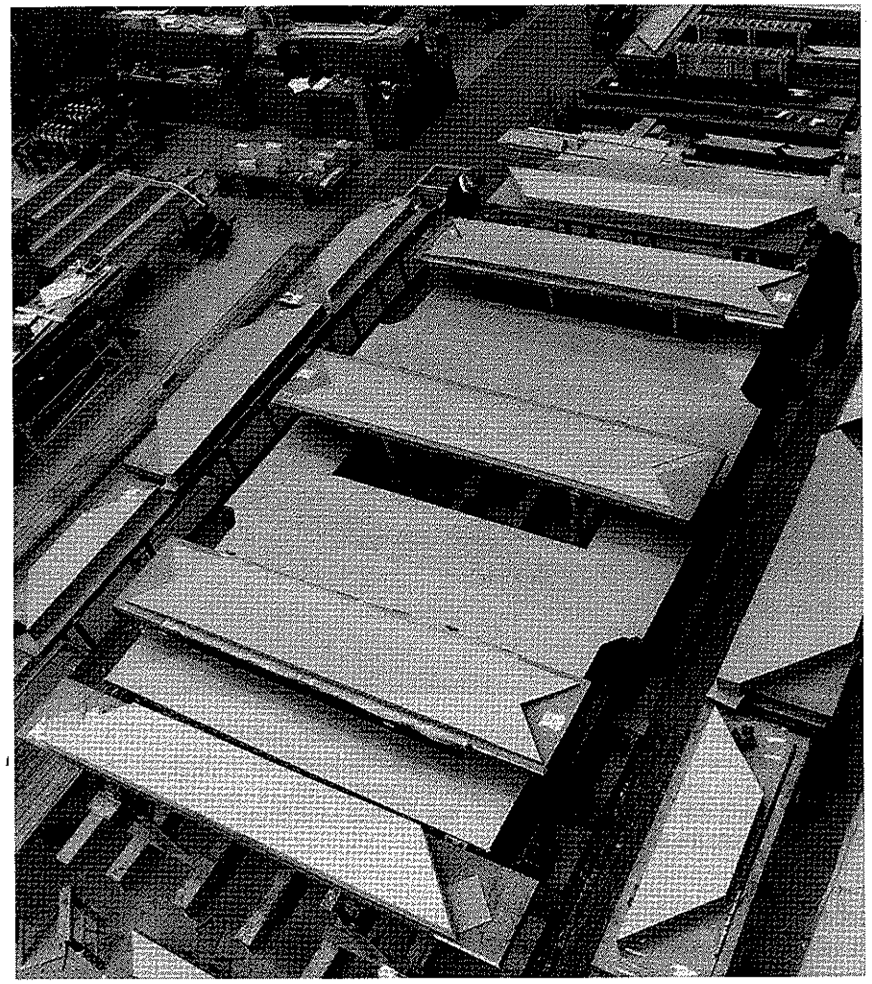


Figure 4.8 Four limb (single-phase with two limbs wound) core with 60/40% yokes and return limbs in course of building. (GEC Alsthom)

Because much of the loss associated with a modern transformer cores arises from the yoke to limb joints manufacturers have given considerable thought to the best method of making these joints. One arrangement which has been used extensively, particularly in distribution transformers, is the *step-lapped* joint. In a step-lapped joint perhaps as many as five different plate lengths are used so that the mitre can have a five-step overlap as shown in *Figure 4.9* rather than the simple overlap shown in *Figure 4.5*. This arrangement which allows the flux transfer to be gradual through the joint ensures a smoother transfer of the flux and thus provides a lower corner loss. The disadvantages

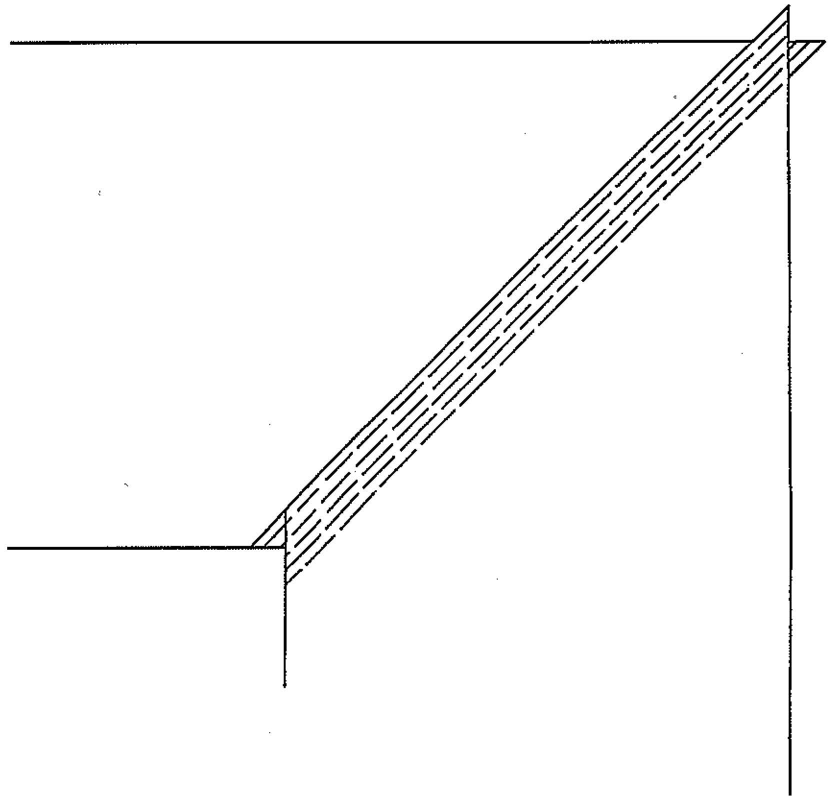


Figure 4.9 Five-step step-lapped mitred core joint

are that more lengths of plate must be cut, which will increase costs, and the replacing of the top yoke after installation of the windings becomes a more complex process requiring greater care and thus further increased labour costs. On a distribution transformer core the smaller, stiffer laminations are probably easier to replace than would be the case on a larger core, which is possibly the reason why this form of construction has found wider application in distribution transformers. It is also the case that the corner joints represent a larger proportion of the total core in the case of a small distribution transformer than they do in a larger power transformer core, making such an improvement more worthwhile. (Of course, the other side of the coin is that it must be easier to cut and build a small core, having a yoke length of, say, 1 metre, to a degree of tolerance which results in joint gaps of, say, 0.5 mm, than it is for a large core having a yoke length of, say, 4 metres.) An additional factor is that the very competitive state of the world distribution transformer market probably means than any savings which can be made, however small, will be keenly sought after.

Core earthing

Before concluding the description of core construction, mention should be made of the subject of core earthing. Any conducting metal parts of a transformer, unless solidly bonded to earth, will acquire a potential in operation which depends on their location relative to the electric field within which they lie. In theory, the designer could insulate them from earthed metal but, in practice, it is easier and more convenient to bond them to earth. However, in adopting this alternative, there are two important requirements:

- The bonding must ensure good electrical contact and remain secure throughout the transformer life.
- No conducting loops must be formed, otherwise circulating currents will result, creating increased losses and/or localised overheating.

Metalwork which becomes inadequately bonded, possibly due to shrinkage or vibration, creates arcing which will cause breakdown of insulation and oil and will produce gases which may lead to Buchholz relay operation, where fitted, or cause confusion of routine gas-in-oil monitoring results (see Section 7 of Chapter 6) by masking other more serious internal faults, and can thus be very troublesome in service.

The core and its framework represent the largest bulk of metalwork requiring to be bonded to earth. On large, important transformers, connections to core and frames can be individually brought outside the tank via 3.3 kV bushings and then connected to earth externally. This enables the earth connection to be readily accessed at the time of initial installation on site (see Section 4 of Chapter 5) and during subsequent maintenance without lowering the oil level for removal of inspection covers so that core insulation resistance checks can be carried out.

In order to comply with the above requirement to avoid circulating currents, the core and frames will need to be effectively insulated from the tank and from each other, nevertheless it is necessary for the core to be very positively located within the tank particularly so as to avoid movement and possible damage during transport. It is usual to incorporate location brackets within the base of the tank in order to meet this requirement. Because of the large weight of the core and windings these locating devices and the insulation between them and the core and frames will need to be physically very substantial, although the relevant test voltage may be modest. More will be said about this in Chapter 5 which deals with testing.

Leakage flux and magnetic shielding

The purpose of the transformer core is to provide a low-reluctance path for the flux linking primary and secondary windings. It is the case, however, that a proportion of the flux produced by the primary ampere-turns will not be constrained to the core thus linking the secondary winding and vice versa.

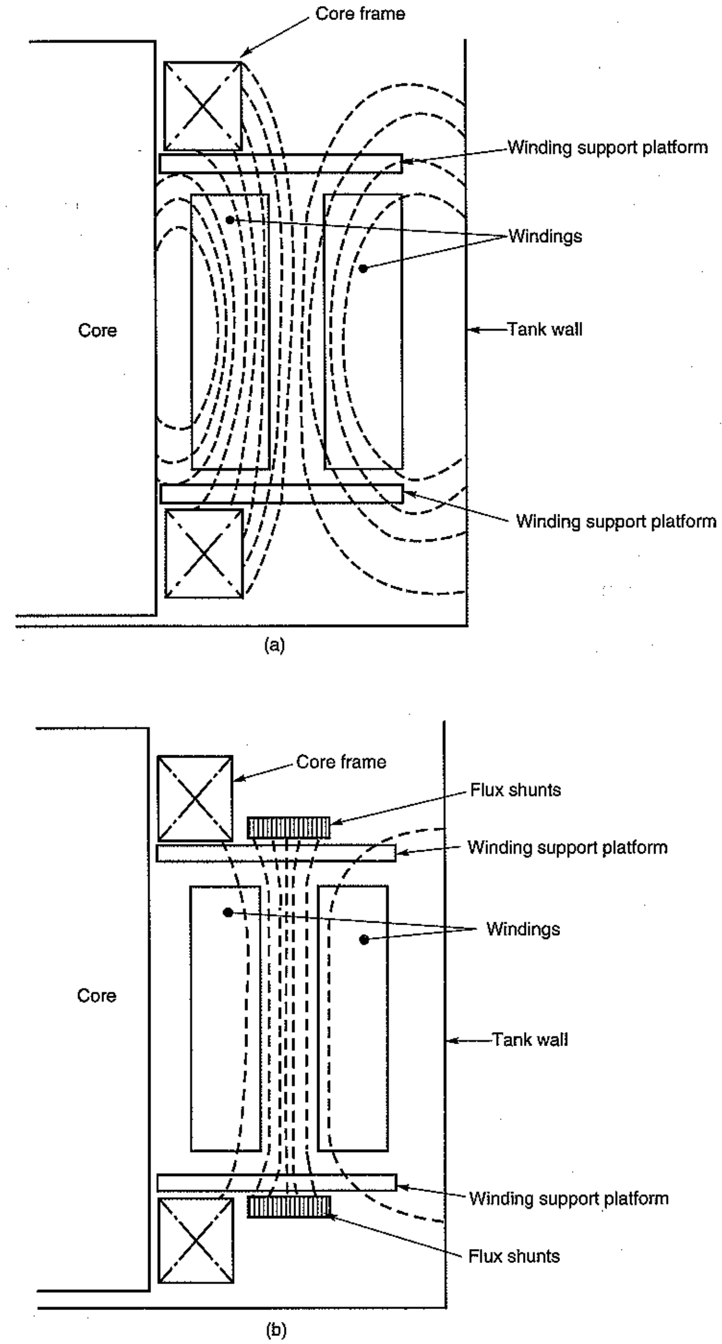


Figure 4.10 (a) Winding leakage flux paths – no shunts;
(b) Winding leakage flux paths modified by the installation of flux shunts

It is this leakage flux, of course, which gives rise to the transformer leakage reactance. As explained in the previous chapter leakage flux also has the effect of creating eddy-current losses within the windings. Control of winding eddy-current losses will be discussed more fully in the section relating to winding design; however, if the leakage flux can be diverted so as to avoid its passing through the winding conductors and also made to run along the axis of the winding rather than have a large radial component as indicated in *Figure 4.10*, this will contribute considerably to the reduction of winding eddy-current losses. The flux shunts will themselves experience losses, of course, but if these are arranged to operate at modest flux density and made of similar laminations as used for the core, then the magnitude of the losses in the shunts will be very much less than those saved in the windings. Requirements regarding earthing and prevention of circulating currents will, of course, be the same as for the core and frames. On very high-current transformers, say where the current is greater than about 1000 A, it is also the case that fluxes generated by the main leads can give rise to eddy-current losses in the tank adjacent to these. In this situation a reduction in the magnitude of the losses can be obtained by the provision of flux shunts, or shields, to prevent their flowing in the tank. This arrangement, shown in *Figure 4.11*, will also prevent an excessive temperature rise in the tank which could occur if it were allowed to carry the stray flux.

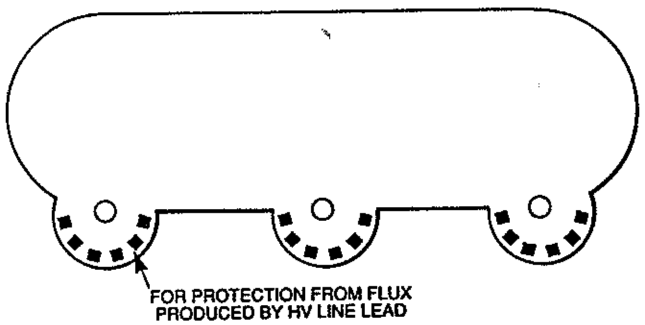


Figure 4.11 Flux shields for main leads

4.2 TRANSFORMER WINDINGS

In describing the basic principles of a two-winding transformer, it has been assumed that the windings comprise a discrete primary and secondary, each being a cylinder concentric with the wound limb of the core which provides the low-reluctance path for the interlinking flux. Whether of single-phase or three-phase construction, the core provides a return flux path and must, therefore, enclose the winding, as shown in *Figure 4.12*. As well as dictating the overall size of the transformer, the size of the two concentric windings thus dictate the size of the window that the core must provide, and hence fix the dimensions of the core which, for a given grade of core steel and flux density, will determine the iron losses. The designer must aim for as compact a winding arrangement as possible. Militating against this are the needs to provide space for cooling ducts and insulation, and also to obtain as large a copper cross-section as possible in order to minimise load losses.

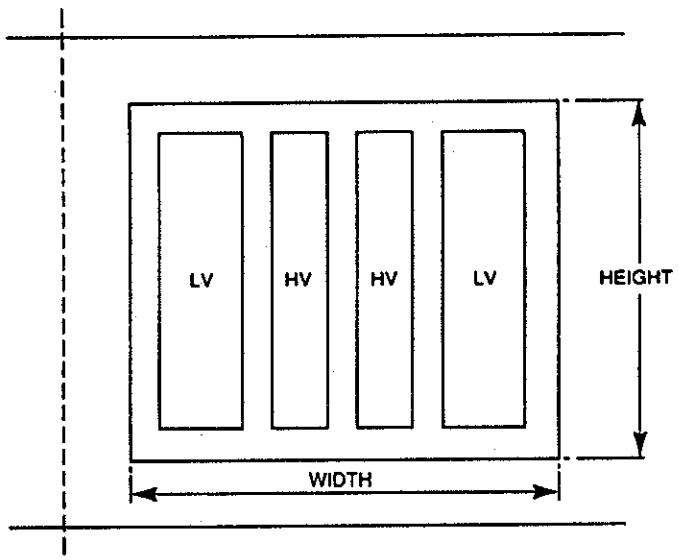


Figure 4.12 Arrangement of windings within core window

The following section describes how the best compromise between these conflicting objectives is achieved in practice. Firstly, it is necessary to look more closely at the subject of load losses. By definition the load loss of a transformer is that proportion of the losses generated by the flow of load current and which varies as the square of the load. There are three categories of load loss which occur in transformers:

- Resistive losses, often referred to as I^2R losses.
- Eddy-current losses in the windings due to the alternating leakage fluxes cutting the windings.

- So-called stray losses in leads, core framework and tank due to the action of load-dependent, stray, alternating fluxes.

More will be said about the third of these later. At the moment it is appropriate to examine the losses which occur in windings. These are by far the most significant proportion.

Resistive losses, as the term implies, are due to the fact that the windings cannot be manufactured without electrical resistance and therefore cannot be eliminated by the transformer designer. There are, however, ways normally open to the designer whereby they can be reduced. These are as follows:

- Use of the lowest resistivity material. This, of course, normally means high-conductivity copper.
- Use of the lowest practicable number of winding turns.
- Increasing the cross-sectional area of the turn conductor.

Minimising the number of winding turns means that a core providing the highest practicable total flux must be used. This implies highest acceptable flux density and the largest practicable core cross-section. The penalty of this option is the increase in core size (frame size) which, in turn, increases iron weight and hence iron loss. Load loss can thus be traded against iron loss and vice versa. Increased frame size, of course, increases the denominator in the expression for per cent reactance (equation (2.1), Chapter 2) so that l, the axial length of the winding, must be reduced in order to compensate and maintain the same impedance, although there will be a reduction in F, the winding ampere-turns by way of partial compensation (since a reduction in the number of turns was the object of the exercise). Reduction in the winding axial length means that the core leg length is reduced, which also offsets the increase in core weight resulting from the increased frame size to some extent. There is thus a band of one or two frame sizes for which the loss variation is not too great, so that the optimum frame size can be chosen to satisfy other factors, such as ratio of fixed to load losses or transport height (since this must be closely related to the height of the core).

The penalty for increasing the cross-section of the turn conductor is an increase in winding eddy-current loss (in addition to the increase in the size of the core window and hence overall size of the core). Eddy-current loss arises because of the leakage flux cutting the winding conductors. This induces voltages which cause currents to flow at right angles to the load current and the flux. The larger the cross-section of the turn the lower will be the resistance to the eddy-current flow and hence the larger the eddy currents. The only way of increasing resistance to the eddy currents without reducing the turn cross-section is to subdivide the turn conductor into a number of smaller strands or subconductors individually insulated from each other (*Figure 4.13*) and transposing these along the length of the winding. The practical aspects of transposition will be described below in the section dealing with winding

construction. In reality, although the winder will prefer to use a reasonably small strand size in order that he can bend these more easily around the mandrel in producing his winding, in general the greater the number of strands in parallel the more costly it becomes to make the winding, so a manufacturer will wish to limit the number of these to the minimum commensurate with an acceptable level of eddy-current loss – more on this later. In addition the extra interstrand insulation resulting on the increased number of strands will result in a poorer winding space factor providing yet another incentive to minimise the number of strands.

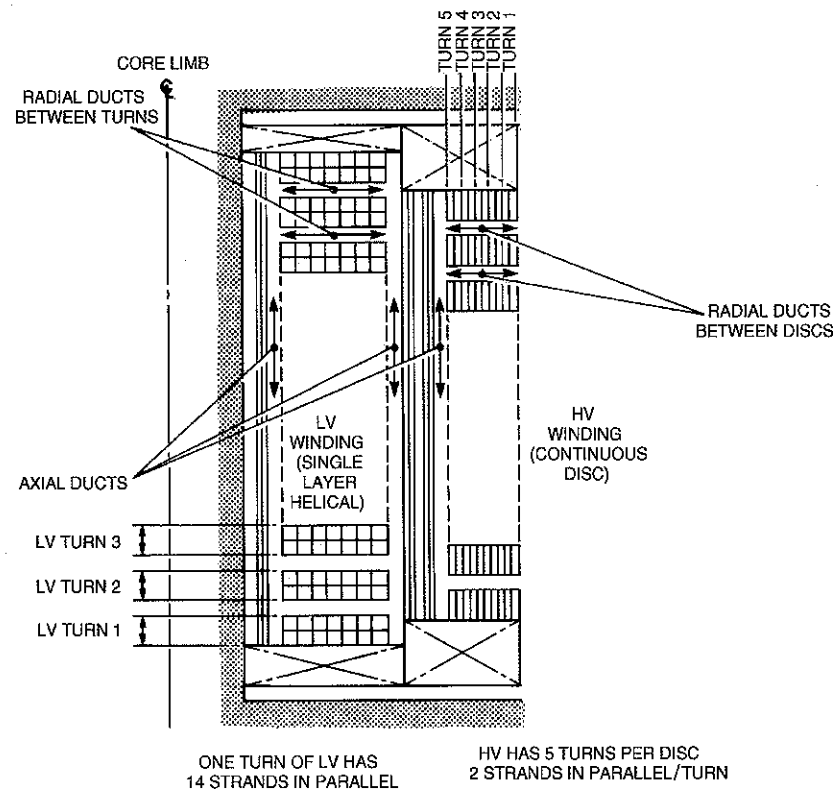
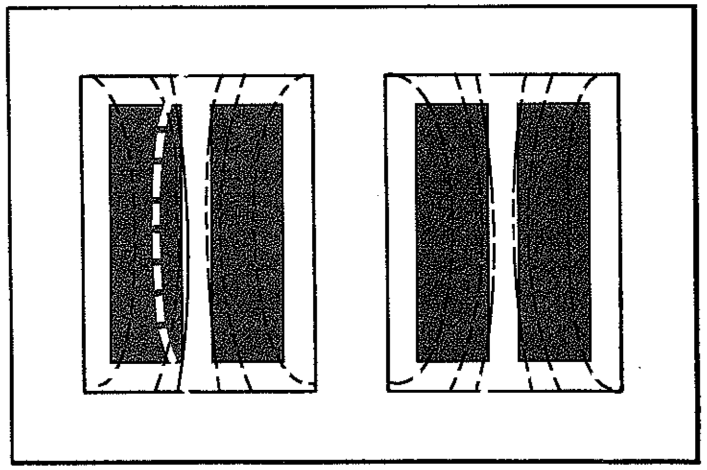
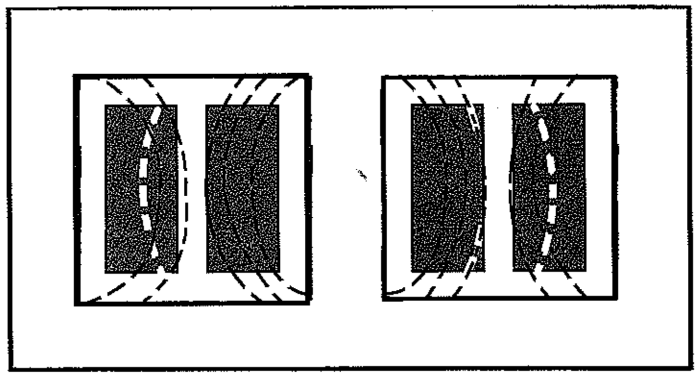


Figure 4.13 Section of LV and HV windings showing radial and axial cooling ducts

As explained above, eddy currents in winding conductors are the result of leakage flux, so a reduction in leakage flux results in smaller eddy currents. It will therefore be evident that in a transformer having a low leakage reactance, winding eddy currents are less of a problem than one with high reactance. Physically this can be interpreted by examination of equation (2.1), Chapter 2, which shows that a low leakage reactance is associated with a long or large



(a) Leakage flux paths in tall single-phase core



(b) Leakage flux in squat core

Figure 4.14 Leakage flux paths in tall and squat windings

winding axial length, l, that is, a tall, slim design will have less leakage flux than a short, squat design and will therefore tend to have less winding eddy currents (*Figure 4.14*). It will also be apparent that with a tall, slim arrangement the leakage flux is largely axial and it can be shown that when this is the case, it is only necessary to subdivide the conductor in the direction perpendicular to the leakage flux, that is, in the radial dimension. With the short, squat winding arrangement the flux will also have a significant radial component, particularly near to the ends of the windings, so that the conductor must be subdivided additionally in the axial dimension. (In theory this would only be necessary near the ends of the windings, but it is not generally feasible to

change the number of conductors mid-winding.) Another method of controlling winding eddy currents, mentioned in the previous section, is the use of flux shunts to modify the leakage flux patterns with the aim of ensuring that these do not pass through windings and where they do so their path will be predominantly axial (*Figure 4.10*). Such measures will only tend to be economic in the larger high-impedance transformers where winding eddy currents prove particularly problematical. In practice, manufacturers find it is economic to limit eddy-current loss to about 25% of that of the resistive loss, although the degree of sophistication necessary to achieve this will vary greatly according to the circumstances and in low-impedance designs the level might easily be considerably less than this without resort to any special features.

Winding construction

Chapter 3 briefly considered the requirements for copper as used in transformer windings and explained why this material is used almost exclusively. Before discussing the details of transformer windings further it is necessary to look a little more closely at winding conductors.

Mention has already been made in the previous chapter that winding conductors for all transformers larger than a few kVA are rectangular in section (*Figure 4.13*). Individual strands must be insulated from each other within a winding conductor and, of course, each conductor must be insulated from its neighbour. This is achieved by wrapping the strands helically with paper strip, and at least two layers are used, so that the outer layer overlaps the butt joints in the layer below. The edges of the copper strip are radiused in order to assist in paper covering. This also ensures that, where strands are required to cross each other at an angle, there will be less 'scissor action' tending to cut into the insulation. Where conditions demand it, many layers of conductor insulation can be applied and the limit to this is determined by the need to maintain a covered cross-section which can be built into a stable winding. This demands that, particularly when they have to have a thick covering of insulation, winding conductors should have a fairly flat section, so that each can be stably wound on top of the conductor below. In practice this usually means that the axial dimension of the strand should be at least twice, and preferable two and a half times, the radial dimension. Conditions may occasionally require that a conductor be wound on edge. This can be necessary in a tapping winding. Such an arrangement can be acceptable if made with care, provided that the winding has only a single layer.

Low-voltage windings

Although the precise details of the winding arrangements will vary according to the rating of the transformer, the general principles remain the same throughout most of the range of power transformers. When describing these windings it is therefore convenient to consider specific cases and it is, hopefully, also of help to the reader to visualise some practical situations.

Generally the low-voltage winding of a transformer is designed to approximately match the current rating of the available low-voltage (LV) switchgear so that, regardless of the voltage class of the transformer, it is likely to have an LV current rating of up to about 2400 A. Occasionally this might extend to 3000 A and, as an instance of this, the majority of the UK power stations having 500 and 660 MW generating units installed have station transformers with a nominal rating of 60 MVA and rated low-voltage windings of 11 kV, 3000 A. This current rating matched the maximum 11 kV air-break circuit-breakers which were available at the time of the construction of these stations. For the low-voltage winding of most transformers, therefore, this is the order of the current involved. (There are transformers outside this range, of course; for an 800 MVA generator transformer, the LV current is of the order of 19 000 A.)

The voltage ratio is such that the current in the high-voltage (HV) winding is an order of magnitude lower than this, say, up to about 300 A. In most oil-filled transformers utilising copper conductors, the current density is between 2 and 4 A/mm^2, so the conductor section on the LV winding is of the order of, say, 50 mm × 20 mm and that on the HV winding, say, 12 mm × 8 mm. As explained in Chapter 1, the volts per turn in the transformer is dependent on the cross-sectional area of the core or core frame size. The frame size used depends on the rating of the transformer but, since, as the rating increases the voltage class also tends to increase, the volts per turn usually gives an LV winding with a hundred or so turns and an HV winding with a thousand or more. In practice, the actual conductor sizes and the number of turns used depend on a good many factors and may therefore differ widely from the above values. They are quoted as an indication of the differing problems in designing LV and HV windings. In the former, a small number of turns of a large-section conductor are required; in the latter, a more manageable cross-section is involved, but a very much larger number of turns. It is these factors which determine the types of windings used.

The LV winding is usually positioned nearest to the core, unless the transformer has a tertiary winding (which would normally be of similar or lower voltage) in which case the tertiary will occupy this position:

- The LV winding (usually) has the lower test voltage and hence is more easily insulated from the earthed core.
- Any tappings on the transformer are most likely to be on the HV winding, so that the LV windings will only have leads at the start and finish and these can be easily accommodated at the top and bottom of the leg.

The LV winding is normally wound on a robust tube of insulation material and this is almost invariably of synthetic resin-bonded paper (s.r.b.p.). This material has high mechanical strength and is capable of withstanding the high

loading that it experiences during the winding of the large copper-section coils used for the LV windings. Electrically it will probably have sufficient dielectric strength to withstand the relatively modest test voltage applied to the LV winding without any additional insulation. (See Section 4 of Chapter 3 regarding dielectric strength of s.r.b.p. tubes when used in oil-filled transformers.)

The hundred or so turns of the LV winding are wound in a simple helix, using the s.r.b.p. tube as a former, so that the total number of turns occupy the total winding axial length, although occasionally, for example, where the winding is to be connected in interstar, the turns might be arranged in two helical layers so that the two sets of winding ends are accessible at the top and bottom of the leg. As explained in Chapter 2, winding length is dictated by the impedance required, so that the need to accommodate the total turns within this length will then dictate the dimensions of the individual turn.

Between the winding base tube and the winding conductor, axial insulation board (pressboard) strips are placed so as to form axial ducts for the flow of cooling oil. These strips are usually of a dovetail cross-section (*Figure 4.15*) so that spacers between winding turns can be threaded onto them during the course of the winding. Axial strips are usually a minimum of 8 mm thick and the radial spacers 4 mm. The radial cooling ducts formed by the spacers are

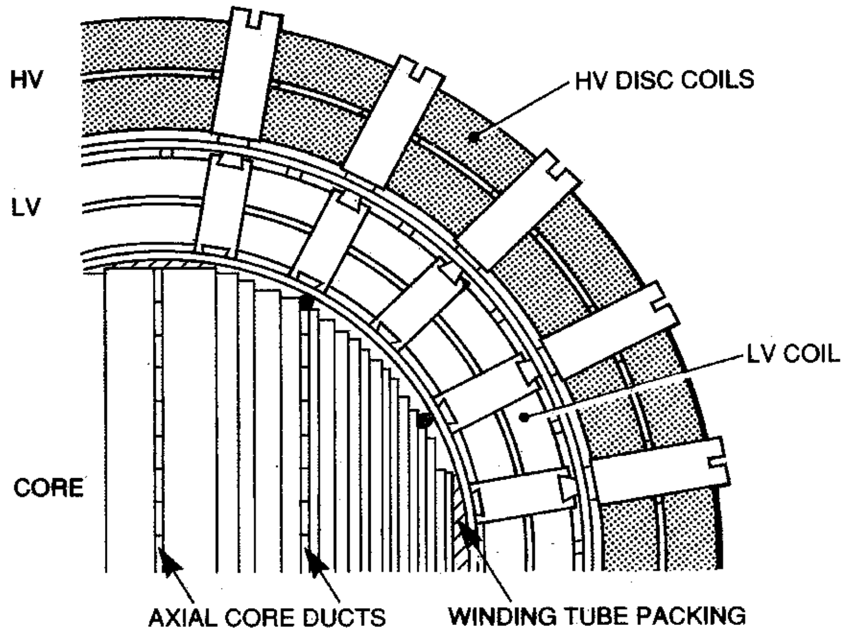


Figure 4.15 Transverse section of core and windings, showing axial cooling ducts above and below windings and dovetailed spacers which form radial ducts

arranged to occur between each turn or every two turns, or even, on occasions, subdividing each turn into half-turns.

Transpositions

It has already been explained that the winding conductor of an LV winding having a large copper cross-section is subdivided into a number of subconductors, or strands, to reduce eddy-current loss and transposing these throughout the length of the winding. Transposition is necessary because of the difference in the magnitude of the leakage flux throughout the radial depth of the winding. If the strands were not transposed, those experiencing the higher leakage flux would be subjected to higher induced voltages and these voltages would cause circulating currents to flow via the ends of the winding where strands are of necessity commoned to make the external connections. Transposition ensures that as nearly as possible each strand experiences the same overall leakage flux. There are various methods of forming conductor transpositions, but typically these might be arranged as shown in *Figure 4.16*. If the winding conductor is subdivided into, say, eight subconductors in the radial dimension, then eight transpositions equally spaced axially are needed over the winding length. Each of these is carried out by moving the inner conductor sideways from below the other seven, which then each move radially inwards by an amount equal to their thickness, and finally the displaced inner conductor would be bent outwards to the outer radial level and then moved to the outside of the stack.

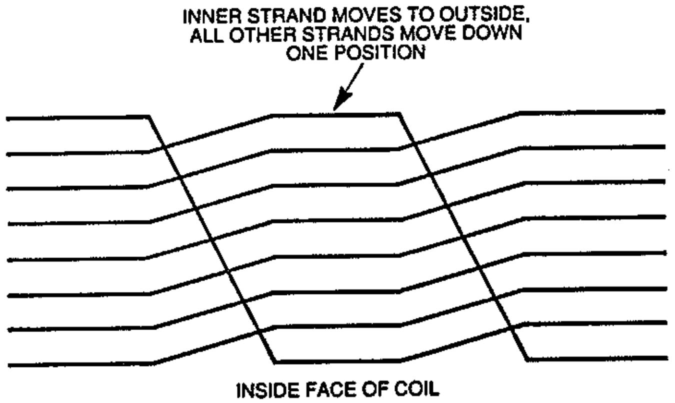


Figure 4.16 Developed section of an eight-strand conductor showing transposition of strands

Continuously transposed strip

Even with an arrangement of transpositions of the type described above and using many subconductors, eddy currents in very high-current windings

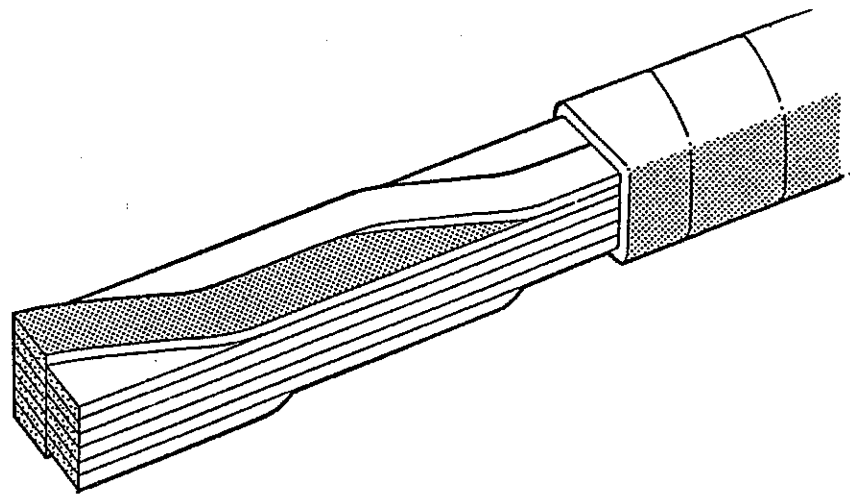


Figure 4.17 Continuously transposed conductor

(perhaps of 2000 A or greater) cannot be easily limited in magnitude to, say, 25% of the resistance losses as suggested above. In addition, transpositions of the type described above take up a significant amount of space within the winding. As a result, in the early 1950s, manufacturers introduced a type of continuously transposed conductor. This enables a far greater number of transpositions to be carried out. In fact, as the name suggests, these occur almost continuously in the conductor itself before it is formed into the winding. Although the 'continuous' transpositions result in some loss of space within the conductor group, this amounts to less space within the winding than that required for conventional transpositions, so that there is a net improvement in space factor as well as improved uniformity of ampere-conductor distribution. *Figure 4.17* shows how the continuously transposed conductor is made up. It has an odd number of strands in flat formation insulated from each other by enamel only and these are in two stacks side by side axially on the finished winding. Transpositions are effected by the top strip of one stack moving over to the adjacent stack as the bottom strip moves over in the opposite direction. The conductor is moved sideways approximately every 50 mm along its length. In addition to the enamel covering on the individual strands, there is a single vertical paper separator placed between the stacks and the completed conductor is wrapped overall with at least two helical layers of paper in the same manner as a rectangular section conductor. Manufacture of the continuously transposed conductor involves considerable mechanical manipulation of the strands in order to form the transpositions and was made possible by the development of enamels which are sufficiently tough and resilient to withstand this. The introduction of continuously transposed strip has been particularly beneficial to the design of large transformers, which must be capable of carrying large currents, but its use is not without some disadvantages of which the following are most significant:

- A single continuously transposed conductor stack which might be up to, say, 12 strands high, and two stacks wide wrapped overall with paper, tends to behave something like a cart spring in that it becomes very difficult to wind round the cylindrical former. This problem can be limited by the use of such strip only for large-diameter windings. It is usual to restrict its use to those windings which have a minimum radius of about 30 times the overall radial depth of the covered conductor.
- When the covered conductor, which has significant depth in the radial dimension, is bent into a circle, the paper covering tends to wrinkle and bulge. This feature has been termed 'bagging'. The bagging, or bulging, paper covering can restrict oil flow in the cooling ducts. The problem can be controlled by restricting the bending radius, as described above, and also by the use of an outer layer of paper covering which has a degree of 'stretch' which will contain the bagging such as the highly extensible paper described in Chapter 3. Alternatively some allowance can be made by slightly increasing the size of the ducts.
- Joints in continuously transposed strip become very cumbersome because of the large number of strands involved. Most responsible manufacturers (and their customers) will insist that a winding is made from one length of conductor without any joints. This does not, however, eliminate the requirement for joints to the external connections. It is often found that these can best be made using crimped connectors but these have limitations and very careful control is necessary in making the individual crimps.
- A high degree of quality control of the manufacture is necessary to ensure that defects in the enamel insulation of the individual strands or metallic particle inclusions do not cause strand-to-strand faults.

High-voltage windings

Mention has already been made of the fact that the high-voltage (HV) winding might have 10 times as many turns as the low-voltage (LV) winding, although the conductor cross-sectional area is considerably less. It is desirable that both windings should be approximately the same axial length subject to the differing end insulation requirements, see below, and, assuming the LV winding occupied a single layer wound in a simple helix, the HV winding would require 10 such layers. A multilayer helical winding of this type would be somewhat lacking in mechanical strength, however, as well as tending to have a high voltage between winding layers. (In a 10-layer winding, this would be one-tenth of the phase voltage.) HV windings are therefore usually wound as 'disc windings'. In a disc winding, the turns are wound radially outwards

one on top of the other starting at the surface of the former. If a pair of adjacent discs are wound in this way the crossover between discs is made at the inside of the discs, both 'finishes' appearing at the outer surfaces of the respective discs. The required number of disc pairs can be wound in this way and then connected together at their ends to form a complete winding. Such an arrangement requires a large number of joints between the pairs of discs (usually individual discs are called *sections*) and so has been largely superseded by the *continuous disc winding*. This has the same configuration when completed as a sectional disc winding but is wound in such a way as to avoid the need for it to be wound in separate disc pairs. When the 'finish' of a disc appears at the outside radius, it is taken down to the mandrel surface using a tapered curved former. From the surface of the mandrel, a second disc is then built up by winding outwards exactly as the first. When this second complete disc has been formed, the tension is taken off the winding conductor, the taper former removed and the turns laid loosely over the surface of the mandrel. These turns are then reassembled in the reverse order so that the 'start' is the crossover from the adjacent disc and the 'finish' is in the centre at the mandrel surface. The next disc can then be built upwards in the normal way. A section of continuous disc winding is shown in *Figure 4.18*.

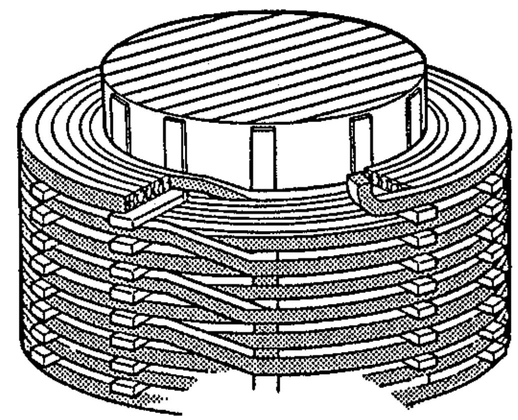


Figure 4.18 Arrangement of continuous disc winding

The operation as described above has been the method of producing continuous disc windings since they were first introduced in about the 1950s. While it may sound a somewhat complex procedure to describe, a skilled winder makes the process appear simple and has no difficulty in producing good quality windings in this way. There are, however, disadvantages of this method of winding. The most significant of these is associated with the tightening of those discs which must be reversed. After reassembling the individual turns of these discs to return the winding conductor to the surface of the mandrel, a procedure which requires that the turns are slack enough to fit inside each other, the winder must then retighten the disc to ensure that the winding is sufficiently stable to withstand any shocks due to faults or short-circuits in service. This tightening procedure involves anchoring the drum from which the conductor is being taken and driving the winding lathe forward. This can result in up to a metre or so of conductor being drawn from the inside of the disc and as this slack is taken up the conductor is dragged across the dovetail strips over which the disc winding is being wound. To ensure that the conductor will slide easily the surface of the strips is usually waxed, but it is not unknown for this to 'snag' on a strip damaging the conductor covering. And, of course this damage is in a location, on the inside face of the disc, which makes it very difficult to see.

The other disadvantage is minor by comparison and concerns only the labour cost of making a continuous disc winding. The process of laying out the disc turns along the surface of the mandrel and reassembling them in reverse order requires skill in manipulation and it is the case that a second pair of hands can be beneficial. In fact when labour costs were very much lower than at present it was standard practice for a winder engaged in producing a continuous disc winding to have the services of a labourer throughout the task. Nowadays such practices are considered to be too costly but nevertheless in many organisations the winder will seek the assistance of a colleague for the more difficult part of the process, which also has cost implications.

Both of the above problems associated with the manufacture of continuous disc windings have been overcome by the introduction of the vertical winding

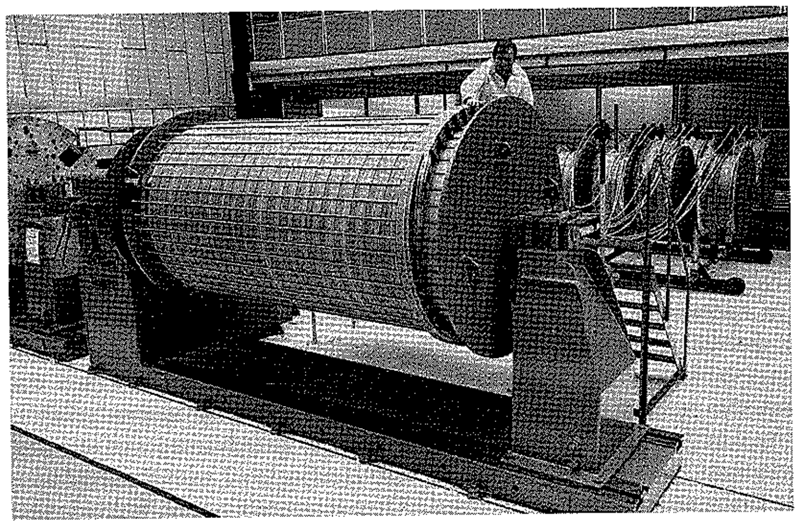


Figure 4.19 Winding in progress – horizontal lathe (Peebles Transformers)

machine which has been used by some manufacturers for many years but whose use became more widespread in the 1980s. From the earliest days of transformer manufacture it has been the practice to wind conductors around horizontal mandrels of the type shown in *Figure 4.19*. *Figure 4.20* shows a modern vertical axis machine which has replaced some of the horizontal axis types in the winding shops of some of the more advanced manufacturers of large high-voltage transformers. On these machines production of continuous disc windings is a much more straightforward and reliable procedure. Using such a machine, the first disc is wound near to the lower end of the mandrel building up the disc from the mandrel surface, outwards, in the normal manner. Then the next disc is wound above this, starting from the outer diameter, proceeding inwards in a conical fashion, over a series of stepped packing pieces of the type shown in *Figure 4.21*. When this 'cone' has been completed, taking the conductor down to the mandrel surface, the packing pieces are removed allowing the cone to 'collapse' downwards to become a disc. This procedure requires only a very small amount of slackness to provide sufficient clearance to allow collapse of the cone, so the tightening process is far less hazardous than on a horizontal machine and furthermore the process can easily be carried out single handed. Vertical machines allow the production of windings of considerably superior quality to those produced using the horizontal type but their installation requires considerably greater capital outlay compared with the cost of procuring and installing a horizontal axis machine.

The HV winding requires space for cooling-oil flow in the same way as described for the LV winding and these are again provided by using dovetail strips over the base cylinder against the inner face of the discs and radial spacers interlocking with these in the same way as described for the LV. Radial cooling ducts may be formed either between disc pairs or between individual discs.

Before concluding the description of the various types of high-voltage winding it is necessary to describe the special type of layer winding sometimes used for very high-voltage transformers and known as a shielded layer winding. Despite the disadvantage of multilayer high-voltage windings identified above namely that of high voltages between layers and particularly at the ends of layers; electrically this winding arrangement has a significant advantage when used as a star-connected high-voltage winding having a solidly earthed star point and employing non-uniform insulation. This can be seen by reference to *Figure 4.22*. If the turns of the winding are arranged between a pair of inner and outer 'shields', one connected to the line terminal and the other to earth, the distribution of electromagnetic voltage within the winding will be the same as the distribution of capacitance voltage if the outer and inner shields are regarded as poles of a capacitor, so that the insulation required to insulate for the electromagnetic voltage appearing on any turn will be the same as that required to insulate for a capacitative voltage distribution. This provides the winding with a high capability for withstanding steep-fronted

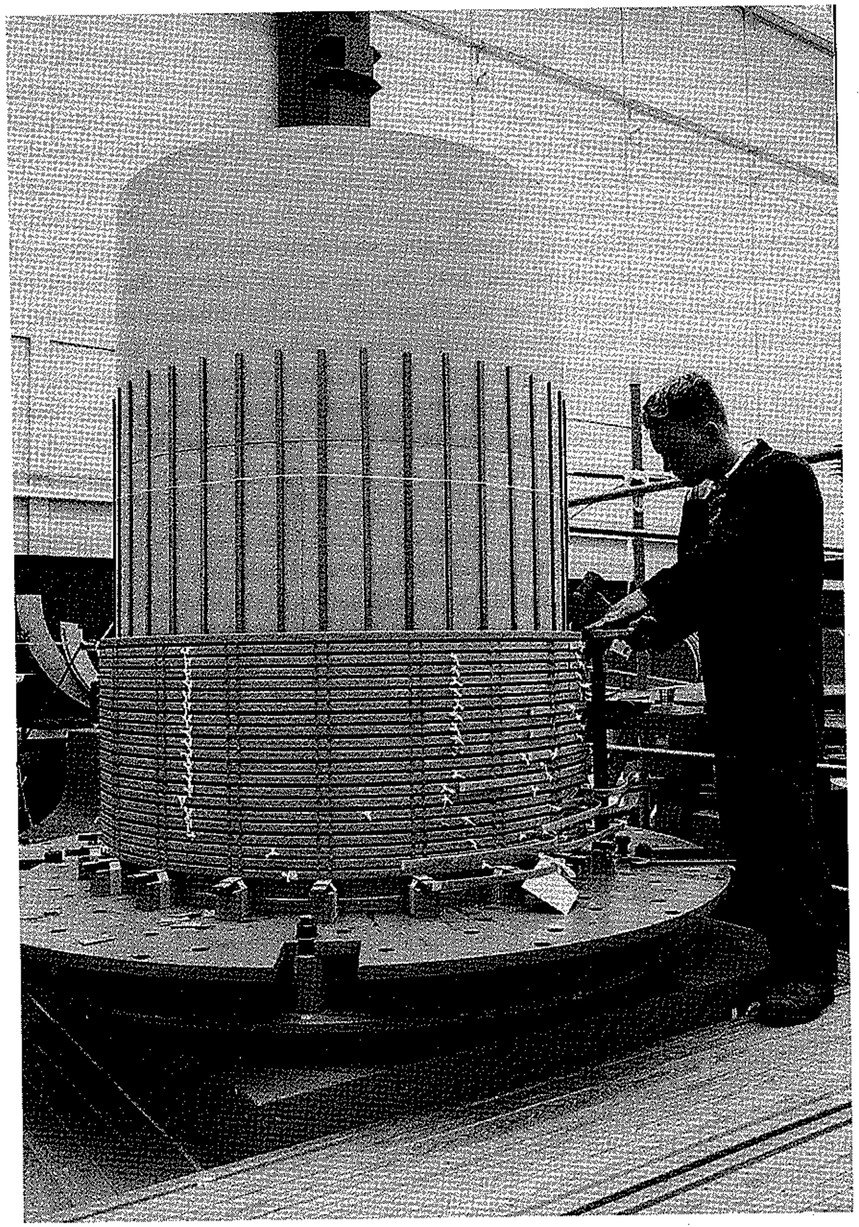


Figure 4.20 Winding in progress – vertical lathe (Peebles Transformers)

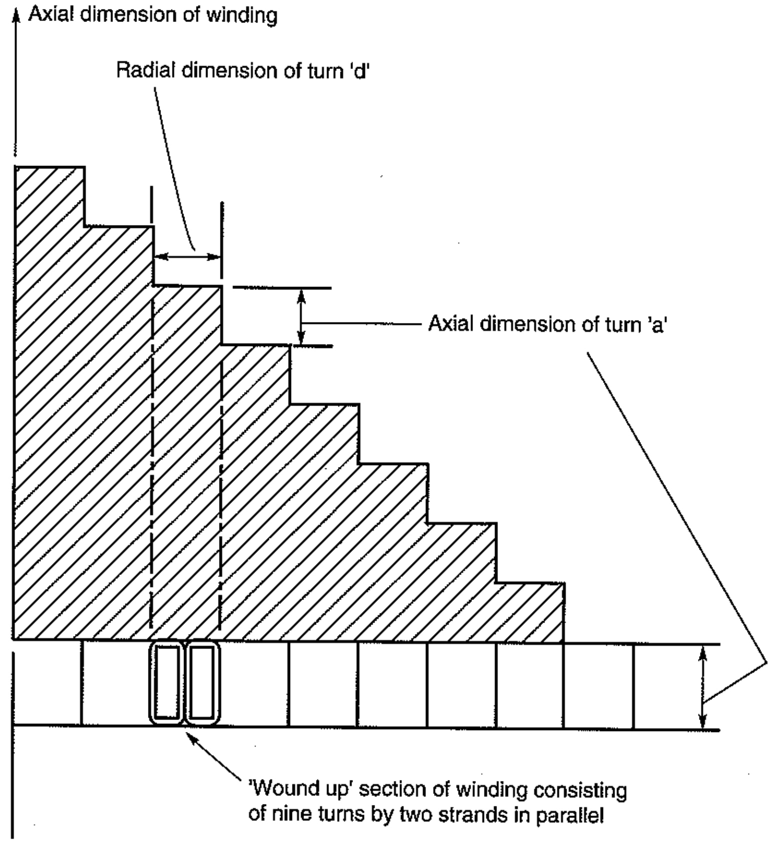


Figure 4.21 Typical stepped wedge used in the production of a continuous disc winding on a vertical axis lathe

waves such as those resulting from a lightning strike on the line close to the transformer. (The next section of this chapter deals in detail with this subject.) *Figure 4.22(a)* shows the ideal arrangement for a shielded layer winding. *Figure 4.22(b)* shows how such a winding might typically be manufactured in practice. As will be apparent from *Figure 4.22(b)*, this type of winding has very poor mechanical strength, particularly in the axial direction, making it difficult to withstand axial clamping forces (see Section 7 of this chapter). There is also a problem associated with the design of the shields. These must be made of very thin conducting sheet, otherwise they attract a high level of stray loss and additionally the line-end shield, being heavily insulated, is difficult to cool, so there can be a problem of local overheating. The shields must have an electrical connection to the respective ends of the winding and making these to flimsy metallic sheets in such a manner that they will withstand a lifetime of high 100 Hz vibration is not easy. These difficulties and in particular the complex insulation structure required make this type must be capable of carrying large currents, but its use is not without some disadvantages of which the following are most significant:

- A single continuously transposed conductor stack which might be up to, say, 12 strands high, and two stacks wide wrapped overall with paper, tends to behave something like a cart spring in that it becomes very difficult to wind round the cylindrical former. This problem can be limited by the use of such strip only for large-diameter windings. It is usual to restrict its use to those windings which have a minimum radius of about 30 times the overall radial depth of the covered conductor.
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High-voltage windings

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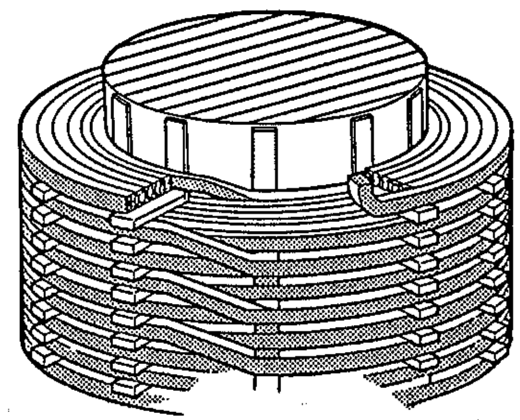

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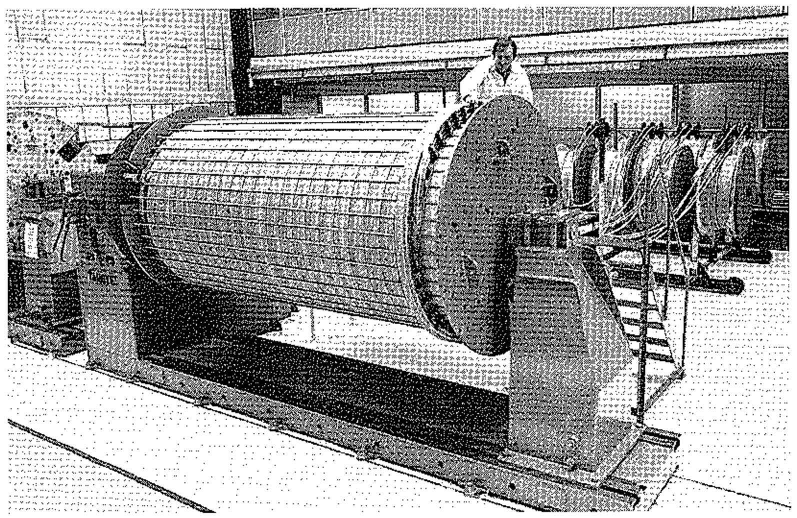


Figure 4.19 Winding in progress – horizontal lathe (Peebles Transformers)

machine which has been used by some manufacturers for many years but whose use became more widespread in the 1980s. From the earliest days of transformer manufacture it has been the practice to wind conductors around horizontal mandrels of the type shown in *Figure 4.19*. *Figure 4.20* shows a modern vertical axis machine which has replaced some of the horizontal axis types in the winding shops of some of the more advanced manufacturers of large high-voltage transformers. On these machines production of continuous disc windings is a much more straightforward and reliable procedure. Using such a machine, the first disc is wound near to the lower end of the mandrel building up the disc from the mandrel surface, outwards, in the normal manner. Then the next disc is wound above this, starting from the outer diameter, proceeding inwards in a conical fashion, over a series of stepped packing pieces of the type shown in *Figure 4.21*. When this 'cone' has been completed, taking the conductor down to the mandrel surface, the packing pieces are removed allowing the cone to 'collapse' downwards to become a disc. This procedure requires only a very small amount of slackness to provide sufficient clearance to allow collapse of the cone, so the tightening process is far less hazardous than on a horizontal machine and furthermore the process can easily be carried out single handed. Vertical machines allow the production of windings of considerably superior quality to those produced using the horizontal type but their installation requires considerably greater capital outlay compared with the cost of procuring and installing a horizontal axis machine.

The HV winding requires space for cooling-oil flow in the same way as described for the LV winding and these are again provided by using dovetail strips over the base cylinder against the inner face of the discs and radial spacers interlocking with these in the same way as described for the LV. Radial cooling ducts may be formed either between disc pairs or between individual discs.

Before concluding the description of the various types of high-voltage winding it is necessary to describe the special type of layer winding sometimes used for very high-voltage transformers and known as a shielded layer winding. Despite the disadvantage of multilayer high-voltage windings identified above namely that of high voltages between layers and particularly at the ends of layers; electrically this winding arrangement has a significant advantage when used as a star-connected high-voltage winding having a solidly earthed star point and employing non-uniform insulation. This can be seen by reference to *Figure 4.22*. If the turns of the winding are arranged between a pair of inner and outer 'shields', one connected to the line terminal and the other to earth, the distribution of electromagnetic voltage within the winding will be the same as the distribution of capacitance voltage if the outer and inner shields are regarded as poles of a capacitor, so that the insulation required to insulate for the electromagnetic voltage appearing on any turn will be the same as that required to insulate for a capacitative voltage distribution. This provides the winding with a high capability for withstanding steep-fronted

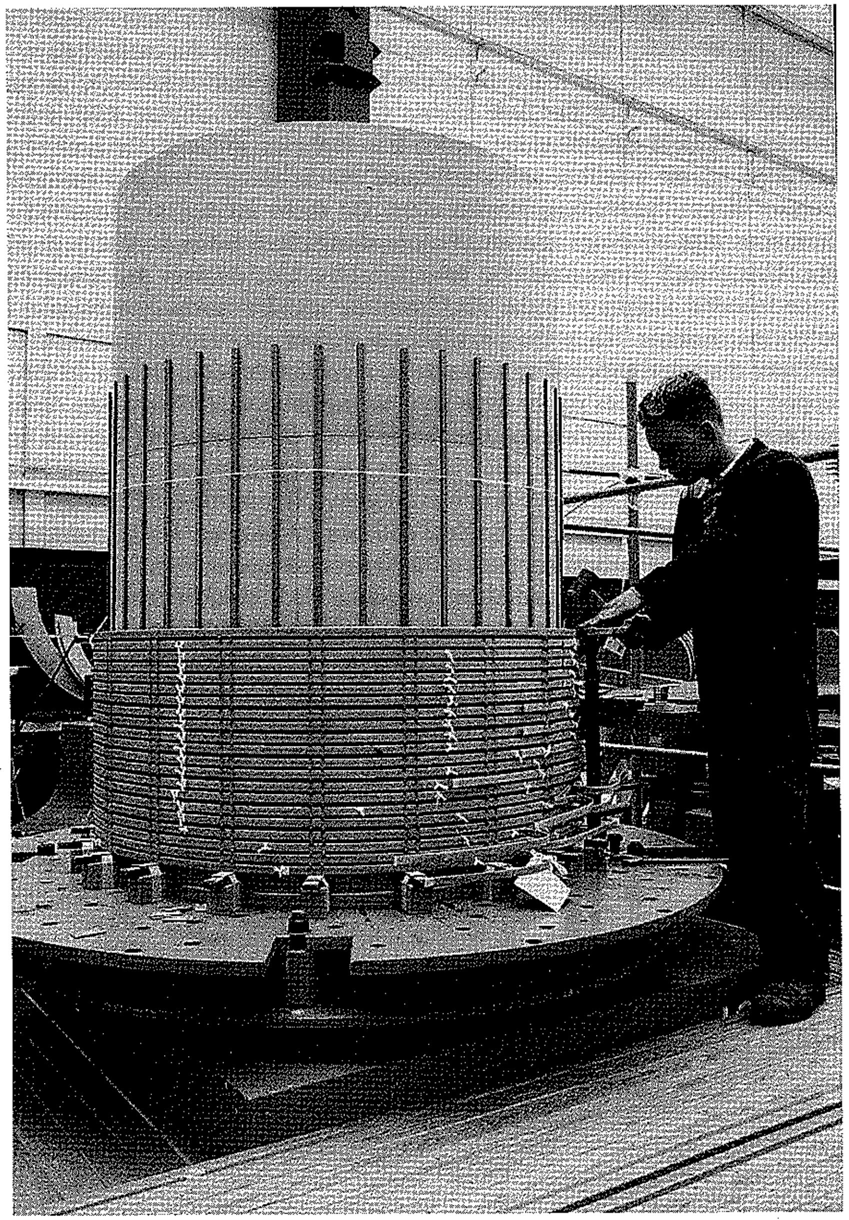


Figure 4.20 Winding in progress – vertical lathe (Peebles Transformers)

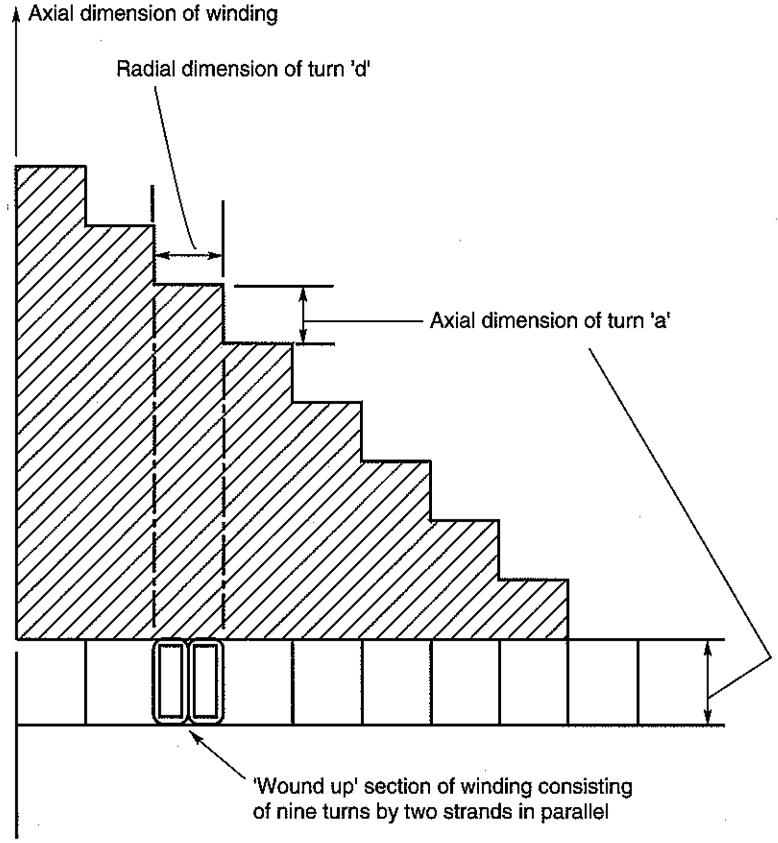
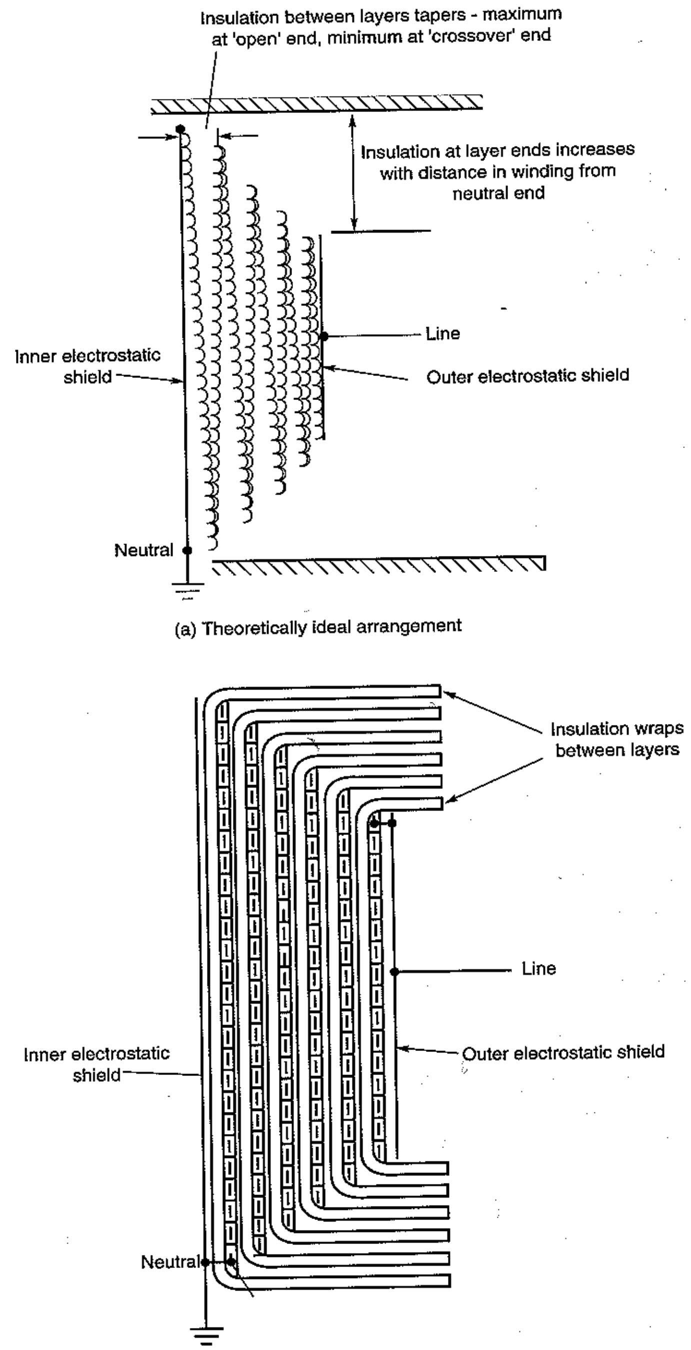


Figure 4.21 Typical stepped wedge used in the production of a continuous disc winding on a vertical axis lathe

waves such as those resulting from a lightning strike on the line close to the transformer. (The next section of this chapter deals in detail with this subject.) *Figure 4.22(a)* shows the ideal arrangement for a shielded layer winding. *Figure 4.22(b)* shows how such a winding might typically be manufactured in practice. As will be apparent from *Figure 4.22(b)*, this type of winding has very poor mechanical strength, particularly in the axial direction, making it difficult to withstand axial clamping forces (see Section 7 of this chapter). There is also a problem associated with the design of the shields. These must be made of very thin conducting sheet, otherwise they attract a high level of stray loss and additionally the line-end shield, being heavily insulated, is difficult to cool, so there can be a problem of local overheating. The shields must have an electrical connection to the respective ends of the winding and making these to flimsy metallic sheets in such a manner that they will withstand a lifetime of high 100 Hz vibration is not easy. These difficulties and in particular the complex insulation structure required make this type



(a) Theoretically ideal arrangement

(b) Typical arrangement used in practice

Figure 4.22 Shielded layer windings

of winding very costly to manufacture. Consequently designers have concentrated on improving the response of disc windings to steep-fronted waves. This work has been largely successful in recent years, so that shielded layer-type windings are now rarely used.

Tapping windings

Thus far it has been assumed that power transformers have simply a primary and secondary winding. However, practically all of them have some form of tapping arrangement to allow both for variations of the applied voltage and for their own internal regulation. In the case of distribution and small auxiliary transformers these tappings will probably allow for $\pm 5\%$ variation, adjustable only off-circuit. On larger transformers tappings of $\pm 10\%$ or more might be provided, selectable by means of on-load tapchangers. More will be said later about the subject of tappings and tapchangers. However, it is convenient at this stage to describe the tap windings themselves.

Most power transformers have the tappings in the HV winding for two reasons. Firstly, it is convenient to assume that the purpose of the tappings is to compensate for variations in the applied voltage which, for most transformers, except generator transformers, will be to the HV winding. (Generator transformers are a special case and will be discussed more fully in Chapter 7.) As the applied voltage increases, more tapping turns are added to the HV winding by the tapchanger so that the volts per turn remain constant, as does the LV winding output voltage. If the applied voltage is reduced, tapping turns are removed from the HV winding again keeping the volts per turn constant and so retaining constant LV voltage. From the transformer design point of view, the important aspect of this is that, since the volts per turn remains constant, so does the flux density. Hence the design flux density can be set at a reasonably high economic level without the danger of the transformer being driven into saturation due to supply voltage excursions (see also Chapter 2).

The second reason for locating tappings on the HV side is that this winding carries the lower current so that the physical size of tapping leads is less and the tapchanger itself carries less current.

Since the tappings are part of the HV winding, frequently these can be arranged simply by bringing out the tapping leads at the appropriate point of the winding. This must, of course, coincide with the outer turn of a disc, but this can usually be arranged without undue difficulty.

In larger transformers, the tappings must be accommodated in a separate tapping winding since the leaving of gaps in one of the main windings would upset the electromagnetic balance of the transformer to an unacceptable degree so that out-of-balance forces in the event of an external fault close to the transformer could not be withstood. The separate tapping winding is usually made the outermost winding so that leads can be easily taken away to the tapchanger. The form of the winding varies greatly and each of the arrangements have their respective advantages and disadvantages.

Before describing separate tapping windings further it should be noted that it is always significantly more costly to place the taps in a separate layer because of the additional interlayer insulation that is required. It is always preferable therefore to accommodate the taps in the body of the HV if this is at all possible.

One common arrangement for a separate tapping winding is the multi-start or interleaved helical winding. This is shown diagramatically in *Figure 4.23*. These windings usually occupy two layers but may occasionally have four layers. The arrangement is best described by using a practical example.

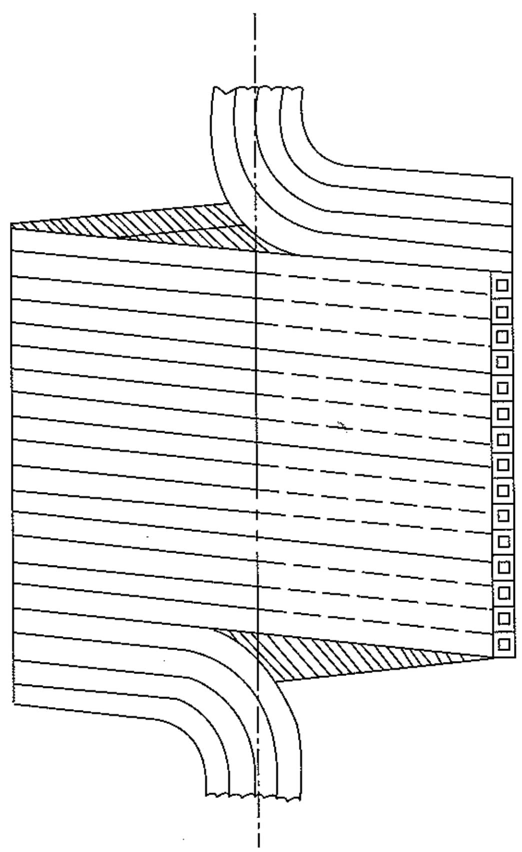


Figure 4.23 Interleaved helical tapping winding having four taps in parallel of five turns per tap

Consider a transformer with a 275 kV star-connected HV winding having a tapping range of plus 10 to minus 20% in 18 steps of 1.67% per step. It has already been suggested that a typical HV winding might have about 1000 turns total. In general, transformers for higher voltages, particularly at the lower end of the MVA rating range, tend to be on smaller frames in relation

to their class so that the number of turns tends to be higher than the average. In this example, and being very specific, assume that the HV winding has 1230 turns on principal tap, so that each tap would require:

$$\frac{1.67}{100} \times 1230 = 20.54 \text{ turns}$$

This means that the tapping winding must provide approximately $20\frac{1}{2}$ turns per tap. Of course, half turns are not possible so this would be accommodated by alternating 20 and 21 turn tapping steps. (In practice the designer would need to be satisfied that his design complied with the requirements of IEC 76, Part 1, as regards tolerance on voltage ratio for all tap positions. This might necessitate the adjustment of the number of turns in a particular tap by the odd one either way compared with an arrangement which simply alternated 20 and 21 turn tapping steps.) One layer of the tapping winding would thus be wound with nine (i.e. half the total number of taps) sets of conductors in parallel in a large pitch helix so that, say, 20 turns took up the full axial length of the layer. There would then be an appropriate quantity of interlayer insulation, say duct-wrap-duct, the ducts being formed by the inclusion of pressboard strips, followed by a further layer having nine sets of 21 turns in parallel. The winding of the layers would be in opposite senses, so that, if the inner layer had the starts at the top of the leg and finishes at the bottom, the outer layer would have starts at the bottom and finishes at the top, thus enabling series connections, as well as tapping leads, to be taken from the top and bottom of the leg. (As stated earlier, the voltage induced in all turns of the transformer will be in the same direction regardless of whether these turns are part of the LV, HV or tapping windings. In order that these induced voltages can be added together, all turns are wound in the same direction. This difference in sense of the windings, therefore, depends upon whether the start is at the top of the leg or at the bottom, or, since most windings are actually wound on horizontal mandrels, whether the start is at the left or the right. In the case of buck/boost tapping arrangements – see Section 7 of this chapter – the winding output voltage is in some cases reduced by putting in-circuit tappings in a subtractive sense, i.e. 'buck', and in other cases increased by putting in-circuit tappings in an additive sense, i.e. 'boost'. The windings themselves are, however, still wound in the same direction.)

The helical interleaved tap winding arrangement has two advantages:

- By distributing each tapping along the total length of the leg a high level of magnetic balance is obtained whether the taps are in or out.
- Helical windings with a small number of turns are cheap and simple to manufacture.

It unfortunately also has disadvantages, the first of which is concerned with electrical stress distribution and is best illustrated by reference to *Figure 4.24*.

Manufacturers design transformers in order to meet a specified test condition so it is the electrical stress during the induced overvoltage test which must be

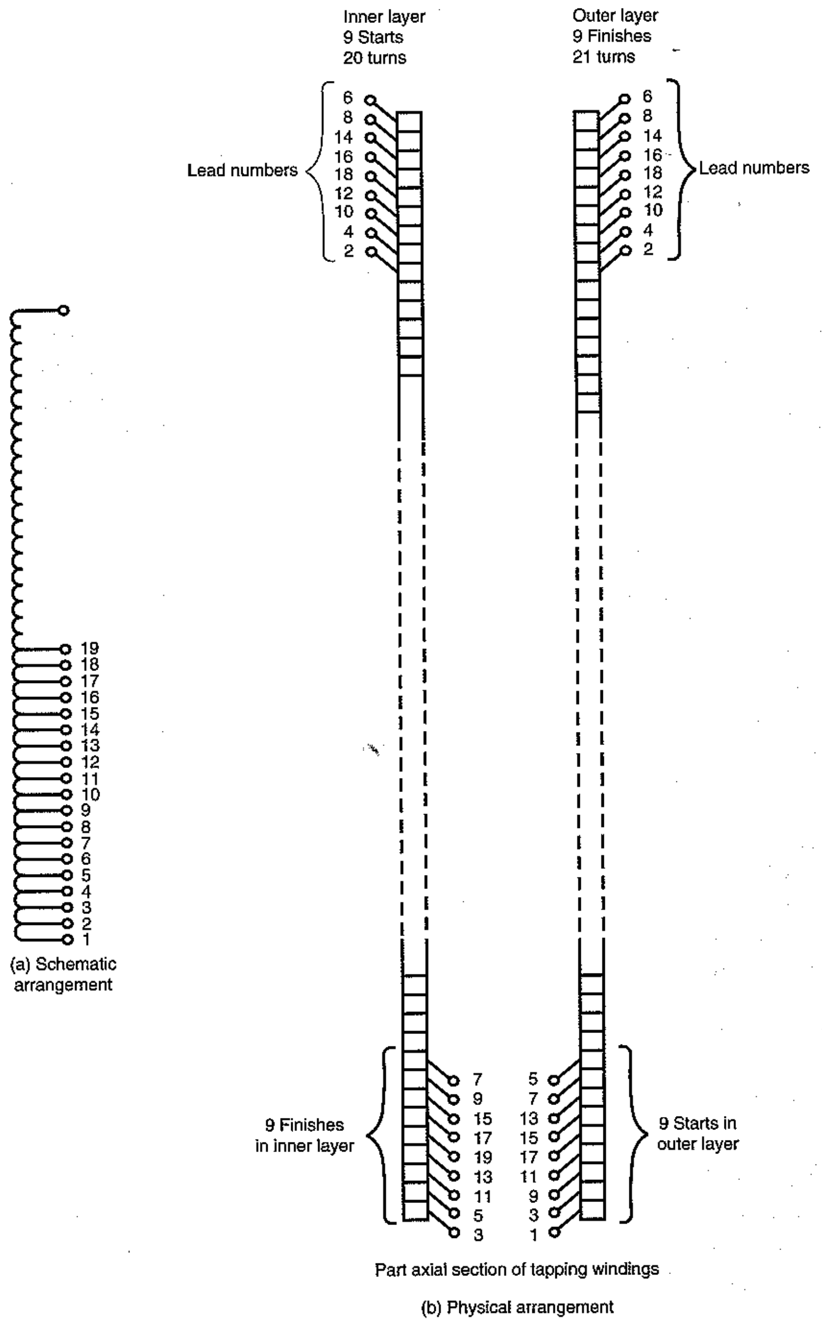


Figure 4.24 Arrangement of two-layer helical interleaved tapping winding

considered. A transformer having an HV voltage of 275 kV will be subjected to an induced overvoltage test at 460 kV (see *Table 5.1* in Section 2 of Chapter 5) and it is permitted in IEC 76 to induce this test voltage on the maximum plus tap, i.e. plus 10% in this example, so that 460 kV must be induced in 110% of the winding turns.

Figure 4.24(b) shows a part section of the tapping layers. It will be apparent from this that it is not advisable to allocate the tapping sections in numerical order, otherwise in the outer layer at the end of the first turn, tapping 1 will be immediately adjacent tapping 17 and in the inner layer, tapping 2 will be adjacent tapping 18. The diagram shows one possible way of distributing the taps so as to reduce the voltage differences between turns which are physically close together. In this arrangement the start of tapping 17 is separated from the start of tapping 1 by the width of three turns. The test voltage appearing between the start of tapping 1 and the start of tapping 17 is that voltage which is induced in 16 tapping steps, which is

$$16 \times \frac{1.67}{100} \times \frac{460}{110} \times 100 = 111.74 \text{ kV, approx.}$$

The width of three turns depends on the total length available for the tapping layer. On a fairly small 275 kV transformer this could be as little as 2 m. In layer one 9 × 20 turns must be accommodated in this 2 m length, so three turns occupy

$$3 \times \frac{2000}{9 \times 20} = 33.33 \text{ mm}$$

and the axial creepage stress is thus

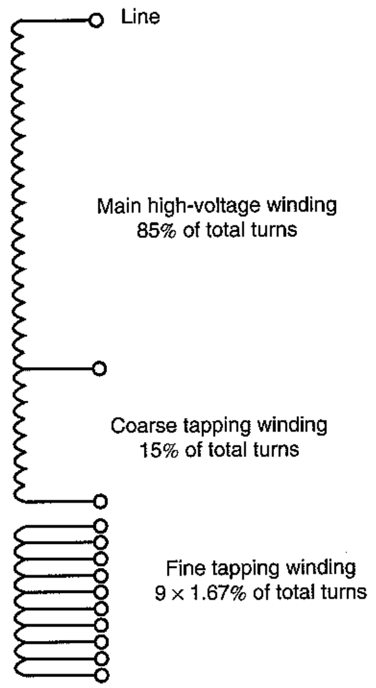
$$\frac{111.74}{33.33} = 3.35 \text{ kV/mm}$$

which is unacceptably high.

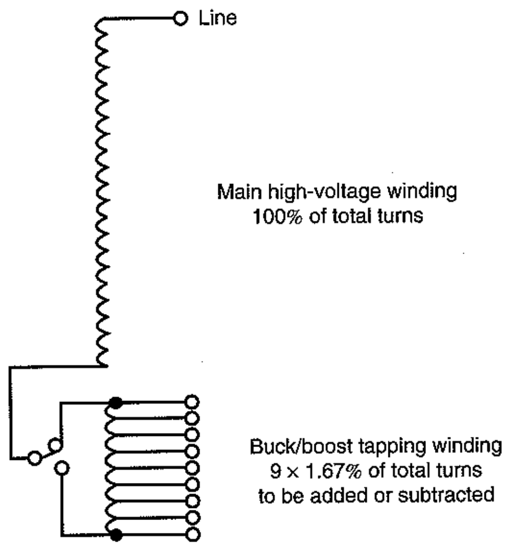
The situation could be greatly improved by opting for four layers of taps rather than two, arranged so that no more than half the tapping-range volts appeared in the same layer.

While the numbers quoted above do not relate to an actual transformer they do illustrate the problem, also showing that design problems frequently arise in very high-voltage transformers at the lower end of the MVA rating band applicable to the voltage class in question.

Another way of resolving this problem would be to use either a coarse/fine or a buck/boost tapping arrangement. These require a more sophisticated tapchanger (see Section 7 of this chapter) but allow the tap winding to be simplified. They can be explained by reference to *Figure 4.25*. With a coarse/fine arrangement (*Figure 4.25(a)*) the tapping winding is arranged in two groups. One, the coarse group, contains sufficient turns to cover about half of the total tapping range and is switched in and out in a single operation, the other, the fine group, is arranged to have steps equivalent to the size of



(a) Coarse/fine arrangement to provide a tapping range of ±15% in 18 steps of 1.67% per step



(b) Buck/boost tapping arrangement to provide a tapping range of ±15% in 18 steps of 1.67% per step

Figure 4.25

tapping step required and is added and subtracted sequentially either with or without the coarse group in circuit. Physically, the coarse group would occupy a third helical layer and no more than half tapping-range volts would appear in one layer. With the buck/boost arrangement, the tapchanger is arranged to put taps in and out with such a polarity as to either add or subtract to or from the voltage developed in the main HV winding. Again, with this arrangement the taps could be contained in two helical layers so that these did not contain more than half tapping-range volts.

The second disadvantage of the helical tapping arrangement concerns its mechanical strength. Under short-circuit conditions (see Section 6 of this chapter) an outer winding experiences an outward bursting force. Such a winding consisting of a small number of turns wound in a helix does not offer much resistance to this outward bursting force and requires that the ends be very securely restrained to ensure that the winding does not simply unwind itself under the influence of such a force. The 20/21 turns in the example quoted above can probably be adequately secured; however, as the transformer gets larger (and the magnitude of the forces increases also) the frame size will be larger, the volts per turn increased and the turns per tap proportionally reduced, so the problem becomes more significant.

The most common alternative to the use of interleaved helical tapping windings is to use disc windings. These at least have the advantage that they can be accommodated in a single layer. The number of turns in an individual tapping section must ideally be equal to an even number of discs, usually a single disc pair. Tapping leads are thus connected between disc pairs so the disc pairs may be joined at this point also, that is, it is just as convenient to make up the winding from sectional disc pairs as to use a continuous disc. This former method of manufacture is therefore often preferred. Another advantage of using a disc winding is that the discs can be arranged in the normal tapping sequence so that the full volts across the tapping range is separated by the full axial length of the tap winding.

A third possibility for the tapping winding is to utilise a configuration as for the disc-wound taps described above but nevertheless to wind each tap section as a helix. This arrangement might be appropriate at the lower end of the size range for which a separate tapping winding is necessary so that the radial bursting forces under short-circuit are not too great. In the example quoted above, a figure of around 20 turns per tap would lend itself ideally to a disc arrangement having 10 turns per disc, that is, 20 turns per disc pair. The example quoted was, however, quite a high-voltage transformer. Often the number of turns per tap will be very much less, possibly as few as six or seven. Such a small number does not lend itself so well to a pair of discs and hence a helical arrangement must be considered, which raises the problem of accommodating the necessary number of turns in a single layer. It is here that it might be necessary to wind the conductor on edge. As previously stated, this can be done provided the winding is single layer and of a reasonably large diameter. In fact this might produce a stiffer winding, more able to withstand the radial bursting forces than one in which the conductor was laid flat.

4.3 DISPOSITION OF WINDINGS

Mention has already been made of the fact that the LV winding is placed next to the core because it has the lower insulation level. It is now necessary to look in further detail at the subject of insulation and insulation levels and to examine the effects of these on the disposition of the windings.

Transformer windings may either be *fully insulated* or they can have *graded insulation*. In IEC 76-3 these are termed *uniform insulation* and *non-uniform insulation* respectively. In a fully or uniform insulated winding, the entire winding is insulated to the same level, dictated by the voltage to which the entire winding is to be raised on test.

Graded or non-uniform insulation allows a more economical approach to be made to the design of the insulation structure of a very high-voltage (EHV) winding. With this system, recognition is made of the fact that such windings will be star connected and that the star point will be solidly earthed. The insulation of the earthy end of the winding thus need only be designed for a very nominal level.

Before the adoption of IEC 76 in the UK, BS 171 required that all windings up to 66 kV working level should he uniformly insulated. Above this, which in the UK means 132, 275 and 400 kV, non-uniform insulation was the norm. Although IEC 76-3 allows for either system to be used at all voltage levels, the UK practice has been continued partly for reasons of custom and practice and also because in many instances new equipment being procured must operate in parallel with equipment designed to earlier standards. In addition the systems themselves have been designed for this standard of equipment. Since most transformers having two EHV windings, that is, each winding at 132 kV or higher, tend to be autotransformers, this means that most double-wound transformers will have, at most, only one winding with graded insulation and many will have both windings fully insulated.

Figure 4.26(a) shows the arrangement of a transformer in which both windings are fully insulated. This might be a primary substation transformer, 33/11 kV and perhaps around 20 MVA. The LV winding must withstand an applied voltage test which will raise the entire winding to 28 kV above earth. The winding insulation must therefore withstand this voltage between all parts and earthed metalwork, including the core. Along the length of the winding this test voltage appears across the dovetail strips plus the thickness of the s.r.b.p. tube. At the ends, these strips and the tube are subjected to surface creepage stress, so that the end-insulation distance to the top and bottom yokes must be somewhat greater.

The 33 kV winding is tested at 70 kV above earth. The radial separation between LV and HV must be large enough to withstand this with, say, a single pressboard wrap and spacing strips inside and outside (*Figure 4.26*). The end

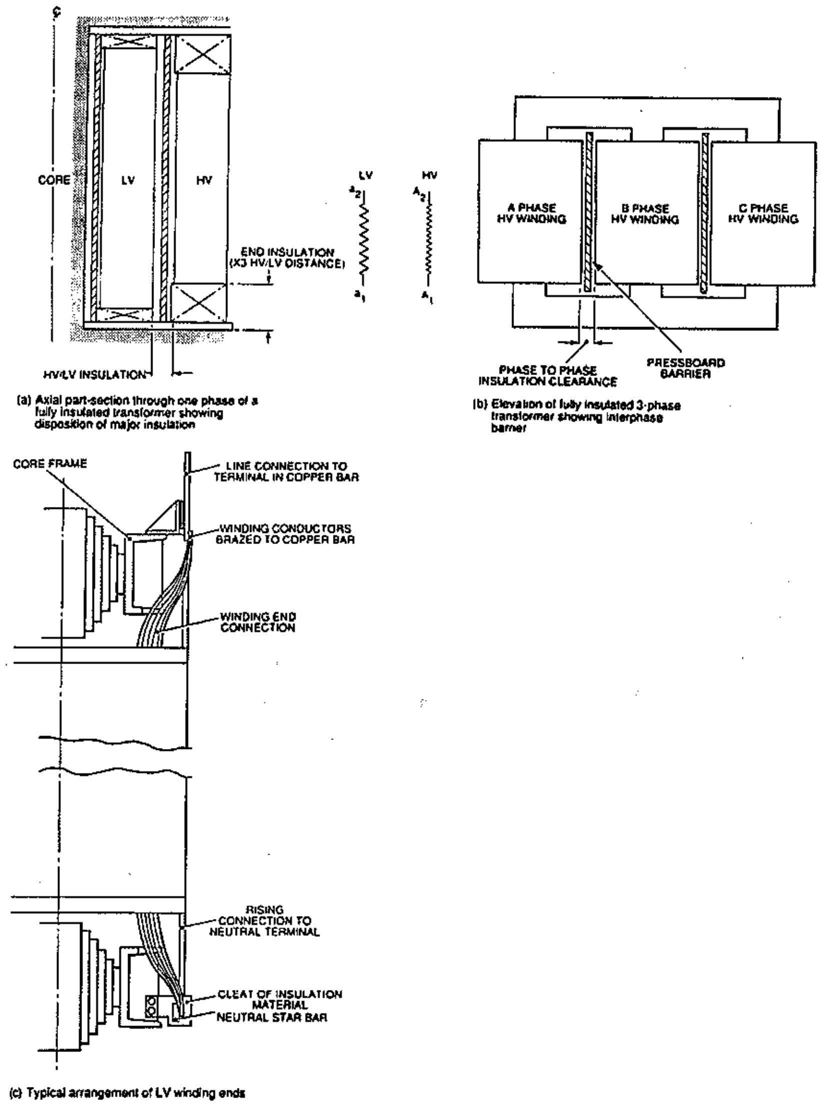


Figure 4.26 Arrangements of windings and leads for transformer having uniform insulation

insulation will be subjected to creepage stress and so the distance to the yoke must be somewhat greater than the HV/LV distance. Between the transformer limbs, the HV windings of adjacent phases come into close proximity.

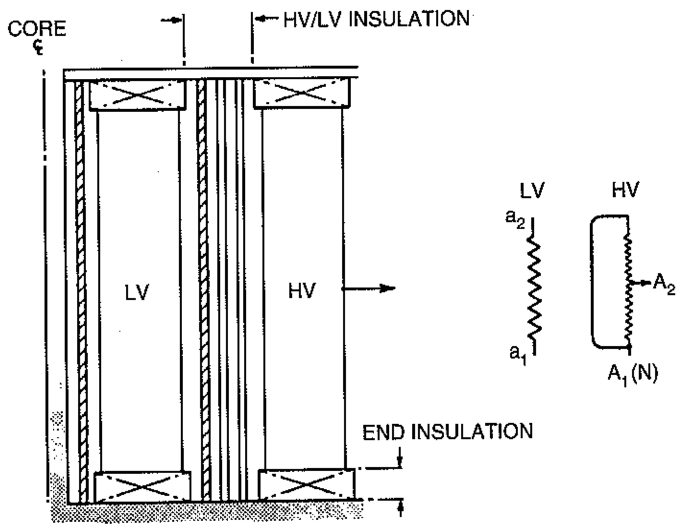
To withstand the 70 kV test voltage between phases, it is necessary to have a clearance similar to that between HV and LV windings with, say, a single pressboard barrier in the middle of this distance, as shown in *Figure 4.26(b)*.

The LV winding leads are taken out at the top and bottom of the leg, which means that they must of necessity pass close to the core framework. Since they are at relatively low voltage, it is probable that the necessary clearance can be obtained by bending these away from the core as close to the winding as possible and by suitably shaping the core frame (*Figure 4.26(c)*).

The HV winding leads also emerge from the top and bottom of the leg but these are taken on the opposite side of the coils from the LV leads. Being at a greater distance from the core frame than those of the LV winding, as well as having the relatively modest test voltage of 70 kV, these require a little more insulation than those of the LV winding.

It is usually convenient to group the tapping sections in the centre of the HV windings. This means that when all the taps are not in circuit, any effective 'gap' in the winding is at the centre, so that the winding remains electromagnetically balanced. More will be said about this aspect below. The tapping leads are thus taken from the face of the HV winding, usually on the same side of the transformer as the LV leads.

Figure 4.27 shows the arrangement of a transformer in which the LV winding is fully insulated and the HV winding has non-uniform (graded) insulation. This could be a bulk supply point transformer, say, 132/33 kV, star/delta connected, possibly 60 MVA, belonging to a Regional Electricity Company (REC). Some RECs take some of their bulk supplies at 11 kV, in which case the transformer could be 132/11 kV, star/star connected, and might well have a tertiary winding. This too could be 11 kV although it is possible that it might be 415 V in order to fulfil the dual purpose of acting as



Section of transformer with HV winding having non-uniform insulation

Figure 4.27

a stabilising winding and providing local auxiliary supplies for the substation. Whichever the voltage class, it would be placed nearest to the core. If 11 kV the test levels would be the same as the 11 kV LV winding and that of the LV winding of the 33/11 kV transformer described above. If 415 V, the test levels would be very modest and the insulation provided would probably be dictated by physical considerations rather than electrical. In either case the tertiary and LV insulations would be similar to that of the 33/11 kV transformer. The LV winding would be placed over the tertiary and the tertiary to LV gap would require radial and end insulation similar to that between LV and core for the star/delta design. The 132 kV HV winding is placed outside the LV winding and it is here that advantage is taken of the non-uniform insulation.

For 132 kV class non-uniform insulation, when it is intended that the neutral shall be solidly connected to earth, the applied voltage test may be as low as 38 kV above earth. (More will be said about the subject of dielectric test levels in Chapter 5.) When the overpotential test is carried out, at least 230 kV is induced between the line terminal and earth. Consequently the neutral end needs insulating only to a level similar to that of the LV winding, but the line end must be insulated for a very much higher voltage. It is logical, therefore, to locate the line end as far as possible from the core and for this reason it is arranged to emerge from a point halfway up the leg. The HV thus has two half-windings in parallel, with the neutrals at the top and bottom and the line ends brought together at the centre. If, with such an arrangement, the HV taps are at the starred neutral end of the winding, the neutral point can thus be conveniently made within the tapchanger and the voltage for which the tapchanger must be insulated is as low as possible. Unfortunately it is not possible to locate these tapping coils in the body of the HV winding since, being at the neutral end, when these were not in circuit there would be a large difference in length between the HV and LV windings. This would greatly increase leakage flux, stray losses and variation of impedance with tap position as well as creating large unbalanced forces on short-circuit. It is therefore necessary to locate the taps in a separate winding placed outside the HV winding. This winding is shorter than the HV and LV windings and split into upper and lower halves, with an unwound area in the middle through which the HV line lead can emerge.

The centre of the HV winding must be insulated from the LV winding by an amount capable of withstanding the full HV overpotential test voltage. This requires a radial distance somewhat greater than that in the 33/11 kV transformer and the distance is taken up by a series of pressboard wraps interspersed by strips to allow oil circulation and penetration. Alternatively, it is possible that the innermost wrap could be replaced by an s.r.b.p. tube which would then provide the base on which to wind the HV winding. The disadvantage of this alternative is that the HV to LV gap is a highly stressed area for which s.r.b.p. insulation is not favoured (see Chapter 3) so that, while it might be convenient to wind the HV onto a hard tube, the use of such an arrangement would require a reduced high to low design stress and a greater high to low gap. High to low gap, a, appears in the numerator of the expression for percent reactance (equation (2.1) of Chapter 2) multiplied by a factor three. If this is increased then winding axial length, l, must be increased in order to avoid an increase in reactance, thus making the transformer larger. The designer's objective is normally, therefore, to use as low a high to low as possible and it is probably more economic to wind the HV over a removable mandrel so that it can be assembled onto the LV on completion thus avoiding the use of a hard tube.

The voltage appearing on test between the line end of the HV winding and the neutral-end taps is similar to that between HV and LV windings so it is necessary to place a similar series of wraps between the HV and tapping windings. These wraps must be broken to allow the central HV line lead to emerge; an arrangement of petalling (see Chapter 3) or formed collars may be used to allow this to take place without reducing the insulation strength (*Figure 4.28*).

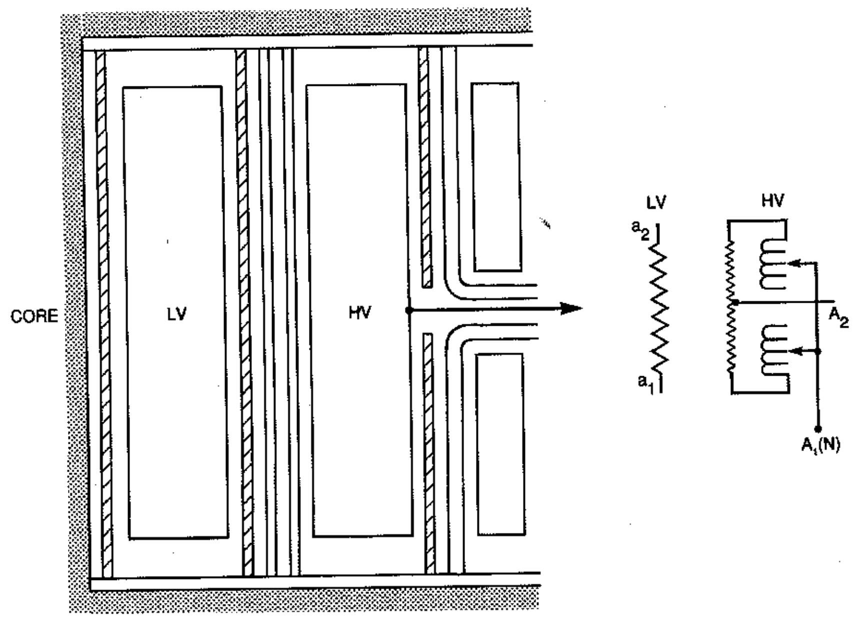


Figure 4.28 Arrangement of HV line lead with outer HV tapping winding and non-uniform insulation

Although the system of non-uniform insulation lends itself well to the form of construction described above, which is widely used in the UK for transformers of 132 kV and over, there are disadvantages and there are also circumstances when this cannot be used. The main disadvantage is seen when the transformer rating is such that the HV current is small. An example will make this clear. Although it is rare to require to transform down from 400 kV

at ratings as low as 60 MVA, it has on occasions occurred, for example to provide station supplies for a power station connected to the 400 kV system where there is no 132 kV available. In this case the HV line current is 86.6 A. With two half HV windings in parallel the current in each half winding is 43.3 A. A typical current density for such a winding might be, say, 3 A/mm^2 so that at this current density the required conductor cross-section is 14.4 mm^2. This could be provided by a conductor of, say, 3×5 mm which is very small indeed and could not easily be built into a stable winding, particularly when it is recognised that possibly one millimetre radial thickness of paper covering might be applied to this for this voltage class. It would therefore be necessary to use a much lower current density than would normally be economic in order to meet the physical constraints of the winding.

This problem can be eased by utilising a single HV winding instead of two half-windings in parallel as indicated in *Figure 4.29*. This would immediately result in a doubling of the conductor strand size so that this might typically become 3×10 mm which is a much more practicable proposition. Of course, the benefit of the central line lead is now no longer available and the winding end must be insulated for the full test voltage for the line end.

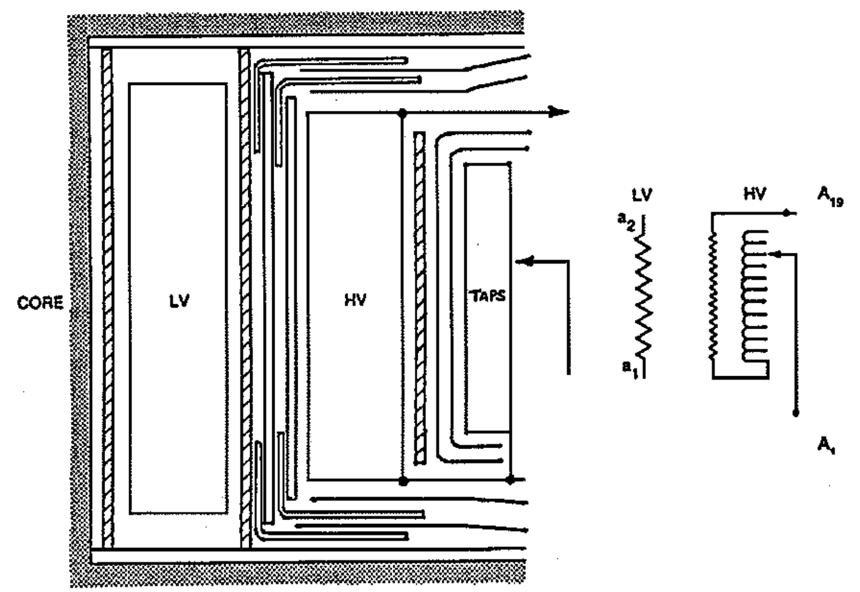


Figure 4.29 Typical arrangement of windings and HV line lead for uniform insulation

Non-uniform insulation cannot be used if the neutral is to be earthed via an impedance, as is often the case outside the UK, nor is it acceptable for a delta-connected HV which would be unlikely to be used in the UK at a voltage of 132 kV and above, but is used occasionally in other countries, so again there is no merit in having the line lead at the centre of the leg. Hence the arrangement shown in *Figure 4.29* would be necessary. If a delta connection is used, any HV tappings must be in the middle of the winding and in order to meet the uniform insulation requirement, the tapchanger must be insulated to the full HV test level. Such a configuration will clearly be more costly than one with non-uniform insulation, but this simply demonstrates the benefit of a solidly earthed neutral as far as the transformer is concerned. No doubt proponents of systems having impedance earthing of the neutral would wish to identify benefits to the system of using this arrangement.

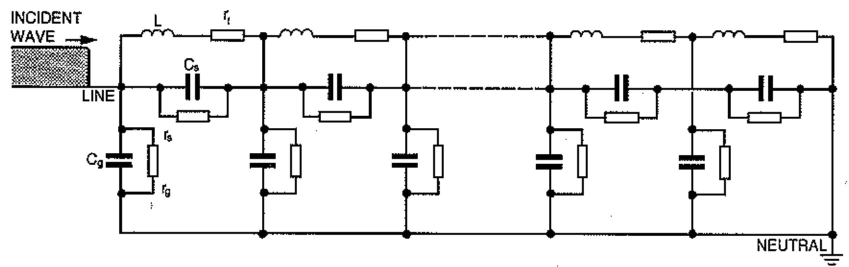
4.4 IMPULSE STRENGTH

The previous section dealt with the disposition of the windings as determined by the need to meet the power frequency tests, or electromagnetic voltage distributions which are applied to the windings, but it also briefly mentioned the need to withstand the effect of steep-fronted waves. When testing a power transformer such waves are simulated by an impulse test, which is applied to the HV line terminals in addition to the dielectric testing at 50 Hz. Impulse testing arose out of the need to demonstrate the ability to withstand such waves, generated by lightning strikes, usually to the high-voltage system to which the transformer is connected. These waves have a much greater magnitude than the power frequency test voltage but a very much shorter duration.

While considering the construction of transformer windings it is necessary to understand something of the different effect which these steep-fronted waves have on them compared with power frequency voltages and to examine the influence which this has on winding design. Section 5 of Chapter 6, which deals with all types of transients in transformers, will go more deeply into the theory and examine the response of windings to lightning impulses in greater detail.

For simulation purposes a standard impulse wave is defined in BS 171 as having a wavefront time of 1.2 μs and a time to decay to half peak of 50 μs. (More accurate definition of these times will be found in Chapter 5 which deals with transformer testing.) When struck by such a steep wavefront, a transformer does not behave as an electromagnetic impedance, as it would to power frequency voltages, but as a string of capacitors as shown in *Figure 4.30*. When the front of the impulse wave initially impinges on the winding, the capacitances C_s to the succeeding turn and the capacitance of each turn to earth C_g predominate, so that the reactance and resistance values can be ignored. It will be shown in Section 5 of Chapter 6 that when a high voltage is applied to such a string, the distribution of this voltage is given by the expression:

$$e_x = \frac{E \sinh \frac{\alpha x}{L}}{\sinh \alpha}$$



THE CIRCUIT PARAMETERS, UNIFORMLY DISTRIBUTED, ARE:

L = INDUCTANCE
C_s = SERIES (TURN-TO-TURN) CAPACITANCE
C_g = SHUNT (TURN-TO-EARTH) CAPACITANCE
r_l = LOSS COMPONENT OF INDUCTANCE (WINDING RESISTANCE)
r_s = LOSS COMPONENT OF SERIES CAPACITANCE
r_g = LOSS COMPONENT OF SHUNT CAPACITANCE

Figure 4.30 Equivalent circuit of transformer for simplified uniform winding

where E = magnitude of the incident wavefront
L = winding length
$\alpha = \sqrt{(C_g/C_s)}$

which represents a curve of the form shown in *Figure 4.31*. The initial slope of this curve, which represents the voltage gradient at the point of application, is proportional to C_g/C_s. In a winding in which no special measures had been taken to reduce this voltage gradient, this would be many times that which would appear under power frequency conditions. If additional insulation were placed between the winding turns, this would increase the spacing between them and thus reduce the series capacitance C_s. C_g would be effectively unchanged, so the ratio C_g/C_s would increase and the voltage gradient become greater still. The most effective method of controlling the increased stress at the line end is clearly to increase the series capacitance of the winding, since reducing the capacitance to earth, which can be partially

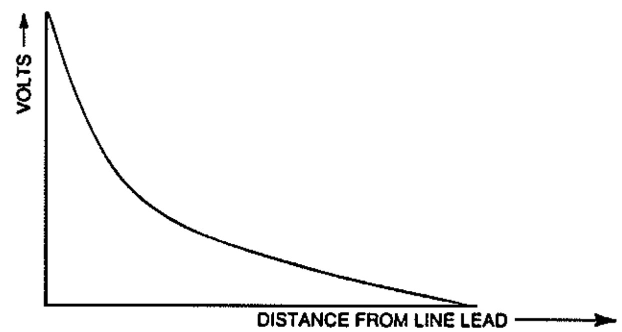


Figure 4.31 Distribution of impulse voltage within winding

achieved by the use of electrostatic shields, is nevertheless not very practicable.* *Figure 4.32* shows several methods by which series capacitance can be increased. The first, *Figure 4.32(a)*, uses an electrostatic shield connected to the line end and inserted between the two HV discs nearest to the line end. The second, *Figure 4.32(b)*, winds in a dummy strand connected to the line lead but terminating in the first disc. Both of these arrangements effectively bring more of the winding turns nearer to the line end. The electrostatic shield was probably the first such device to be used and is possibly still the most widely favoured. The shield itself is usually made by wrapping a pressboard ring of the appropriate diameter with thin metal foil (thin to ensure minimum stray loss – see description of shield for shielded layer winding earlier in this chapter) and then covering this with paper insulation of about the same radial thickness as the winding conductor. It is necessary to make a connection to the foil in order to tie this to the line lead and this represents the greatest weakness of this device, since, as indicated in the description of the shielded layer winding, making a high-integrity connection to a thin foil is not a simple matter.

As the travelling wave progresses further into the winding, the original voltage distribution is modified due to the progressive effect of individual winding elements and their capacitances, self- and mutual inductances and resistance. The voltage is also transferred to the other windings by capacitance and inductive coupling. *Figure 4.33(a)* shows a series of voltage distributions typical of a conventional disc winding having an HV line lead connection at one end. It can be seen that, as time elapses, the voltage distribution changes progressively – the travelling wave being reflected from the opposite end of the winding back towards the line end, and so on. These reflections interact with the incoming wave and a complex series of oscillations occur and reoccur until the surge energy is dissipated by progressive attenuation and the final distribution (*Figure 4.33(b)*) is reached.

Thus it is that, although the highest voltage gradients usually occur at or near to the line-end connection coincident with the initial arrival of the impulse wave, these progress along the winding successively stressing other parts and, while these stresses might be a little less than those occurring at the line end they are still likely to be considerably greater than those present under normal steady-state conditions. In many instances, therefore, stress control measures limited to the line end will be insufficient to provide the necessary dielectric strength and some form of interleaving is required (*Figure 4.32(c)*). This usually involves winding two or more strands in parallel and then reconnecting the ends of every second or fourth disc after winding to give the interleaving arrangement required. It has the advantage over the first two methods that it does not waste any space, since every turn remains active. However, the

* A short, squat winding tends to have a lower capacitance to ground than a tall slim winding, so such an arrangement would have a better intrinsic impulse strength. There are, however, so many other constraints tending to dictate winding geometry that designers are seldom able to use this as a practical means of obtaining the required impulse strength.

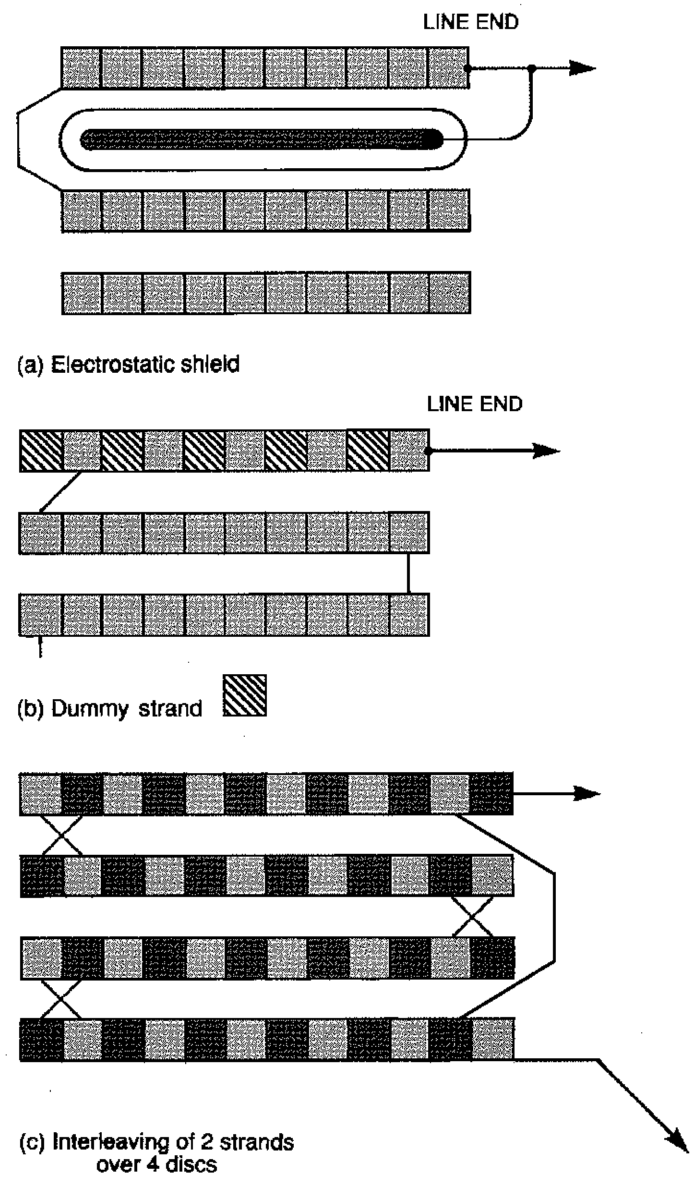


Figure 4.32 Types of winding stress control

cost of winding is greatly increased because of the large number of joints. It is possible by adjustment of the degree of interleaving, to achieve a nearly linear distribution of impulse voltage throughout the winding, although because of the high cost of interleaving, the designer aims to minimise this and, where possible, to restrict it to the end sections of the winding.

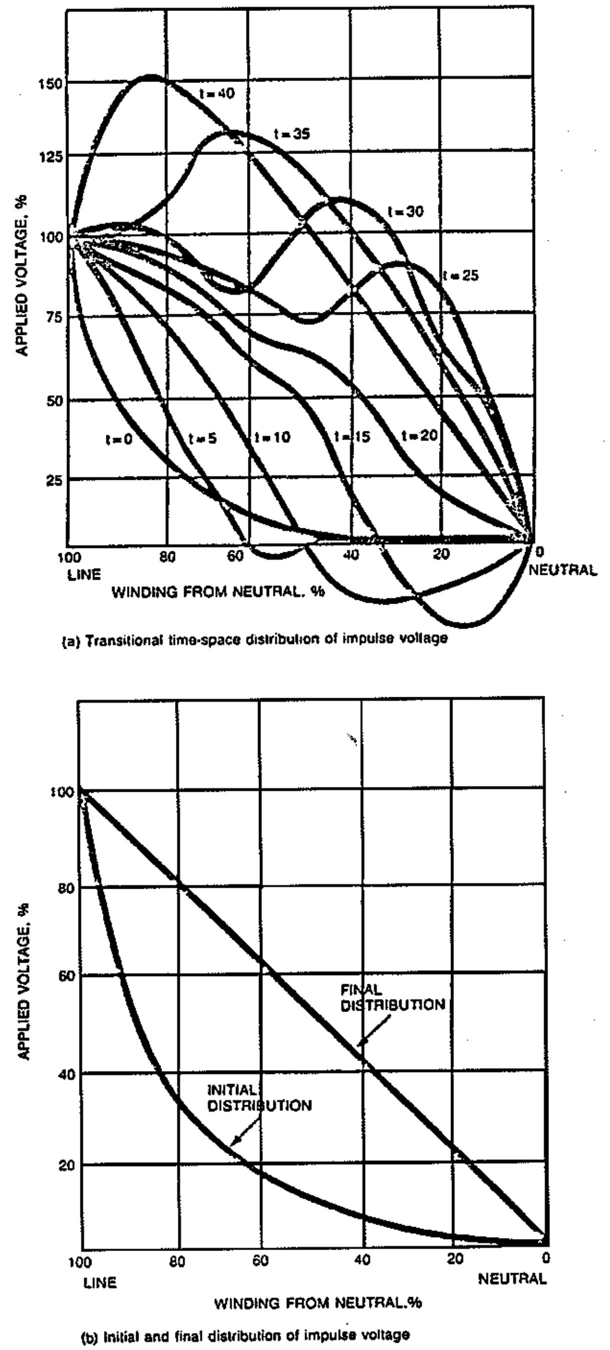


Figure 4.33 Voltage distribution through windings

After the line-end sections, the next most critical area will usually be at the neutral end of the winding, since the oscillations resulting from interactions between the incident wave and the reflection from the neutral will lead to the greatest voltage swings in this area (*Figure 4.33*). If some of the tapping winding is not in circuit, which happens whenever the transformer is on other than maximum tap, the tapping winding will then have an overhang which can experience a high voltage at its remote end. The magnitude of the impulse voltage appearing both across the neutral end sections and within the tapping winding overhang will be similar and will be at a minimum when the initial distribution is linear, as can be seen from *Figure 4.34*. It is often necessary, therefore, to use a section of interleaving at the neutral end to match that of the line-end sections. The magnitude of impulse voltage seen by the tapping winding due to overhang effects is likely to be dependent on the size of tapping range (although it will also be influenced by the type of tapping arrangement, for example, buck/boost or linear, and physical disposition of this with respect to the HV winding and earth), so this must be borne in mind when deciding the size of tapping range required.

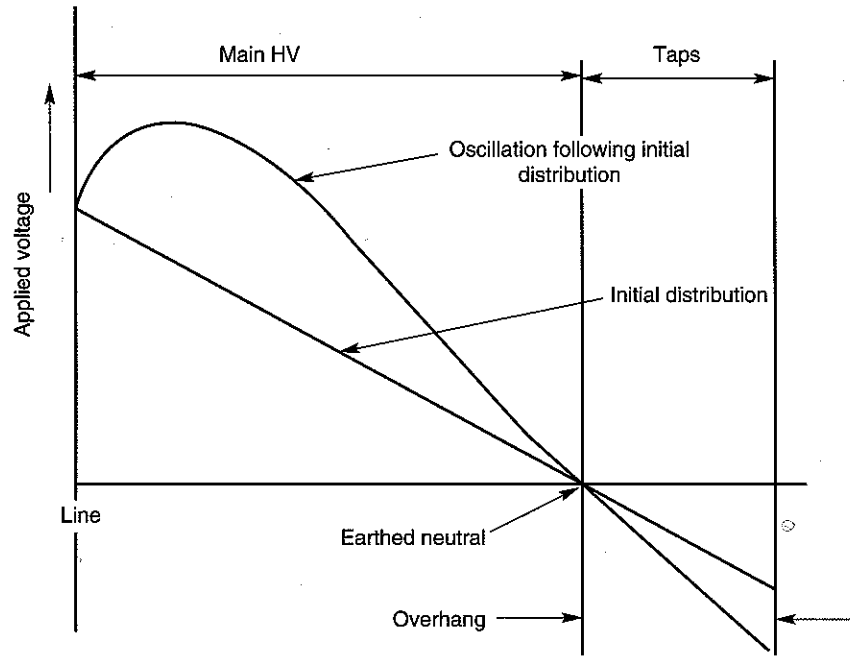


Figure 4.34 Impulse voltage distribution in tapping winding overhang – tapchanger selected on minimum tap

The need for an interleaved HV winding arrangement, as opposed to, say, a simpler line-end shield, is often determined by the rating of the transformer as well as the voltage class and impulse test level required. The lower the MVA rating of the unit the smaller the core frame size, which in turn leads to a lower volts per turn for the transformer and a greater total number of turns. A disc winding with a high total number of turns must have a large number of turns per disc, perhaps as many as 15 or 16, in order to accommodate these within the available winding length, compared with a more normal figure of, say, eight or nine. As a result, the maximum volts appearing between adjacent discs might be as high as 32 times the volts per turn compared with say, only 18 times in a more 'normal' winding. This large difference in power frequency voltage between adjacent sections, i.e. discs, can become even more marked for the impulse voltage distribution, thus necessitating the more elaborate stress-control arrangement.

For very high-voltage windings the impulse voltage stress can be too high to be satisfactorily controlled even when using an interleaved arrangement. It is in this situation that it may be necessary to use a shielded-layer winding as described earlier in the chapter. When the steep-fronted impulse wave impinges on the line end of this type of winding, the inner and outer shields behave as line and earth plates of a capacitor charged to the peak magnitude of the impulse voltage. The winding layers between these plates then act as a succession of intermediate capacitors leading to a nearly linear voltage distribution between the shields. (This is similar to the action of the intermediate foils in a condenser bushing which is described in Section 8 of this chapter.) With such a near linear distribution, the passage of the impulse wave through the winding is not oscillatory and the insulation structure required to meet the impulse voltage is the same as that required to withstand the power frequency stress. Electrically, therefore, the arrangement is ideal. The disadvantage, as explained earlier, is the winding's poor mechanical strength so that a disc winding is used whenever the designer is confident that the impulse stress can be satisfactorily controlled by static shield, dummy strand, or by interleaving.

Chopped waves

For many years it has been the practice to protect transformers of all voltages connected to overhead lines and therefore exposed to lightning overvoltages, by means of surge diverters or coordinating gaps. More will be said about the devices themselves in Section 6 of Chapter 6. Although such devices undoubtedly protect the windings by limiting the magnitude of the wavefronts and the energy transferred to them, operation of these does itself impose a very steep rate of change of voltage onto the line terminal which can result in severe inter-turn and inter-section stress within the windings. The most modern surge arresters are designed to attenuate steep-fronted waves in a 'softer' manner than the majority of those used hitherto, but the cost of protecting every transformer connected to an overhead line in this way would be prohibitive. By far the most practicable and universal form of protection used in the UK is the rod gap, or coordinating gap. *Figure 4.35* shows a simple arrangement as used on the 11 kV HV bushings of a 11/0.415 kV rural distribution transformer. A

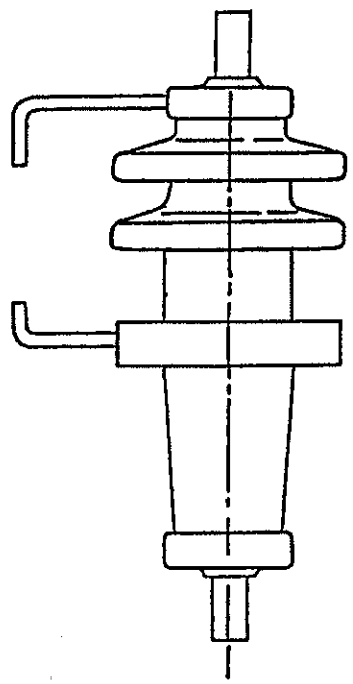


Figure 4.35 Arrangement of rod gap on 11 kV bushing

more elaborate device as used at 275 and 400 kV is shown in *Figure 6.81*. The coordinating gap is designed to trigger at a voltage just below that to which the winding may be safely exposed. If it is set too low it will operate too frequently. Set too high it will fail to provide the protection required. Because of the severe dV/dt imposed on the transformer windings by the triggering of a rod gap it has been the practice to test for this condition by means of chopped-wave tests when carrying out impulse tests in the works. *Figure 4.36* shows a typical chopped impulse wave as applied during these tests.

For many years the chopping was carried out by installing a rod gap across the impulse generator output. In order to ensure that this gap flashed over as close as possible to the nominal impulse test level, it was the practice in the UK electricity supply industry to specify that the impulse voltage for the chopped-wave test should be increased by a further 15% over the normal full-wave test level. Specification requires that the gap should flash over between 2 and 6 μs from the start of the wave and since the nominal time to peak is 1.2 μs, this means that the peak has normally passed before flashover and the winding has been exposed to 115% of the nominal test voltage. Designers were thus required to design the windings to withstand this 115% as a full-wave withstand. It is now possible to use triggered gaps whose instant of flashover can be very precisely set, so the need to specify that the test be carried out at 115% volts no longer arises and IEC 76, Part 3, which deals

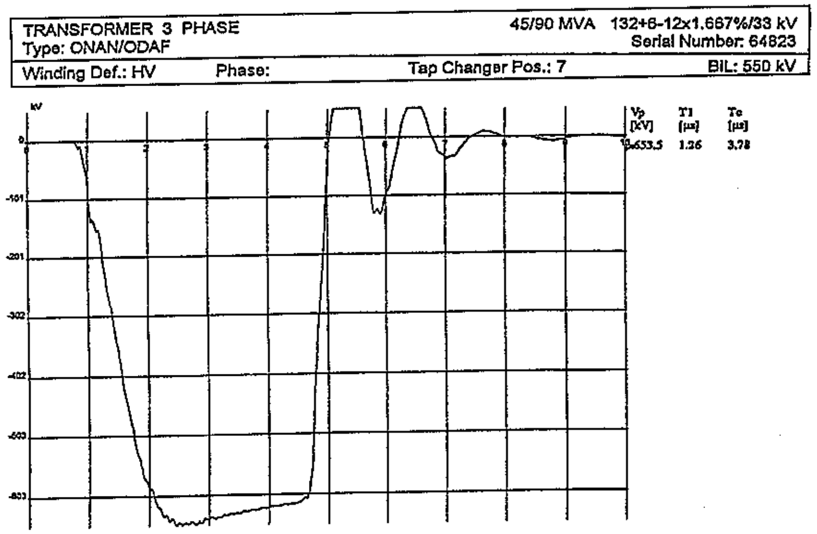


Figure 4.36 Chopped-wave impulse test record for 132/33 kV transformer

with dielectric testing of transformers, now specifies that the chopped-wave tests should be carried out at 100% volts. As far as withstanding the rapidly collapsing voltage wave is concerned, this will, of course, be better dispersed through the winding with a high series capacitance, so that the winding design will follow the same principles as for the full-wave withstand.

4.5 THERMAL CONSIDERATIONS

When the resistive and other losses are generated in transformer windings heat is produced. This heat must be transferred into and taken away by the transformer oil. The winding copper retains its mechanical strength up to several hundred degrees Celsius. Transformer oil does not significantly degrade below about 140°C, but paper insulation deteriorates with greatly increasing severity if its temperature rises above about 90°C. The cooling oil flow must, therefore, ensure that the insulation temperature is kept below this figure as far as possible.

The maximum temperature at which no degradation of paper insulation occurs is about 80°C. It is usually neither economic nor practical, however, to limit the insulation temperature to this level at all times. Insulation life would greatly exceed transformer design life and, since ambient temperatures and applied loads vary, a maximum temperature of 80°C would mean that on many occasions the insulation would be much cooler than this. Thus, apart from premature failure due to a fault, the critical factor in determining the life expectancy of a transformer is the working temperature of the insulation

or, more precisely, the temperature of the hottest part of the insulation or *hot spot*. The designer's problem is to decide the temperature that the hot spot should be allowed to reach. Various researchers have considered this problem and all of them tend to agree that the rate of deterioration or ageing of paper insulation rapidly increases with increasing temperature. In 1930, Montsinger [4.1] reported on some of the materials which were then in common use and concluded that the rate of ageing would be doubled for every 8°C increase between 90 and 110°C. Other investigators of the subject found that rates of doubling varied for increases between 5 and 10°C for the various materials used in transformer insulation, and a value of 6°C is now generally taken as a representative average for present-day insulation materials.

It is important to recognise that there is no 'correct' temperature for operation of insulation, nor is there a great deal of agreement between transformer designers as to the precise hot-spot temperature that should be accepted in normal operation. In fact it is now recognised that factors such as moisture content, acidity and oxygen content of the oil, all of which tend to be dependent upon the breathing system and its maintenance, have a very significant bearing on insulation life. Nevertheless BS 171 (IEC 76) and other international standards set down limits for permissible temperature rise which are dictated by considerations of service life and aim at a minimum figure of about 30 years for the transformer. These documents are based on the premise that this will be achieved with an *average* hot-spot temperature of 98°C.

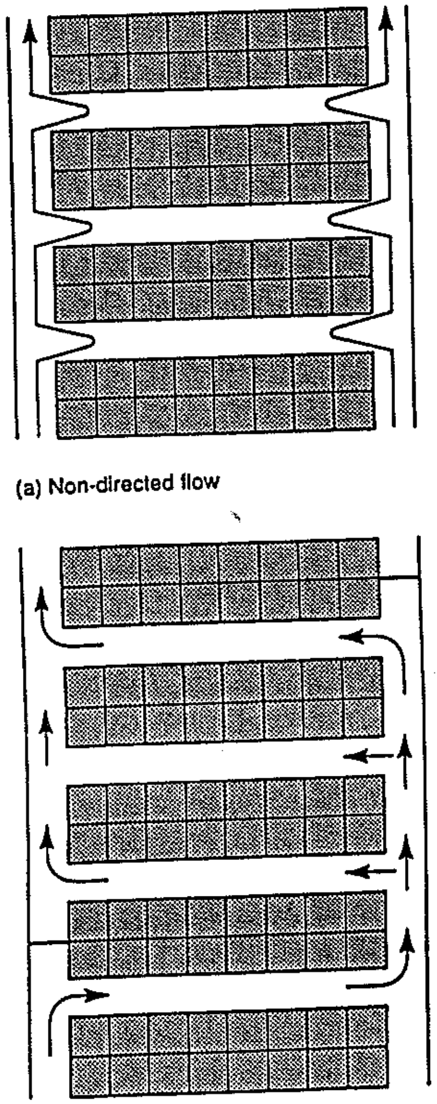
It must also be recognised that the *specified* temperature rise can only be that value which can be measured, and that there will usually be, within the transformer, a hot spot which is hotter than the temperature which can be measured and which will really determine the life of the transformer.

Study of the permitted temperature rises given in BS 171 and IEC 76 shows that a number of different values are permitted and that these are dependent on the method of oil circulation. The reason for this is that the likely difference between the value for temperature rise, which can be measured, and the hot spot, which cannot be measured, tends to vary according to the method of oil circulation. Those methods listed in BS 171 are:

- Natural.
- Forced, but not directed.
- Forced and directed.

Natural circulation utilises the thermal head produced by the heating of the oil which rises through the windings as it is heated and falls as it is cooled in passing through the radiators.

With *forced circulation*, oil is pumped from the radiators and delivered to the bottom of the windings to pass through the vertical axial ducts formed by the strips laid 'above' and 'below' the conductors. In referring to axial ducts within the windings, the expressions 'above' and 'below' mean 'further from the core' and 'nearer to the core' respectively. Radial ducts are those which connect these. In a non-directed design, flow through the radial, horizontal, ducts which connect the axial ducts above and below is dependent entirely on thermal and turbulence effects and the rate of flow through these is very much less than in the axial ducts (*Figure 4.37(a)*). With a forced and directed circulation, oil is fed to a manifold at the bottom of the windings and thence in appropriate proportions to the individual main windings. *Oil flow washers* are inserted at intervals in the winding which alternately close off the outer



(a) Non-directed flow

(b) Directed flow

Figure 4.37 Directed and non-directed oil flow

and then the inner axial ducts so that the oil in its passage through the winding must weave its way through the horizontal ducts thus ensuring a significant oil flow rate in all parts of the winding. This arrangement is illustrated in *Figure 4.37(b)*. The rate of heat transfer is very much a function of the rate of oil flow so that the directed oil flow arrangement will result in a lower winding to oil differential temperature or *gradient*. Typical values of gradient will be discussed shortly.

The designer generally aims to achieve a 'balanced' design, in which both top oil temperature rise and temperature rise by resistance for LV and HV windings approach reasonably close to the specified maxima by control of the winding gradient. If the gradient is 'too high' it will be necessary to limit the top oil temperature rise to ensure that the permitted temperature rise by resistance is not exceeded. Given that the oil flow arrangement used will itself be dictated by some other factors, the designer's main method of doing this will be by adjustment of the number of horizontal cooling ducts employed in the winding design.

The *average* temperature rise of the winding is measured by its change in resistance compared with that measured at a known ambient temperature. There are many reasons why the temperature rise in some parts of the winding will differ significantly from this average, however, and, while some of the differences can be accurately estimated, there are others which are less easily predicted. For example, some of the winding at the bottom of the leg is in cool oil and that at the top of the leg will be surrounded by the hottest oil. It is a relatively simple matter to measure these two values by placing a thermometer in the oil at the top of the tank near to the outlet to the coolers and another at the bottom of the tank. The average oil temperature will be halfway between these two values and the *average* gradient of the windings is the difference between average oil temperature rise and average winding temperature rise, that is, the temperature rise determined from the change of winding resistance. The temperature of the hottest part of the winding is thus the sum of the following:

- Ambient temperature.
- Top oil temperature rise.
- Average gradient (calculable as indicated above).
- A temperature equal to the difference between maximum and average gradient of the windings (hot-spot factor).

It will be seen that this is the same as the sum of:

- Ambient temperature.
- Temperature rise by resistance.
- Half the temperature difference between inlet oil from cooler and outlet oil to cooler.

- Difference between maximum and average gradient of the windings, as above.

This latter sum is, on occasions, a more convenient expression for hot-spot temperature. In both cases it is the last of these quantities which cannot be accurately determined. One of reasons why there will be a difference between maximum gradient and average gradient will be appreciated by reference to *Figure 4.38* which represents a group of conductors surrounded by vertical and horizontal cooling ducts. The four conductors at the corners are cooled directly on two faces, while the remainder are cooled on a single face only. Furthermore, unless the oil flow is forced and directed, not only will the heat transfer be poorer on the horizontal surfaces, due to the poorer oil flow rate, but this oil could well be hotter than the general mass of oil in the vertical ducts. In addition, due to the varying pattern of leakage flux, eddy-current losses can vary in different parts of the winding. Fortunately copper is as good a conductor of heat as it is of electricity so that these differences can be to a large extent evened out. However, in estimating the hot-spot temperature this difference between average and maximum winding gradient cannot be neglected. For many years this was taken to be approximately 10% of the average gradient, that is, the maximum gradient was considered to be 1.1 times the average. It is now suggested that this might have been somewhat optimistic and the 1991 issue of IEC 354, *Guide to Loading of Power Transformers*, concludes that a value of 1.1 is reasonable for small transformers but that a figure of up to 1.3 is more appropriate for medium and large transformers. More will be said on this aspect in Section 8 of Chapter 6.

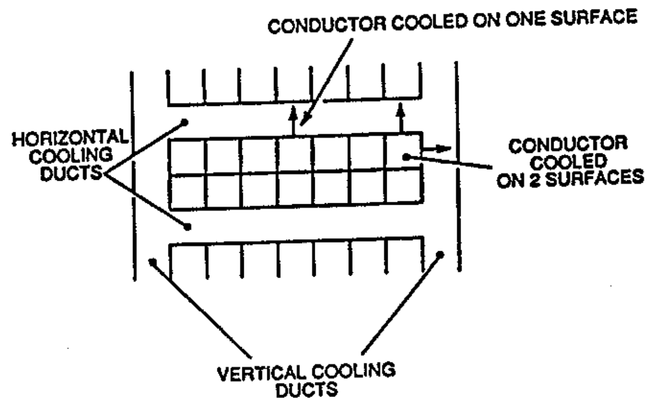


Figure 4.38 Winding hot spots

BS 171, Part 2 and IEC 76, Part 2 deal with temperature rise. In these documents the type of cooling for a particular transformer is identified by means of a code of up to four letters. These are as follows:

The first letter refers to the type of internal cooling medium in contact with the windings. This may be:

O mineral oil or synthetic insulating liquid with a fire point ≤300°C
K insulating liquid with fire point >300°C
L insulating liquid with no measurable fire point.

The second letter refers to the circulation mechanism for the internal cooling medium from the options described above:

N *natural* thermosiphon flow through cooling equipment and windings
F *forced* circulation through cooling equipment, thermosiphon through windings
D forced circulation through cooling equipment, *directed* from the cooling equipment into at least the main windings.

Frequently, tapping windings which might contain only 20% of the total ampere-turns and thus have far fewer losses to dissipate than the main windings, will be excluded from the directed flow arrangements and cooled only by natural circulation.

The third letter refers to the external cooling medium, thus:

A air
W water.

The fourth letter refers to the circulation mechanism for the external cooling medium:

N natural
F forced circulation (fans, pumps).

A transformer may be specified to have alternative cooling methods, for example ONAN/ODAF, which is a popular dual rating arrangement in the UK. The transformer has a totally self-cooled or ONAN rating, usually to cover base load conditions and a forced cooled ODAF rating achieved by means of pumps and fans to provide for the condition of peak load. A ratio of one to two between the ONAN and ODAF ratings is common.

For normal ambient conditions, which are defined in BS 171, Part 2, as air never below −25°C and never hotter than +40°C, not exceeding +30°C average during the hottest month and not exceeding +20°C yearly average, or water never exceeding 25°C at the inlet to oil/water coolers, permitted temperature rises are as follows:

Temperature rise of top oil	60 K
Average winding temperature rise by resistance	
• for transformers identified as ON.. or OF..	65 K
• for transformers identified as OD..	70 K

No tolerances are permitted on the above values.

In all except the smallest transformers cooling of the oil will be by some external means, tubes or radiators mounted on the side of the tank, external banks of separate radiators or even oil/water heat exchangers. If the oil is required to circulate through these coolers by natural thermosiphon, that is, ON.. type cooling is employed, then a fairly large thermal head will be required to provide the required circulation, possibly of the order of 25 K. If the oil is pumped through the coolers, that is, OF.. or OD.. type cooling is employed, then the difference between inlet and outlet oil temperatures might be, typically, 10–15 K. Thus temperatures within designs of each type of transformer, using the second of the two alternative derivations identified above, might typically be:

Type of cooling	ODAF	ONAN
(a) Ambient (BS 171)	30	30
(b) Temperature rise by resistance (BS 171)	70	65
(c) Half (outlet–inlet) oil	8	12
(d) Maximum gradient–average gradient, typical value	4	5
Hot spot temperature	112	112

The differences between maximum and average gradient are estimates simply for the purpose of illustration. The value has been taken to be less for the ODAF design on the basis that there are likely to be fewer inequalities in oil flow rates. The fact that the hot-spot temperature is the same in both cases is coincidence.

For each of the above arrangements the permitted top oil rise according to BS 171 is 60 °C, so the mean oil rises could be (60 − 8) = 52°C and (60 − 12) = 48°C respectively for the ODAF and ONAN designs. Since temperature rise by resistance is mean oil temperature rise plus gradient, it would thus be acceptable for the winding gradient for the ODAF design to be up to 18°C and for the ONAN design this could be up to 17°C. This is, of course, assuming 'balanced' designs as defined above. It should be remembered that, if one of the windings is tapped, the transformer is required to deliver full rating on the maximum minus tapping and that the BS 171 temperature rise limits must be met on this tapping.

It must be stressed that in the examples given above, items (c) and (d) cannot be covered by specification, they are typical values only and actual values will differ between manufacturers and so, therefore, will the value of hot-spot temperature. It will be noted also that the hot-spot temperatures derived significantly exceed the figure of 98°C quoted above as being the temperature which corresponds to normal ageing. It will also be seen that the figure used for ambient temperature is not the maximum permitted by BS 171, which allows for this to reach 40°C, giving a hot-spot temperature of 122°C in this case. Such temperatures are permissible because the maximum ambient temperature occurs only occasionally and for a short time.

When a transformer is operated at a hot-spot temperature above that which produces normal ageing due to increase in either ambient temperature or loading, then insulation life is used up at an increased rate. This must then be offset by a period with a hot-spot temperature below that for normal ageing, so that the total use of life over this period equates to the norm. This is best illustrated by an example; if two hours are spent at a temperature which produces twice the normal rate of ageing then four hours of life are used in this period. For the balance of those four hours (i.e. 4 − 2 = 2) the hot-spot must be such as to use up no life, i.e. below 80°C, so that in total four hours life are used up. This principle forms the basis of IEC 354. The subject will be discussed at greater length in Section 8 of Chapter 6. The system works well in practice since very few transformers are operated continuously at rated load. Most transformers associated with the public electricity supply network are subjected to cyclic daily loading patterns having peaks in the morning and afternoon. Many industrial units have periods of light loading during the night and at weekends, and ambient temperatures are subject to wide seasonal variations. In addition, in many temperate countries such as the UK a significant portion of the system load is heating load which is greater in the winter months when ambient temperatures are lower, thus reducing the tendency for actual hot-spot temperatures to reach the highest theoretical levels.

Core, leads and internal structural steelwork

Although the cooling of the transformer windings represents the most important thermal aspect of the transformer design, it must not be overlooked that considerable quantities of heat are generated in other parts. The core is the most significant of these. There is no specified maximum for the temperature rise of the core in any of the international standards. One of the reasons for this is, of course, the practical aspect of enforcement. The hottest part of the core is not likely to be in a particularly accessible location. In a three-phase three-limb core, for example, it is probably somewhere in the middle of the leg to yoke joint of the centre limb. Its temperature could only be measured by means of thermocouple or resistance thermometer, even this exercise would be difficult and the accuracy of the result would be greatly dependent on the manufacturer placing the measuring device in exactly the right location. BS 171 resolves this difficulty by stating that the temperature rise of the core or of electrical connections or structural parts shall not reach temperatures which will cause damage to adjacent parts or undue ageing of the oil. This approach is logical since, in the case of all of these items, temperatures are unlikely to reach such a value as to damage core steel or structural metalwork or even the copper of leads. It is principally the material in contact with them, insulation, or oil, which is most at risk of damage. Hence 'damage to adjacent parts' usually means overheating of insulation and this can be detected during a temperature rise test if oil samples are taken for dissolved gas analysis. More will be said about this in Chapter 5, which deals with testing.

Cooling of the core will usually be by natural circulation even in transformers having forced cooling of the windings. The heat to be removed will depend on grade of iron and flux density but direct heat transfer from the core surface to the surrounding oil is usually all that is necessary up to leg widths (frame sizes) of about 600 mm. Since the ratio of surface area to volume is inversely proportional to the diameter of the core, at frame sizes above this the need to provide cooling becomes an increasingly important consideration.

Because the concern is primarily that of overheating of insulation, some users do specify that the maximum temperature rise for the surface of the core should not exceed the maximum temperature permitted for windings. Some users might also agree to a localised hot-spot of 130°C on the surface of very large cores in an area well removed from insulation, on the basis that oil will not be significantly degraded on coming into contact with this temperature provided the area of contact is not too extensive and recognising that cooling of these large cores is particularly problematical. Enforcement of such restrictions, of course, remains difficult.

Cooling of the oil

In discussion of the typical internal temperatures identified above, little has been said about the cooling of the oil, which having taken the heat from the windings and other internal parts, must be provided with means of dissipating this to the atmosphere. In a small transformer, say up to a few kVA, this can be accomplished at the tank surface. As a transformer gets larger, the tank surface will increase as the square of the linear dimension whereas the volume, which is related to rating and thus its capacity for generating losses, will increase in proportion to the cube of this, so the point is soon reached at which the available tank surface is inadequate and other provision must be made to increase the dissipation, either tubes or fins attached to the tank, or radiators consisting of a series of pressed steel 'passes'. While the transformer remains small enough for fins or tubes to be used, heat loss is by both radiation and convection. The radiation loss is dependent on the size of the envelope enclosing the transformer, convection loss is related to the total surface area. The effectiveness of a surface in radiating energy is also dependent on its emissivity, which is a function of its finish. Highly polished light-coloured surfaces being less effective than dull black surfaces. In practical terms, however, investigators soon established that most painted surfaces have emissivities near to unity regardless of the colour of the paint.

It is possible to apply the laws of thermodynamics and heat transfer to the tank and radiators so as to relate the temperature rise to the radiating and convecting surfaces and, indeed, in the 1920s and 1930s when much of the basic ground work on transformer cooling was carried out, this was done by a combination of experiment and theory. Nowadays manufacturers have refined their databases empirically so as to closely relate the cooling surface required to the watts to be dissipated for a given mean oil rise. For the larger sizes of transformer, say, above a few MVA, the amount of convection surface required becomes so large that the radiating surface is negligible by proportion and can thus be neglected. Then it is simply a matter of dividing the total heat to be dissipated by the total cooling surface to give a value of watts per square centimetre, which can then be tabulated against mean oil rise for a given ambient. As an approximate indication of the order of total convection surface required when heat is lost mainly by convection, for a mean oil rise of 50 K in an ambient of 20°C, about 0.03 watts/cm^2 can be dissipated.

An example can be used to translate this figure into practical terms. Consider a 10 MVA ONAN transformer having total losses on minimum tapping of 70 kW. Let us assume it has a tank 3.5 m long × 3.5 m high × 1.5 m wide.

Total cooling surface required at 0.03 watts/cm^2 $= \dfrac{70\,000}{0.03}$

$= 233\ \text{m}^2$

Tank surface (sides plus cover) $= 2(3.5 \times 3.5) + 2(1.5 \times 3.5) + 1.5 \times 3.5$

$= 40.25\ \text{m}^2$

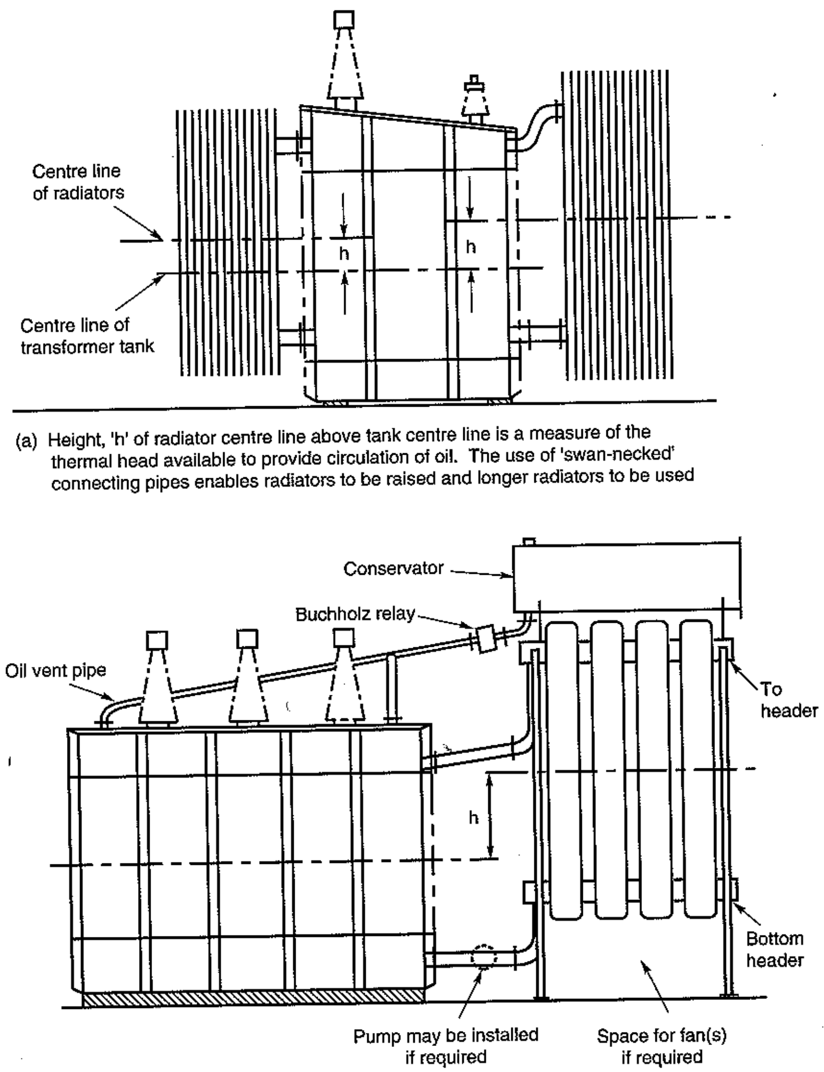
Hence, net surface of radiators $= 233 - 40.25$

$= 193\ \text{m}^2$

Suppose pressed steel radiators are used 3 m long × 0.25 m wide, these will have a convection surface of approximately 1.5 m^2 per pass, hence $193/1.5 = 129$ passes will be required, or, say, 10 radiators of 13 passes per radiator.

It will be noted that in the above example, the tank is contributing about one-sixth of the total convection surface required. If the transformer were a 30/60 MVA ONAN/ODAF, having total losses at its 30 MVA ONAN rating of 100 kW, then for the same mean oil temperature rise the total convection surface required is about 333 m^2. The tank may have only increased to 4 m long × 3.6 m high × 1.7 m wide, so that it will contribute only 47.8 m^2 or about one-seventh of the area required and, clearly, as unit size increases the contribution from the tank is steadily reduced. At the ODAF rating when fans are brought into service, these will blow the radiator surface much more effectively than they will the tank, even if the radiator banks are tank mounted. Hence, it becomes less worthwhile including the tank surface in the cooling calculations. Additionally, there may be other reasons for discounting the tank, for example it may be necessary to provide an acoustic enclosure to reduce external noise. There can then be advantages in mounting the radiators in a separate bank. Some of these can be seen by reference to *Figure 4.39*.

An important parameter in an ONAN cooling arrangement is the mounting height of the radiators. The greater the height of the horizontal centre line of the radiators in relation to that of the tank, the greater will be the thermosiphon effect creating the circulation of the oil, and the better this circulation, the less will be the difference between inlet and outlet oil temperature. The net effect is to reduce the hot spot temperature rise for the same heat output and effective



(a) Height, 'h' of radiator centre line above tank centre line is a measure of the thermal head available to provide circulation of oil. The use of 'swan-necked' connecting pipes enables radiators to be raised and longer radiators to be used

(b) Provision of separate bank of radiators allows 'h' to be increased considerably

Figure 4.39 Arrangements of cooling radiators

cooling surface area. To fully appreciate this it is necessary to refer back to the derivation of the hot-spot temperature given above. This is related to the *top oil* temperature plus maximum gradient. The area of cooling surface determines the *mean oil* temperature, which is less than top oil by half the difference between inlet and outlet oil. Thus, the smaller this difference, the less will be the amount added to the mean oil temperature to arrive at top oil temperature and the lower will be the hot-spot temperature.

When the radiators are attached to the tank, there is a limit to the mounting height of these, although some degree of swan-neck connection is possible as shown in *Figure 4.39(a)*. If the radiators are separately mounted the height limitation is dictated solely by any restrictions which might be imposed by the location. In addition the tank height ceases to impose a limitation to the length of radiator which can be used and by the use of longer radiators fewer of them may be necessary.

4.6 TAPPINGS AND TAPCHANGERS

Almost all transformers incorporate some means of adjusting their voltage ratio by means of the addition or removal of tapping turns. This adjustment may be made on-load, as is the case for many large transformers, by means of an off-circuit switch, or by the selection of bolted link positions with the transformer totally isolated. The degree of sophistication of the system of tap selection depends on the frequency with which it is required to change taps and the size and importance of the transformer.

At the start, two definitions from the many which are set out in BS 171, Part 1: *principal tapping* is the tapping to which the rated quantities are related and, in particular, the *rated voltage ratio*. This used to be known as normal tapping and the term is still occasionally used. It should be avoided since it can easily lead to confusion. It should also be noted that in most transformers and throughout this book, except where expressly indicated otherwise, tappings are *full-power tappings*, that is, the power capability of the tapping is equal to rated power so that on plus tappings the rated current for the tapped winding must be reduced and on minus tappings the rated current for the winding is increased. This usually means that at minus tappings, because losses are proportional to current squared, losses are increased, although this need not always be the case.

Uses of tapchangers

Before considering the effects of tappings and tapchangers on transformer construction it is first necessary to examine the purposes of tapchangers and the way in which they are used. A more complete discussion of this subject will be found in a work dealing with the design and operation of electrical systems. Aspects of tapchanger use relating to particular types of transformers will be discussed further in Chapter 7, but the basic principles apply to all transformer types and are described below.

Transformer users require tappings for a number of reasons:

- To compensate for changes in the applied voltage on bulk supply and other system transformers.
- To compensate for regulation within the transformer and maintain the output voltage constant on the above types.

- On generator and interbus transformers to assist in the control of system VAr flows.
- To allow for compensation for factors not accurately known at the time of planning an electrical system.
- To allow for future changes in system conditions.

All the above represent sound reasons for the provision of tappings and, indeed, the use of tappings is so commonplace that most users are unlikely to consider whether or not they could dispense with them, or perhaps limit the extent of the tapping range specified. However, transformers without taps are simpler, cheaper and more reliable. The presence of tappings increases the cost and complexity of the transformer and also reduces the reliability. Whenever possible, therefore, the use of tappings should be avoided and, where this is not possible, the extent of the tapping range and the number of taps should be restricted to the minimum. The following represent some of the disadvantages of the use of tappings on transformers:

- Their use almost invariably leads to some variation of flux density in operation so that the design flux density must be lower than the optimum, to allow for the condition when it might be increased.
- The transformer impedance will vary with tap position so that system design must allow for this.
- Losses will vary with tap position, hence the cooler provided must be large enough to cater for maximum possible loss.
- There will inevitably be some conditions when parts of windings are not in use, leading to less than ideal electromagnetic balance within the transformer which in turn results in increased unbalanced forces in the event of close-up faults.
- The increased number of leads within the transformer increases complexity and possibility of internal faults.
- The tapchanger itself, particularly if of the on-load type, represents a significant source of unreliability.

One of the main requirements of any electrical system is that it should provide a voltage to the user which remains within closely defined limits regardless of the loading on the system, despite the regulation occurring within the many supply transformers and cables, which will vary greatly from conditions of light load to full load. Although in many industrial systems, in particular, the supply voltage must be high enough to ensure satisfactory starting of large motor drives, it must not be so high when the system is unloaded as to give rise to damaging overvoltages on, for example, sensitive electronic equipment. Some industrial processes will not operate correctly if the supply voltage is not high enough and some of these may even be protected by undervoltage relays which will shut down the process should the voltage become too low. Most

domestic consumers are equally desirous of receiving a supply voltage at all times of day and night which is high enough to ensure satisfactory operation of television sets, personal computers washing machines and the like, but not so high as to shorten the life of filament lighting, which is often the first equipment to fail if the supply voltage is excessive.

In this situation, therefore, and despite the reservations concerning the use of tapchangers expressed above, many of the transformers within the public supply network must be provided with on-load tapchangers without which the economic design of the network would be near to impossible. In industry, transformers having on-load tapchangers are used in the provision of supplies to arc furnaces, electrolytic plants, chemical manufacturing processes and the like.

Figure 4.40 shows, typically, the transformations which might appear on a section of public electricity supply network from the generating station to the user. The voltage levels and stages in the distribution are those used in the UK but, although voltage levels may differ to some degree, the arrangement is similar to that used in many countries throughout the world.

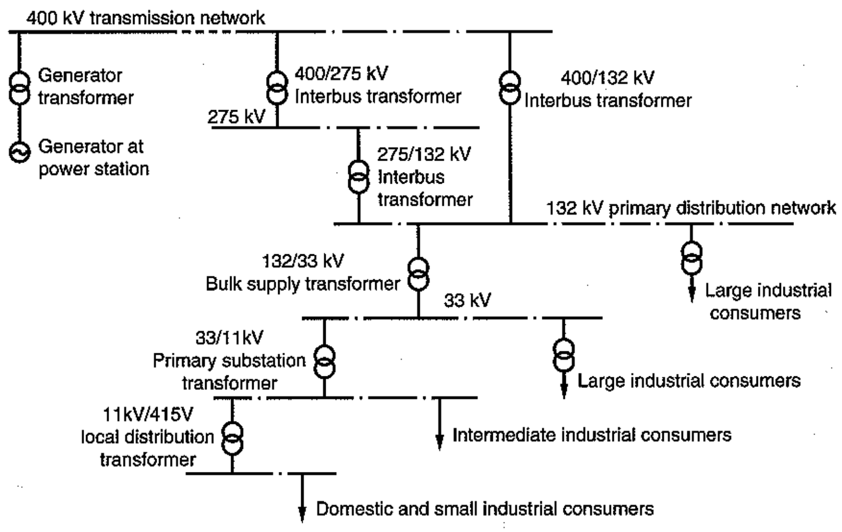


Figure 4.40 Typical public electricity supply network

The generator transformer is used to connect the generator whose voltage is probably maintained within ±5% of nominal, to a 400 kV system which normally may vary independently by ±5% and up to +10% for up to 15 minutes. This cannot be achieved without the ability to change taps on load. However, in addition to the requirement of the generator to produce megawatts, there may also be a requirement to generate or absorb VArs, according to the system conditions, which will vary due to several factors, for example time of day, system conditions and required power transfer. Generation of VArs will be effected by tapping-up on the generator transformer, that is, increasing the number of HV turns for a given 400 kV system voltage. Absorption of VArs will occur if the transformer is tapped down. This mode of operation leads to variation in flux density which must be taken into account when designing the transformer. The subject is fairly complex and will be described in more detail in Section 1 of Chapter 7 which deals specifically with generator transformers.

Interbus transformers interconnecting 400, 275 and 132 kV systems are most likely to be autoconnected. Variation of the ratio of transformation cannot therefore be easily arranged since adding or removing tapping turns at the neutral end changes the number of turns in both windings. If, for example, in the case of a 400/132 kV autotransformer it were required to maintain volts per turn and consequently 132 kV output voltage constant for a 10% increase in 400 kV system voltage then the additional turns required to be added to the common winding would be 10% of the total. But this would be equivalent to $10 \times 400/132 = 30.3\%$ additional turns in the 132 kV winding which would increase its output from 132 to 172 kV. In fact, to maintain a constant 132 kV output from this winding would require the *removal* of about 17.2% of the total turns. Since 10% additional volts applied to 17.2% fewer turns would result in about 33% increase in flux density this would require a very low flux density at the normal condition to avoid approaching saturation under overvoltage conditions, which would result in a very uneconomical design. Tappings must therefore be provided either at the 400 kV line end or at the 132 kV common point as shown in *Figure 4.41*. The former alternative requires the tapchanger to be insulated for 400 kV working but maintains flux density constant for 400 kV system voltage variation, the latter allows the tapchanger to operate at a more modest 132 kV, but still results in some flux density variation. Most practical schemes therefore utilise the latter arrangement. Alternatively these transformers may be used without tapchangers thereby avoiding the high cost of the tapchanger itself as well as all the other disadvantages associated with tapchangers identified above. The 'cost' of this simplification of the transformer is some slightly reduced flexibility in the operation of the 275 and 132 kV systems but this can be compensated for by the tappings on the 275/33 or 132/33 kV transformers, as explained below.

In the UK the 400 kV system is normally maintained within ±5% of its nominal value. If the transformers interconnecting with the 275 and 132 kV systems are not provided with taps then the variation of these systems will be greater than this because of the regulation within the interbus transformers. The 275 and 132 kV systems are thus normally maintained to within ±10% of nominal. Hence 275/33 kV and the more usual 132/33 kV bulk supplies transformers must have tapchangers which allow for this condition. If, in addition, these transformers are required to boost the 33 kV system volts at times of heavy loading on the system as described in Chapter 2, i.e. when the 275 or 132 kV system voltage is less than nominal, it is necessary to provide a tapping range extending to lower than −10%, so it is common for these transformers to have tapping ranges of +10% to −20%. This runs counter

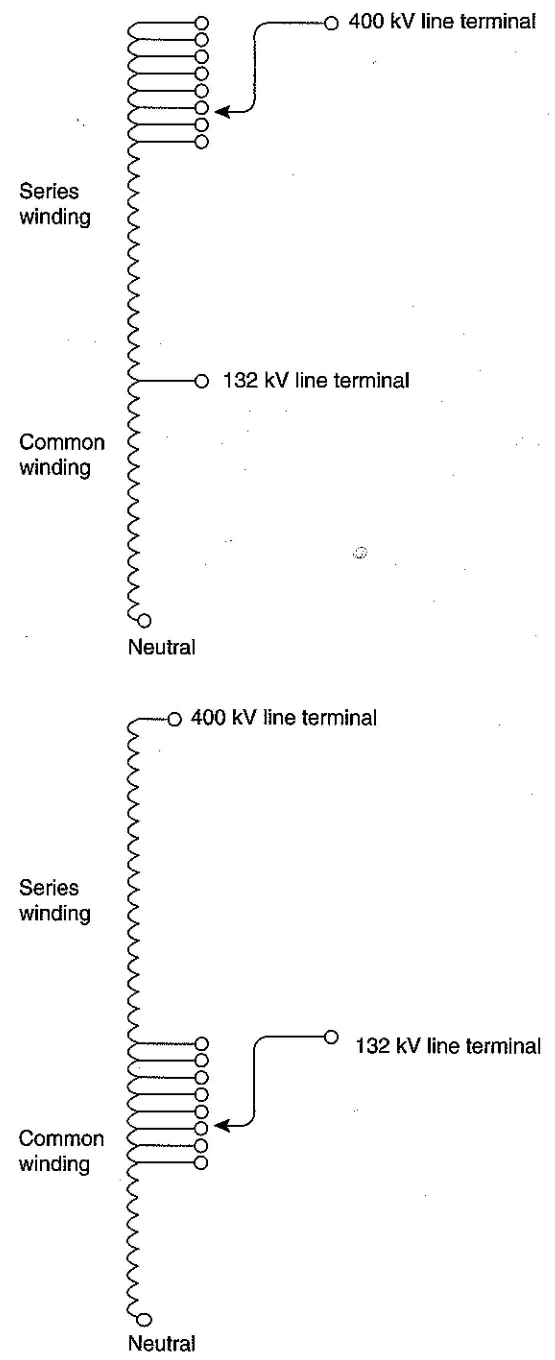


Figure 4.41 Alternative locations for tappings of 400/132 kV autotransformer

to the aim of limiting the extent of the tapping range for high reliability in transformers, identified earlier, but represents another of the complexities resulting from the reduced system flexibility caused by omitting tappings on the 400/132 kV transformers. Clearly tappings at the earthed neutral point of a star-connected 275 or 132 kV winding are likely to be more reliable and less costly than those operating at the 275 or 132 kV line end of a 400/275 or 132 kV interbus transformer.

The greater degree of control which can be maintained over the 33 kV system voltage compared with that for the 132 kV system means that 33/11 kV transformers normally need to be provided with tapping ranges of only ±10%. As in the case of 132/33 kV transformers, however, the HV taps can still be used as a means of boosting the LV output voltage to compensate for system voltage regulation. In this case this is usually achieved by the use of an open-circuit voltage ratio of 33/11.5 kV, i.e. at no load and with nominal voltage applied to the HV the output voltage is higher than nominal LV system volts.

The final transformers in the network, providing the 11/0.433 kV transformation, normally have a rating of 1600 kVA or less. These small low-cost units do not warrant the expense and complexity of on-load tapchangers and are thus normally provided with off-circuit taps, usually at ±2.5% and ±5%. This arrangement enables the voltage ratio to be adjusted to suit the local system conditions, usually when the transformer is initially placed into service, although the facility enables adjustments to be made at a later date should changes to the local system loading, for example, necessitate this.

Impedance variation

Variation of impedance with tap position is brought about by changes in flux linkages and leakage flux patterns as tapping turns are either added or removed

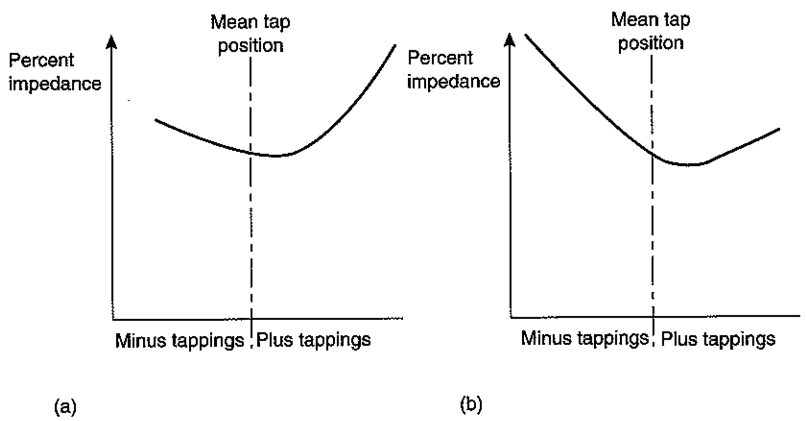


Figure 4.42 Typical variation of impedance with tap position for a two-winding transformer having taps in the body of one of the windings

from the tapped winding. Auxiliary system designers would, of course, prefer to be able to change the voltage ratio without affecting impedance but the best the transformer designer can do is to aim to minimise the variation or possibly achieve an impedance characteristic which is acceptable to the system designer rather than one which might aggravate his problems. It should be noted, however, that any special measures which the transformer designer is required to take are likely to increase first cost and must therefore be totally justified by system needs.

The magnitude and sense of the change depends on the winding configuration employed and the location of the taps. *Figure 4.42* shows typically the pattern of variation which may be obtained, although all of these options may not be available to the designer in every case. *Figures 4.42(a)* and *(b)* represent the type of variation to be expected when the taps are placed in the body of one of the windings.

Figure 4.43 represents a series of sections through the windings of a two-winding transformer having the tappings in the body of the HV winding. In all three cases the HV winding is slightly shorter than the LV winding in order to allow for the extra end insulation of the former. In *Figure 4.43(a)* all tappings are in circuit, *Figure 4.43(b)* shows the effective disposition of the windings on the principal tapping and *Figure 4.43(c)* when all the tappings are out of circuit. It can be seen that, although all the arrangements are symmetrical about the winding centre line and therefore have overall axial balance, the top and bottom halves are only balanced in the condition represented by *Figure 4.43(b)*. This condition will therefore have the minimum leakage flux and hence the minimum impedance. Addition or removal of tappings increases the unbalance and thus increases the impedance. It can also be seen that the degree of unbalance is greatest in *Figure 4.43(c)*, so that this is the condition corresponding to maximum impedance. This enables an explanation to be given for the form of impedance variation shown in *Figure 4.42*. *Figure 4.42(a)* corresponds to the winding configuration of *Figure 4.43*. It can be seen that the tap position for which the unbalance is minimum can be varied by the insertion of gaps in the untapped winding so that the plot can be reversed (*Figure 4.42(b)*) and, by careful manipulation of the gaps at the centre of the untapped winding and the ends of the tapped winding, a more or less symmetrical curve about the mean tap position can be obtained. This is usually the curve which gives minimum overall variation.

From this it will be apparent also that the variation will be reduced if the space which the taps occupy can be reduced to a minimum. While this can be achieved by increasing the current density in the tapping turns, the extent to which this can be done is limited by the need to ensure that the temperature rise in this section does not greatly exceed that of the body of the winding, since this would then create a hot-spot. If it is necessary to insert extra radial cooling ducts in order to limit the temperature rise, then the space taken up by these offsets some of the space savings gained from the increased current density. The designer's control of temperature rise in the taps tends to be

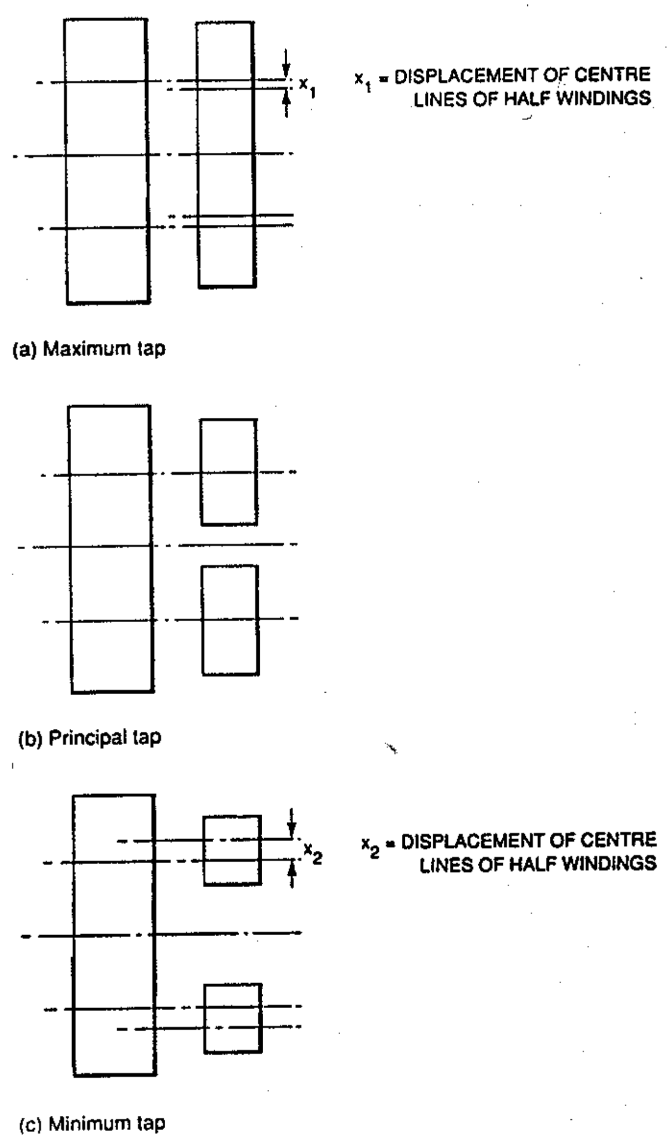


Figure 4.43 Effects of tappings within windings

less than that which can be achieved in the body of the winding, where the designer can vary the number of sections by adjusting the number of turns per section, with a radial cooling duct every one or two sections. In the taps, the turns per section are dictated by the need to ensure that the tapping leads appear at the appropriate position on the outside of a section, hence one tap must span an even number of sections, with a minimum of two.

With the tappings contained in a separate layer the degree of impedance variation throughout the tapping range tends to be less than for taps in the body of the HV winding but the slope of the characteristic can be reversed depending on where the taps are located. This is illustrated by reference to *Figure 4.44* which shows alternative arrangements having HV taps located either outside the main high-voltage winding or inside the low-voltage winding. Ampere-turn distributions for each extreme tap position are shown for both arrangements and also the resulting impedance variation characteristics. The arrangement having the taps located outside the HV winding is most commonly used in the UK and usually the transformer will have a star-connected HV winding

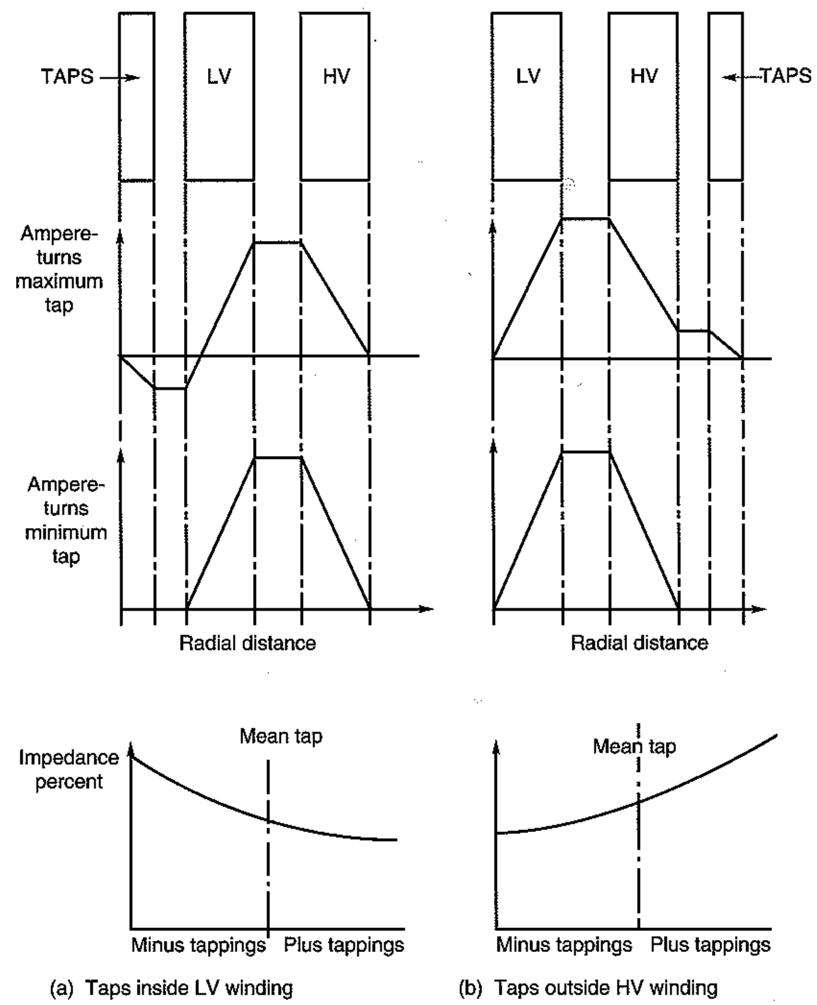


Figure 4.44 Impedance variation with tap position with taps in a separate layer. In both cases HV winding is tapped winding

employing non-uniform insulation. With this arrangement, described earlier in this chapter, the taps will probably have two sections in parallel and a centre gap to accommodate the HV line lead. The impedance characteristic shown in *Figure 4.44(b)* will in this case be modified by the additional distortion of the leakage flux created by the centre gap. This will probably result in an additional component of impedance and a resulting characteristic as shown in *Figure 4.45*.

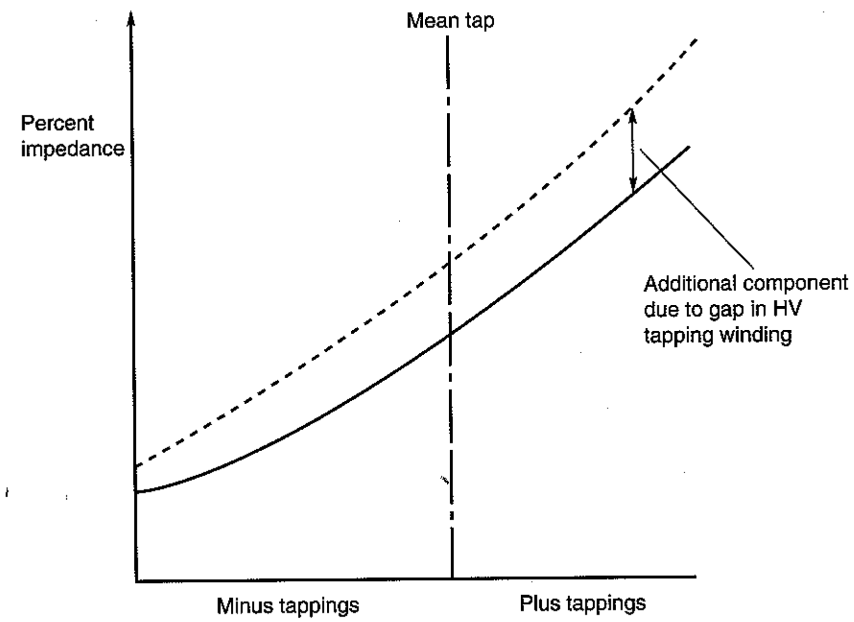


Figure 4.45 Effect of gap in HV tapping winding on percentage impedance

In the arrangements described above all the tappings are configured in a linear fashion, that is, for each increasing tap position an equal number of tapping turns are added. However, if these are contained in a separate layer, it is possible to configure these in a buck/boost arrangement as indicated in *Figure 4.46*. With this arrangement the taps are first inserted with a subtractive polarity, that is, minimum tap position is achieved by inserting all taps in such a sense as to oppose the voltage developed in the main HV winding, these are removed progressively with increasing tap position until on mean tap all tapping turns are out and they are then added in the reverse sense until on maximum tap all are inserted. The advantage of this arrangement is that it reduces the physical size of the tapping winding and also the voltage across the tapping range. The reduction in size is beneficial whether this is placed inside the LV winding or outside the HV winding. In the former case a smaller tap winding enables the diameters of both LV and HV main windings to be

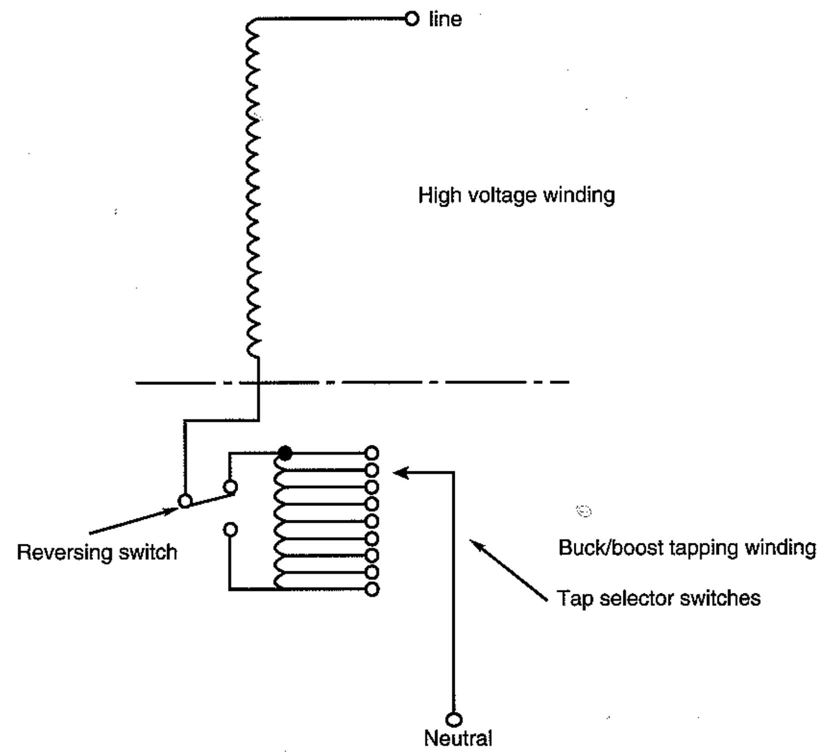
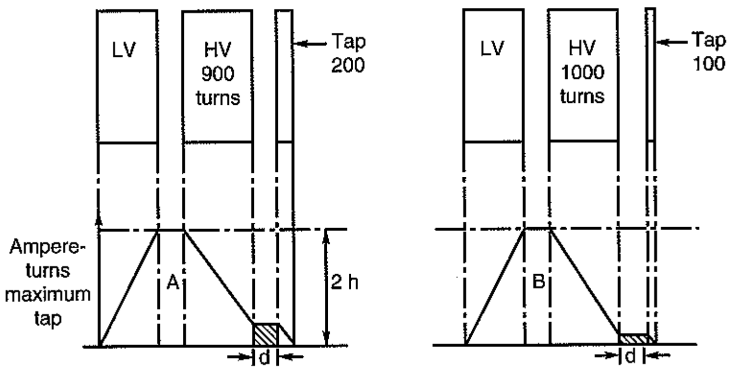


Figure 4.46 Connection of HV tapping winding in buck/boost arrangement

reduced. In both cases it produces a small reduction in impedance, which is often useful in the case of large high-voltage transformers, as well as reducing the number of tapping leads. The reason for the impedance reduction will be apparent from a simple example: a transformer requires 1000 turns on principal tap with a tapping range of $\pm 10\%$. With a linear arrangement this would have 900 turns in the body of the HV winding and 200 in the tapping winding. This is represented by *Figure 4.47(a)*. If a buck/boost arrangement were used the HV winding would have 1000 turns in the main body and 100 turns in the tapping winding as shown in *Figure 4.47(b)*. Both arrangements utilise the same total number of turns but it is clear that the area of the ampere-turns diagram is less in the case of the buck/boost arrangement. The price to be paid for these benefits is a slightly more complicated and therefore more expensive tapchanger.

Tapchanger mechanisms

The principal of on-load tapchanging was developed in the late 1920s and requires a mechanism which will meet the following two conditions:



(a) Linear tapping arrangement (b) Buck/boost tapping arrangement

Area under ampere-turns curves differs by difference in shaded areas

for A, shaded area is:

$$d \times 2h.\frac{200}{1100} = 0.36\ \text{dh}$$

for B, shaded area is:

$$d \times 2h.\frac{100}{1100} = 0.18\ \text{dh}$$

Readers may wish to sketch the equivalent diagrams for the minimum tap condition. In this case the tapping winding makes no contribution to the total ampere-turns with the linear arrangement but adds negative ampere-turns with the buck/boost arrangement.

Figure 4.47 Effect of type of tapping winding on impedance

- The load current must not be interrupted during a tapchange.
- No section of the transformer winding may be short-circuited during a tapchange.

Early on-load tapchangers made use of reactors to achieve these ends but in modern on-load tapchangers these have been replaced by transition resistors which have many advantages. In fact, the first resistor-transition tapchanger made its appearance in 1929, but the system was not generally adopted in the UK until the 1950s. In the USA, the change to resistors only started to take place in the 1980s. Despite the fact that it was recognised that resistor transition had advantages of longer contact life, due to the relatively short arcing times associated with unity power factor switching, the centre-tapped reactor-type tapchanger was, in general, more popular because reactors could be designed to be continuously rated, whereas transition resistors had a finite time rating due to the high power dissipated when in circuit. This would have been of little consequence if positive mechanical tapchanger operations could have been assured but, although various attempts at achieving this were generally successful, there were risks of damage if a tapchanger failed to complete its cycle of operation.

With the earlier designs thermal protection arrangements were usually introduced, to initiate the tripping and isolation of the transformer. These early

types of tapchangers operated at relatively low speeds and contact separation was slow enough for arcing to persist for several half cycles. Arc extinction finally took place at a current zero when the contact gap was wide enough to prevent a restrike. The arcing contacts were usually manufactured from plain copper.

The mechanical drive to these earlier tapchangers, both resistor or reactor types, was either direct drive or the stored energy type, the stored energy being contained in a flywheel or springs. But such drives were often associated with complicated gearing and shafting and the risk of failure had to be taken into account.

Most of these older designs have now been superseded by the introduction of the high-speed resistor-type tapchanger. Reliability of operation has been greatly improved, largely by the practice of building the stored energy drive into close association with the actual switching mechanism thus eliminating many of the weaknesses of earlier designs. The introduction of copper tungsten alloy arcing tips has brought about a substantial improvement in contact life and a complete change in switching philosophy. It is recognised that long contact life is associated with short arcing time, and breaking at the first current zero is now the general rule.

The bridging resistors are short time rated but with the improved mechanical methods of switch operation and the use of high-performance resistance materials, such as nickel chrome alloy, there is only a negligible risk of resistor damage as the resistors are only in circuit for a few milliseconds. The switching time of a flag cycle, double-resistor tapchanger (see below) is usually less than 75 ms.

A further advantage with high-speed resistor transition is that of greatly improved oil life. The oil surrounding the making and breaking contacts of the on-load tapchanger becomes contaminated with carbon formed in the immediate vicinity of the switching arc. This carbon formation bears a direct relationship to the load current and arcing time and whereas with earlier slow-speed designs the oil had to be treated or replaced after a few thousand operations a life of some 10 times this value is now obtainable.

The mid-point reactor type of tapchanger has some advantages over the high-speed resistor type, the main one being that since the reactor can be left in circuit between taps twice as many active working positions can be obtained for a given number of transformer tappings, giving a considerable advantage where a large number of tapping positions are required and this arrangement is still used by North American manufacturers. A number of special switching arrangements including shunting resistors, and modification to the winding arrangement of the reactor to enable use of vacuum switches, have been introduced to improve contact life where reactors are employed, but there are definite limits to the safe working voltage when interrupting circulating currents.

Recommendations for on-load tapchanging have been formulated as British Standard 4571 (CENELEC HD 367 S2) *On-load tap-changers* which is based on IEC 214 having the same title and IEC 542 *Application Guide for on-load* *tapchangers* and are primarily written to set performance standards and offer guidance on requirements for high-speed resistor-type equipment.

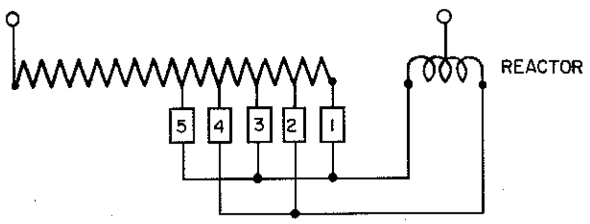
In some of the earliest designs of tapchangers the transformer was equipped with two parallel tapping windings. Each tap winding was provided with a form of selector and an isolating switch. When a tap change was required the isolating switch on one winding was opened, the load being transferred to the other tapping winding, the selector switch on the open circuit winding was then moved to its new position and the isolator reclosed. The second winding was treated in exactly the same manner and the operation was completed when both windings were finally connected in parallel on the new tapping position.

This scheme had the drawback that both halves of the windings were overloaded in turn, and the transformer had to be designed to restrict the circulating current which existed during the out-of-step mid-position. Any failure in the switching sequence or the switch mechanisms could be disastrous.

It is useful to explain the methods of tap changing which have been used in the past and those which are in use today.

On-load tap changing by reactor transition

The simplest form of reactor switching is that shown in *Figure 4.48*. There is only a single winding on the transformer and a switch is connected to each tapping position. Alternate switches are connected together to form two separate groups connected to the outer terminals of a separate mid-point reactor, the windings of which are continuously rated. The sequence of changing taps is shown in the table on the diagram. In the first position, switch No. 1 is closed and the circuit is completed through half the reactor winding.



POSITIONS	1	2	3	4	5	6	7	8	9
SWITCH 1	O	O							
2		O	O	O					
3				O	O	O			
4						O	O	O	
5								O	O

O INDICATES SWITCH CLOSED

Figure 4.48 On-load tap changing by reactor transition

To change taps by one position, switch No. 2 is closed in addition to switch No. 1, the reactor then bridges a winding section between two taps giving a mid-voltage position. For the next tap change switch No. 1 is opened and switch No. 2 is left closed so that the circuit then is via the second tap on the transformer winding. This particular type of tapchanger necessitates a relatively large number of current breaking switches which in turn produce a bulky unit and consequently a large oil volume is involved.

On-load reactor-type tapchanger using diverter switches

A modified type of reactor tapchanger is shown in *Figure 4.49*. This arrangement uses two separate selectors and two diverter switches. The selectors and diverter switches are mechanically interlocked and the sequence of operation is as follows. A tap change from position 1 to 2 is brought about by opening diverter switch No. 2, moving selector switch No. 2 from tap connection 11 to tapping connection 10 and then closing diverter switch No. 2.

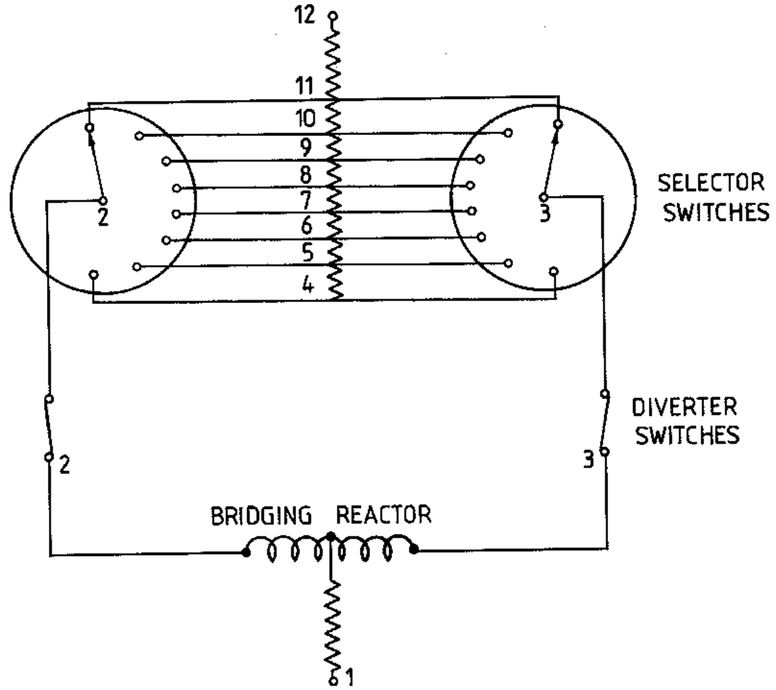
A tap change from position 2 to 3 initiates a similar sequence utilising selector and diverter switches No. 3 in place of switch No. 2.

On-load reactor-type tapchanger with vacuum switch

In some instances it is possible to utilise a vacuum interrupter in conjunction with a redesigned winding arrangement on the reactor-type tapchanger. A typical schematic diagram for this type of unit it shown in *Figure 4.50*.

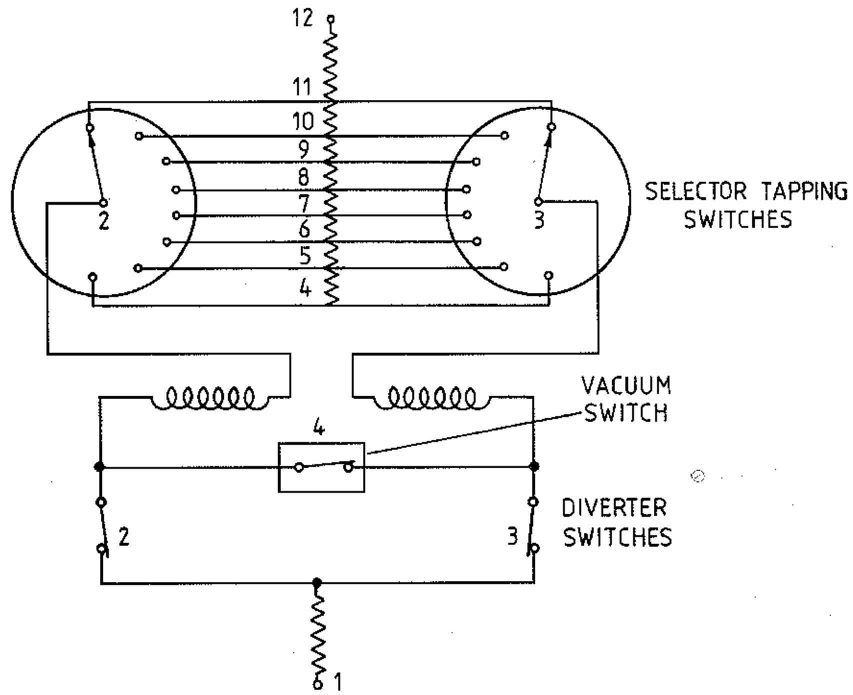
The running position for tap 1 is shown in the diagram with all switches closed. A tap change from tap position 1 to tap position 2 is as follows. Diverter switch No. 2 opens without arcing and the load current flows via selector switch No. 2, vacuum switch No. 4, in parallel with the circuit via diverter switch No. 3, selector switch No. 3 through diverter switch No. 3. Vacuum switch No. 4 opens, selector switch No. 2 moves from tap connection 11 to tap connection 10, vacuum switch No. 4 closes, diverter switch No. 2 closes, completing the tap change to tap position 2. A tap change from tap position 2 to tap position 3 utilises selector No. 3, diverter switch No. 3 and vacuum switch No. 4 in a similar manner to that explained for the movement from tap position 1 to tap position 2.

Whenever vacuum switches are used, the problem of protection against loss of vacuum must be considered. In North America, two approaches to this problem have been considered. The first is the current balance method where a current transformer detects the current flowing through the vacuum switch. If this does not cease on opening the switch mechanically the tapchanger locks out after one tap change during which the selector contact is called upon to break load and circulating currents. The second method utilises a transformer which applies a medium voltage across the vacuum gap between the closed contacts and a special metal contact sheath. If the gap breaks down, a relay ensures that the next tap change does not take place. A series contact disconnects this voltage before each tap change is initiated.



POSITION	CONNECTIONS	
	LEFT HAND SWITCH	RIGHT HAND SWITCH
1	2 – 11	3 – 11
2	2 – 10	3 – 11
3	2 – 10	3 – 10
4	2 – 9	3 – 10
5	2 – 9	3 – 9
6	2 – 8	3 – 9
7	2 – 8	3 – 8
8	2 – 7	3 – 8
9	2 – 7	3 – 7
10	2 – 6	3 – 7
11	2 – 6	3 – 6
12	2 – 5	3 – 6
13	2 – 5	3 – 5
14	2 – 4	3 – 5
15	2 – 4	3 – 4

Figure 4.49 On-load reactor-type tapchanger using diverter switches



POSITION	CONNECTIONS	
	LEFT HAND SWITCH	RIGHT HAND SWITCH
1	2 – 11	3 – 11
2	2 – 10	3 – 11
3	2 – 10	3 – 10
4	2 – 9	3 – 10
5	2 – 9	3 – 9
6	2 – 8	3 – 9
7	2 – 8	3 – 8
8	2 – 7	3 – 8
9	2 – 7	3 – 7
10	2 – 6	3 – 7
11	2 – 6	3 – 6
12	2 – 5	3 – 6
13	2 – 5	3 – 5
14	2 – 4	3 – 5
15	2 – 4	3 – 4

Figure 4.50 On-load reactor-type tapchanger with vacuum switch

Diverter resistor tapchangers

The concept of enclosure of the arc is attractive in many ways since it prevents oil contamination and eliminates the need for a separate diverter switch compartment. Even though the contact life of a high-speed resistor tapchanger is longer than that of a reactor type, the question of using vacuum switching of resistor units has been seriously considered for many years. Several designs have been proposed utilising the principle of removing the vacuum switches from the circuit and thereby from both current and voltage duties between tap changes.

In the USA, on-load tapchangers are frequently fitted on the low-voltage winding, and as stated in Clause 4.2 of ANSI C57.12.30-1977, 32 × 5/8% steps are quite normal. To meet these conditions it is more economical to use a reactor for the transition impedance and to utilise the bridging position as a tapping. This reduces the number of tapping sections required on the transformer winding. For this purpose, gapped iron-cored reactors with a single

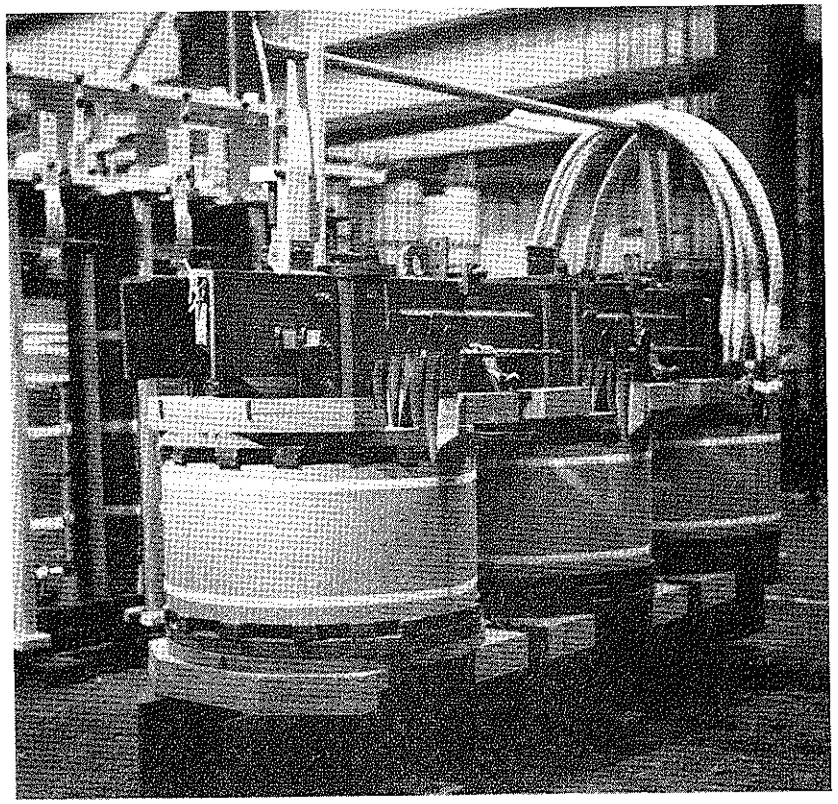


Figure 4.51 Three-phase reactor for a 200 MVA, 230/67 kV autotransformer with tappings at the LV line end (Federal Pacific Electric Co.)

centre-tapped winding are employed. The voltage across the reactor is equal to that of two tapping steps and the magnetising current at that voltage is approximately 40–50% of the maximum load current. *Figures 4.51* and *4.52* illustrate typical examples of North American practice employing reactor on-load tapchangers.

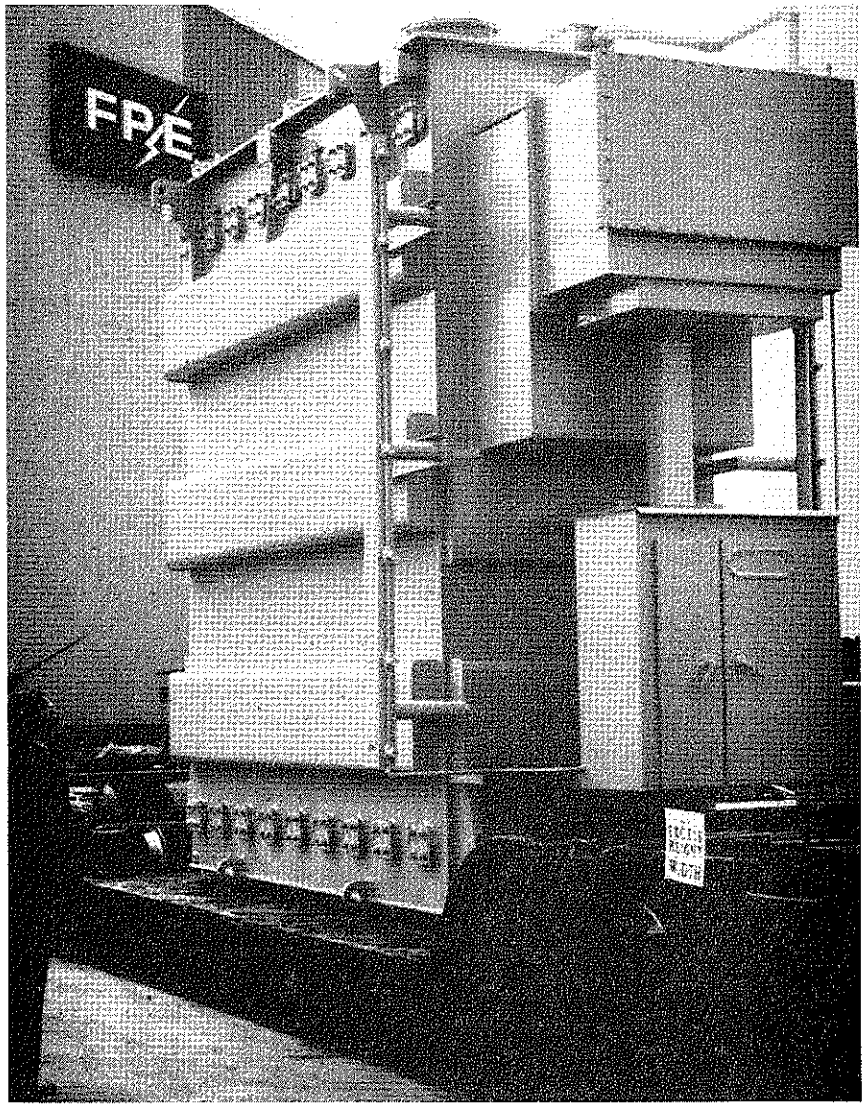


Figure 4.52 120 MVA, 230/13.8 kV, three-phase transformer with reactor pocket and the on-load tapchanger attached to the end of the tank (Federal Pacific Electric Co.)

As previously mentioned, high-speed resistor-type tapchangers have now almost completely superseded the reactor type in many parts of the world since it is easier and more economical to use resistors mounted in the tapchanger and the transformer tank need only be designed to accommodate the transformer core and windings.

In general high-speed diverter resistor tapchangers fall into two categories. The first is referred to as the double compartment type, having one compartment containing the selectors which when operating do not make or break load or circulating currents and a second compartment containing the diverter switches and resistors. It is in this compartment that all the switching and associated arcing takes place and where oil contamination occurs.

It is usual therefore to ensure that the oil in this chamber is kept separated from that in the main transformer tank. Double compartment-type tapchangers can also be considered to be of two types.

(a) In-tank type.
(b) Externally mounted type.

In-tank-type tapchangers

In the UK for many years the practice has been to house even the selector switches, which do not make or break current, in a separate compartment from the main tank so that these are not operating in the same oil as that which is providing cooling and insulation for the transformer. The operating mechanism for the selector switch contacts and the contacts themselves suffer wear and require maintenance, contact pressures have to be periodically checked, and minute metallic particles are produced and contaminate the oil. However, modern selector switch mechanisms have been developed since the early 1960s which need very little maintenance and cause very little oil contamination as a proportion of total quantity of oil in the main tank. These tapchangers have been designed for installation directly in the oil in the main tank, an arrangement which the manufacturers claim is cheaper, although the economic argument is a complex one.

They have the advantage that all tapping leads can be formed and connected to the appropriate selector switch contacts before the transformer is installed in the tank. With the separate compartment pattern, the usual practice is for selector switch contacts to be mounted on a base board of insulating material which is part of the main tank and forms the barrier between the oil in the main tank and that in the selector switch compartment. The tapping leads thus cannot be connected to the selector contacts until the core and windings have been installed in the tank. This is a difficult fitting task, requiring the tapping leads to be made up and run to a dummy selector switch base during erection of the transformer and then disconnected from this before tanking. Once the windings are within the tank, access for connection of the tapping leads is restricted and it is also difficult to ensure that the necessary electrical clearances between leads are maintained. With in-tank tapchangers it is still necessary to keep the diverter switch oil separate from the main-tank oil. This is usually achieved by housing the diverter switches within a cylinder of glass-reinforced resin mounted above the selector switch assembly. When the transformer is installed within the tank, removal of the inspection cover which forms the top plate of this cylinder provides access to the diverter switches. These are usually removable via the top of the cylinder for maintenance and contact inspection. Such an arrangement is employed on the Reinhausen type M series which is a German design, also manufactured in France under licence by the GEC Alsthom group.

Another claimed disadvantage of the in-tank tapchanger is that the selector switch contacts do, in fact, switch small capacitative currents thus generating gases which become dissolved in the oil. These dissolved gases can then cause confusion to any routine oil monitoring programme which is based on dissolved gas analysis (see Section 7 of Chapter 6). In addition it is, of course, necessary to take a drive from the diverter-switch compartment through to the selector switches and this usually requires a gland seal. There have been suggestions that this seal can allow contaminating gases to pass from the diverter-switch compartment into the main tank thus distorting dissolved gas figures. This was such a serious concern of those traditionally preferring separate compartment tapchangers that before acceptance of IEC 214 as a CENELEC harmonisation document an additional test was inserted into the *Service duty test* specification as a demonstration that hydrocarbon gases would not leak through the gland seal. This requires that the tapchanger undergoing service duty testing be placed in a chamber, not exceeding 10 times the volume of the diverter-switch compartment, filled with clean new transformer oil. At the end of the test sequence a sample of oil from this chamber is required to be tested for dissolved hydrocarbon gases which shall not show a total increase greater than 10 ppm (BS 4571: 1994, clause 8.2.1).

An example of an in-tank tapchanger is shown in *Figure 4.53*. The unit illustrated is rated at 300 A and 60 kV and is a three-phase 17-position linear regulator. This type of tapchanger is available for currents up to 500 A and a system voltage of 220 kV. In-tank tapchangers may also be utilised using three separate single-phase units; the advantage of this configuration lies in the fact that the phase to earth voltage only appears across the upper insulated housing which can be extended to provide appropriate insulation levels, while interphase clearances are determined by the design of the transformer. These clearances, together with an increase of the surrounding radial distance from the tank wall permit the working voltage to be extended to higher values more economically for certain applications than is the case with externally mounted tapchangers.

The diverter is designed as a three-pole segmental switch with the three sections spaced 120° apart. The sections of the diverter switches may be connected in parallel for currents up to 1500 A when the switch is used as a single-phase unit. When used on non-uniform insulation star point applications the diverter becomes a complete three-phase switch for currents up to 500 A.

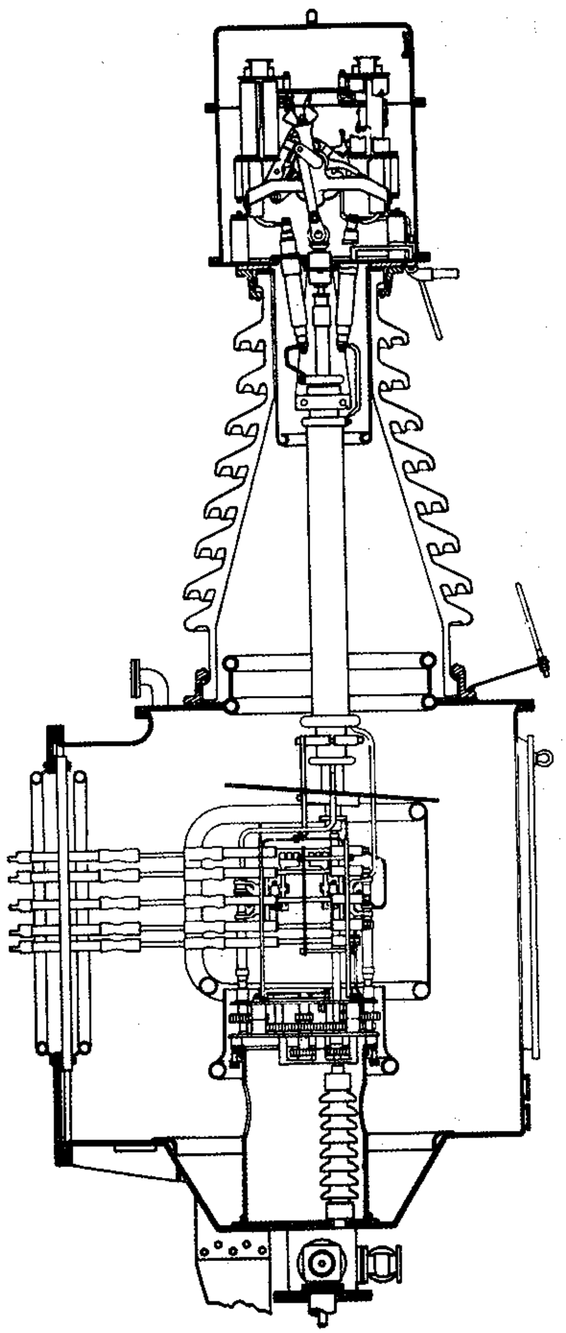


Figure 4.63(b) Cross-sectional drawing of the tapchanger illustrated in *Figure 4.63(a)*

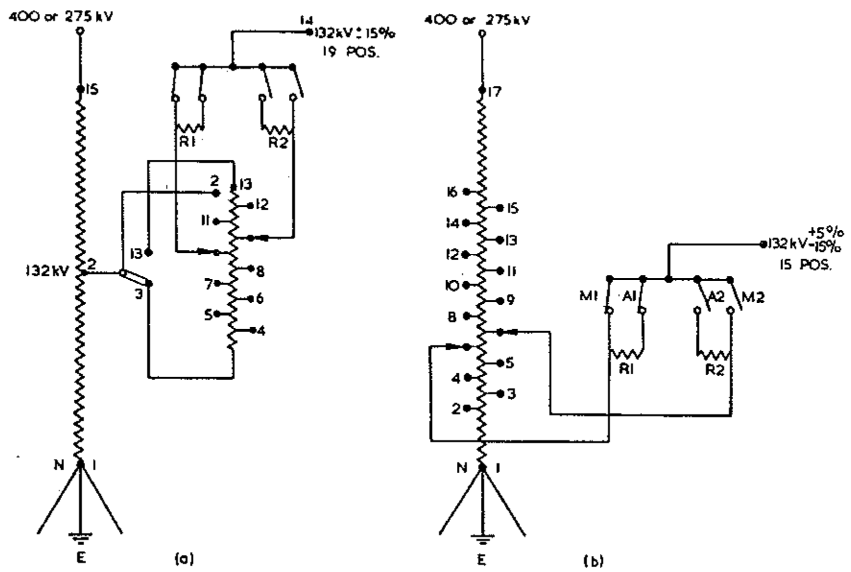


Figure 4.64 Diagrams of three-phase 400/132 kV and 275/132 kV autotransformers with 132 kV high-speed resistor-type tapchanger

If a bank of three single-phase transformers is used to make up a three-phase unit, then each phase must have its own tapchanger. This is often the case for large generator transformers. These need to be coupled so as to ensure that all three remain in step and, while it is possible to make this coupling electrically, it is far preferable and more reliable to use a single drive mechanism with a mechanical shaft coupling between phases. Assuming that the units have tappings at the neutral end of a star-connected HV winding, it is also necessary to make the HV neutral connection externally, usually by means of a copper busbar spanning the neutral bushings of each phase.

Another method of voltage regulation employed in transmission and distribution systems is one in which shunt regulating and series booster transformers are used. The former unit is connected between phases while the latter is connected in series with the line. Tappings on the secondary side of the shunt transformer are arranged to feed a variable voltage into the primary winding of the series transformer, these tappings being controlled by on-load tap changing equipment. The frame size or equivalent kVA of each transformer is equal to the throughput of the regulator multiplied by the required percentage buck or boost.

It should be noted that the voltage of the switching circuit of the regulator transformer to which the on-load tapchanger is connected can be an optimum value chosen only to suit the design and rating of the tap changing equipment. This arrangement of transformers is described as the series and shunt regulating transformer. It is normally arranged for 'in-phase' regulation but can also be employed for 'quadrature' regulation, or for both. *Figure 4.65* shows the connections for a typical 'in-phase' and 'quadrature' booster employing two tapchangers. Such a unit can be used for the interconnection of two systems for small variations of phase angle. Fuller descriptions of phase shifting transformers and quadrature boosters and their applications are given in Section 5 of Chapter 7.

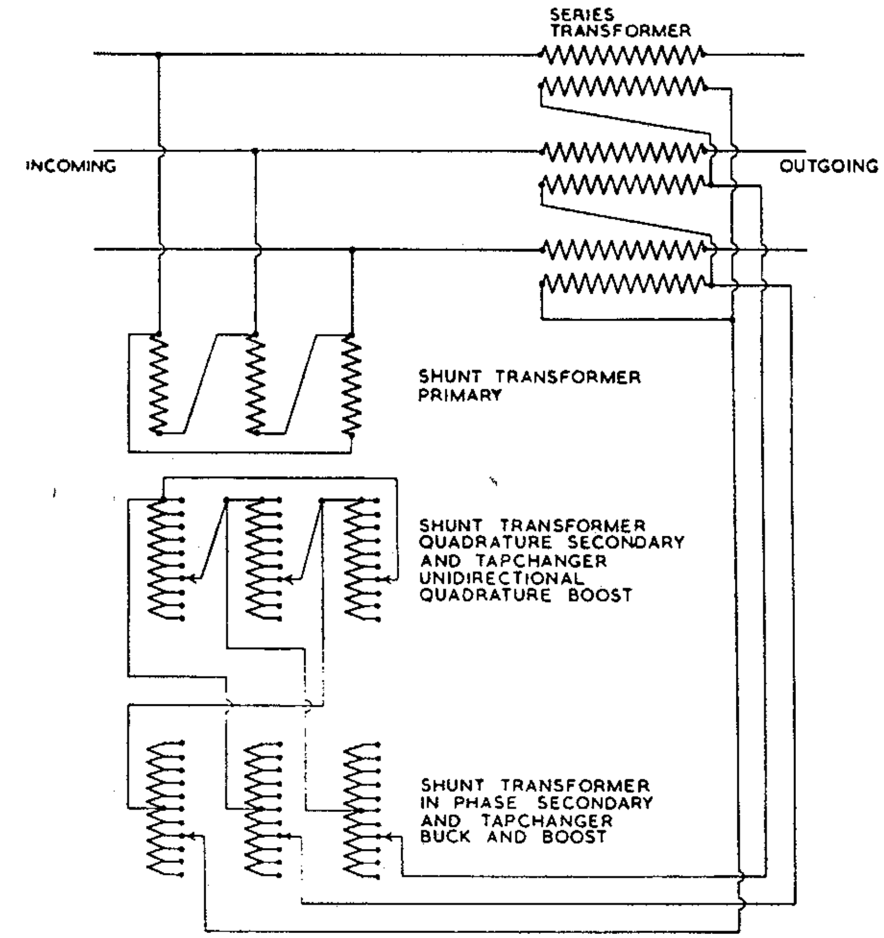


Figure 4.65 Diagram of connections for a three-phase 'in-phase' and 'quadrature' booster

Off-circuit tapchangers

As explained earlier in this chapter, the off-circuit tapping switch enables accurate electrical system voltage levels to be set when the transformer is

put into operation. Once selected, the transformer may remain at that setting for the remainder of its operating life. The simplest arrangement is that in which the power transformer tappings are terminated just below oil level and there changed manually by means of bolted swinging links or plugs mounted on a suitable terminal board. The drawback to this arrangement is that it necessitates removing the transformer tank cover or handhole cover. It is, however, extremely simple, reliable and is the cheapest tap changing device. It is important to design the tap changing link device with captive parts as otherwise there is always the danger that loose nuts, washers, etc. may fall into the tank while the position of the taps is being altered. *Figure 4.66* shows one phase of this arrangement used to provide off-circuit taps on a 345 kV transformer. In this situation it is necessary to incorporate stress shielding into both the bridging-link and the open ends of the unconnected tapping leads.

Most off-circuit tapping switches use an arrangement similar to the selector switch mechanism of the on-load tapchanger, employing similar components, but if these selector contacts are not operated occasionally contact problems can occur. This can be particularly problematical for higher current-rating devices. An example is the case of power station unit transformers. On some large stations these can have ratings as high as 50 MVA at 23.5/11 kV. The 23.5 kV HV side is connected to the generator output terminals whose voltage is maintained within $\pm 5\%$ of nominal by the action of the generator automatic voltage regulator. The transformer is normally only in service when the unit is in operation and under these conditions its load tends to be fairly constant at near to rated load. An on-load tapchanger is therefore not essential and would reduce reliability, but off-circuit taps are desirable to enable fine trimming of the power station electrical auxiliary system voltage to take place when the station is commissioned. For a transformer of this rating the HV current can be up to 1300 A which for trouble-free operation demands a very low contact resistance. If this is not the case heating will take place resulting in a build-up of pyrolytic carbon which increases contact resistance still further. This can lead to contact arcing and, in turn, produces more carbon. Ultimately a runaway situation is reached and the transformer will probably trip on Buchholz protection, shutting down the associated generator as well. To avoid the formation of pyrolytic carbon on high-current off-circuit tapchangers, it is vital that the switch has adequate contact pressure and that it is operated, off-circuit, through its complete range during routine plant maintenance or preferably once per year to wipe the contact faces clean before returning it to the selected tapping. Because of these problems, the UK Central Electricity Generating Board in its latter years specified that ratio adjustments on unit transformers and other large power station auxiliary transformers, which would, hitherto, have had off-circuit tapping switches, should be carried out by means of links under oil within the transformer tank. The links need to be located at the top of the tank so that access can be obtained with the minimum removal of oil, but provided this is specified, tap changing is relatively simple and reliability is greatly improved. In fact, the greatest inconvenience from

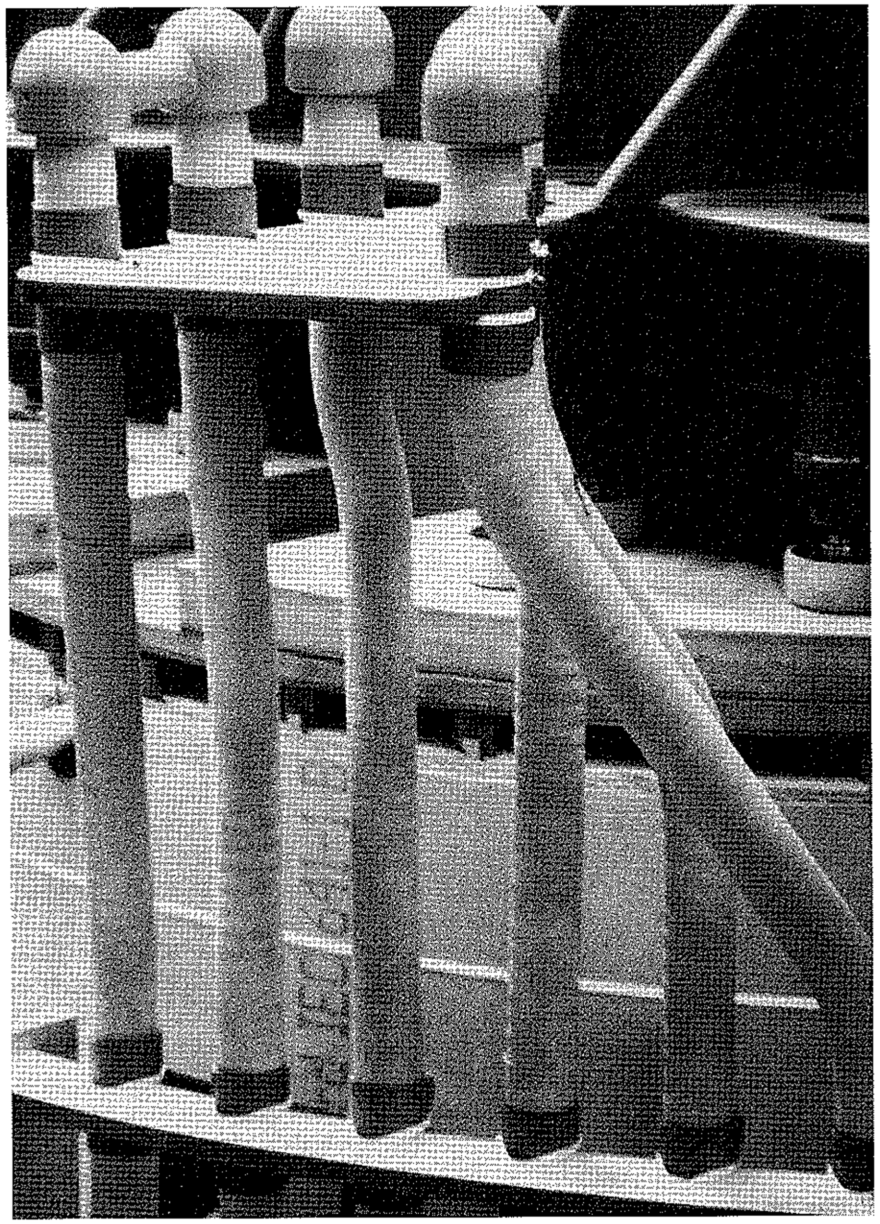


Figure 4.66 Arrangement of links under oil used to provide off-circuit taps on the HV winding of a 650 MVA, 20.9/345 kV, generator transformer supplied to the USA (Peebles Transformers)

this arrangement occurs during works testing, when the manufacturer has to plan his test sequence carefully in order to minimise the number of occasions when it is necessary to change taps. More tap changes will probably be made at this time than throughout the remainder of the transformer lifetime. This problem does not, of course, arise on the many small distribution and industrial transformers of 1 or 2 MVA or less operating at 11/0.433 kV. These have an HV current of less than 100 A which does not place high demands on contact performance when operating under oil. Very conveniently, therefore, these can be provided with simple off-circuit switches enabling the optimum ratio to be very easily selected at the time of placing in service. It is nevertheless worthwhile operating the switches, where fitted, whenever routine maintenance is carried out, particularly where the transformer is normally operating at or near full load when the oil temperature will consequently be high.

Construction of tapchangers

It is a fundamental requirement of all tapchangers that the selector and diverter switches shall operate in the correct sequence. One of the methods used is based on the Geneva wheel. *Figure 4.67* shows the mechanism and its main component parts. The drive shaft 46 is driven from the motor drive or manual operating mechanism via a duplex chain and sprocket 45, and is coupled at one end to the diverter drive 42, and at the other end to the selectors via the lost motion device 78 and the Geneva arm 77. Referring to *Figures 4.67(b)* and *(c)* the lost motion device operates as follows: the drive shaft 46 has a quadrant driving segment in contact at its left-hand side with a quadrant segment on the Geneva arm 77. If the drive shaft rotates in a clockwise direction then the Geneva arm will be driven. However, if the drive shaft rotates in an anticlockwise direction then no movement of the Geneva arm takes place until the drive shaft has rotated through 180°. During this 180° rotation the diverter switch driven by 42 will have completed a full operation. Further drive shaft rotation will move the appropriate Geneva wheel for a particular selector.

Examination of the operation of the four-position Geneva mechanism in *Figure 4.67(c)* shows the following:

- The Geneva drive does not engage until the Geneva drive arm itself has passed through approximately 45°.
- The driven period of the selector shaft occupies only 90° of the movement of the Geneva arm and the selector rotation rate is not constant. Entry of the Geneva arm into the slot produces an initial slow start increasing to maximum velocity after 45° of rotation when the drive wheel centres are in line and reducing to zero as the Geneva arm rotates through the second 45°.
- The Geneva arm travels a further 45° after disengaging from the Geneva drive wheel before the completion of a tap sequence.

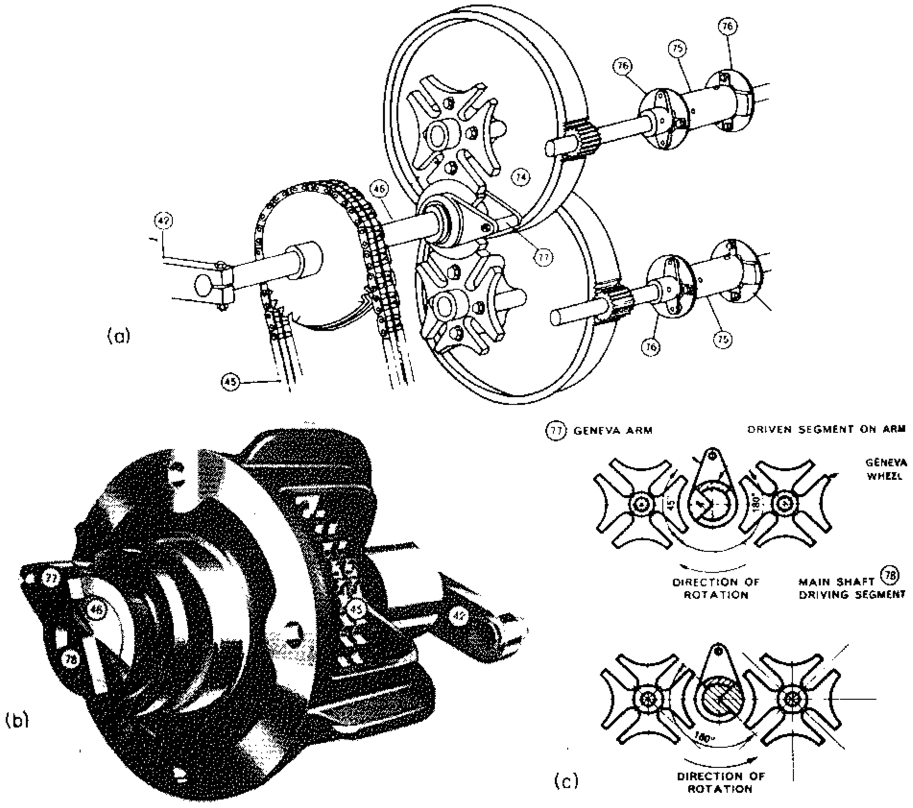


Figure 4.67 (a) Schematic drawing of a Geneva mechanism and drive; (b) 180° lost motion device; (c) Schematic drawing of 180° lost motion device and Geneva drive.

42. Diverter switch driving crank;
45. Driving chain and sprocket;
46. Selector switch drive shaft;
74. Selector switch Geneva mechanism;
75. Insulated driving shafts;
76. Flexible couplings;
77. Geneva arm;
78. 180° lost motion segment

The tapchanger design arranges for the diverter switch operation to occur after the moving selector has made contact with the fixed selector. In order to provide a definite switching action of the diverter switch it is usual to provide some form of positive stored energy device to operate the diverter switch of the single compartment unit.

Examples of stored energy devices are a spring charged across a toggle which is tripped mechanically at a predetermined time. Alternatively a falling weight is driven to a top dead centre position by a motor or by manual operation and once at that position provides sufficient energy to complete the tap change.

Highly reliable operation has been achieved and long contact life can be guaranteed; diverter switch contacts will now last generally for the useful life of the transformer itself. One type of three-phase single compartment tapchanger suitable for 44 kV, 600 A, 17 positions is illustrated in *Figure 4.68.* It is fitted with a low oil level and surge protection device which is shown at the top of the tapchanger housing.

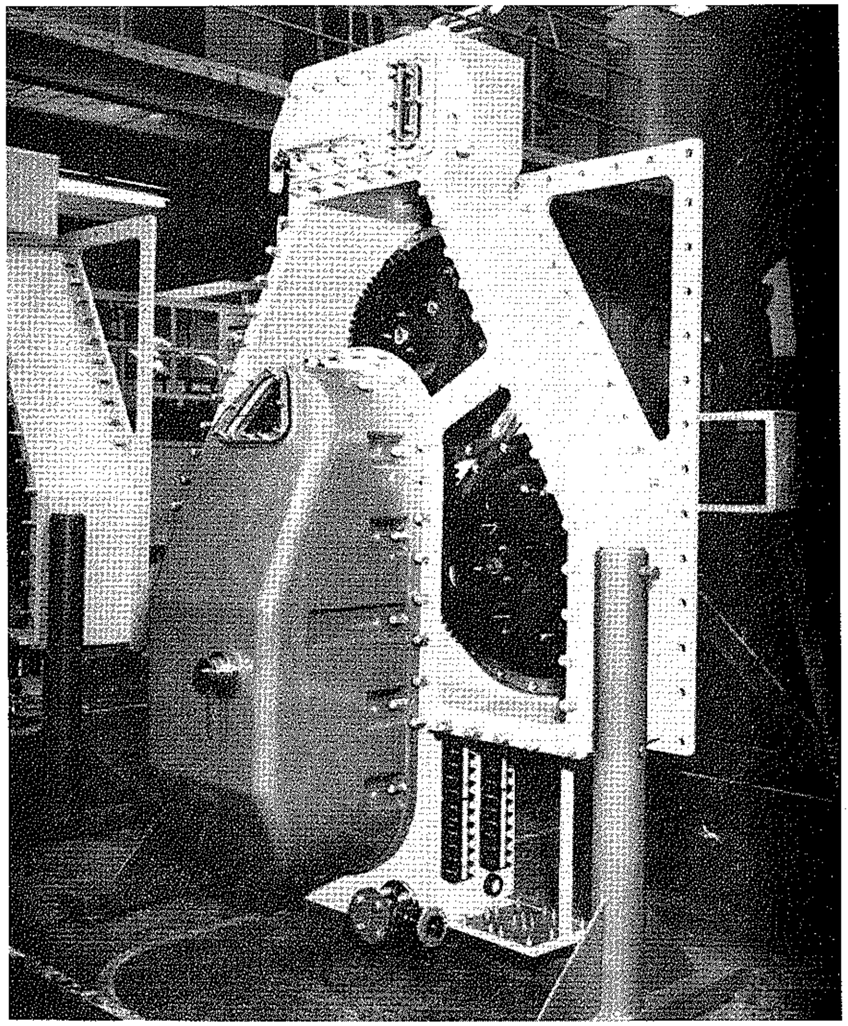


Figure 4.68 600 A, 44 kV three-phase 17 position single compartment tapchanger (Ferranti Engineering Ltd)

Figure 4.69 illustrates three single-phase 1600 A linear-type tapchangers mechanically coupled together and is suitable for connection at the neutral end of a 400 kV graded winding.

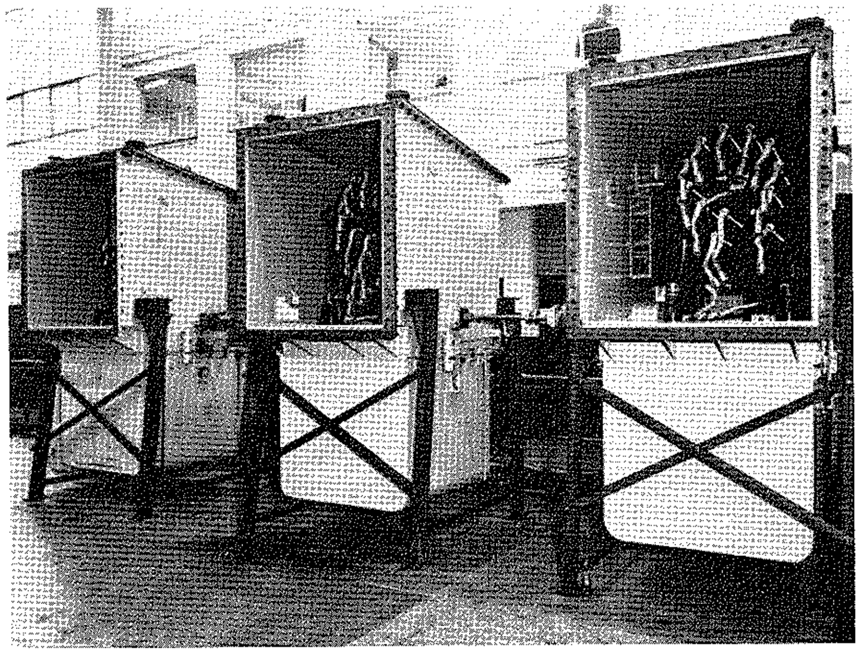


Figure 4.69 Three single-phase 19 position linear-type, 1600 A tapchangers mechanically coupled as a three-phase unit (Associated Tapchangers Ltd)

Figure 4.70 shows an example of a three-phase roller contact diverter switch which would be housed in the diverter compartment of the tapchanger shown in *Figure 4.62*.

Figure 4.71 illustrates a three-phase tapchanger which can be used as a coarse/fine or reversing regulator up to 33 positions, alternatively 17 positions as a linear switch. It is rated at 600 A with a power frequency insulation level of 70 kV, 200 kV impulse and is suitable for use at the neutral end of a 132 kV winding. On the right-hand side of the tapchanger is the separate compartment containing the driving mechanism and incorporated into this chamber is the Ferranti 'integral solid-state voltage and temperature control unit'. This feature dispenses with the necessity of a separate tapchanger and cooling circuit control cubicle.

Control of on-load tapchangers

Many advances have been made in the design of control circuits associated with on-load tap changing. Mention has already been made of driving mechanisms and the fundamental circuits associated with the starting of the motor for carrying out a tap change. While these vary from one maker to another they are comparatively simple. In general, the motor is run up in one direction

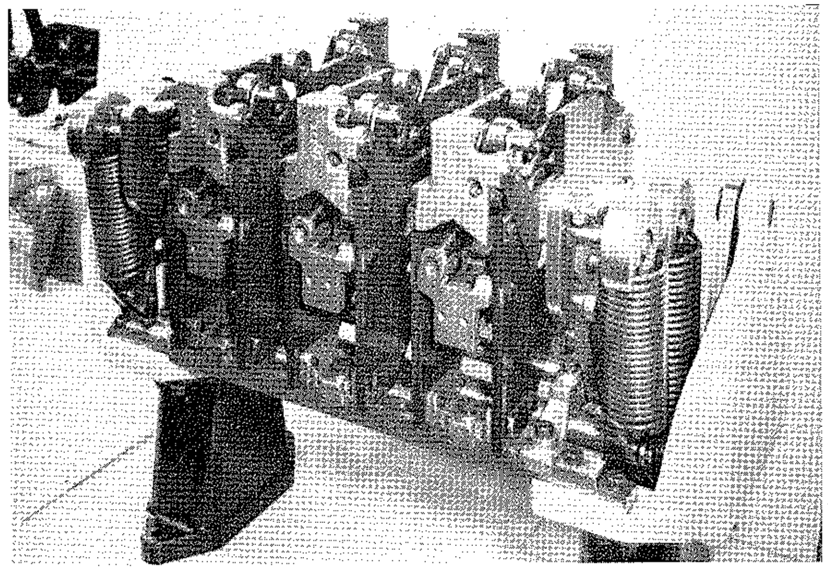


Figure 4.70 Three-phase roller contact diverter switch rated 650 A, normally housed in the diverter compartment of the tapchanger shown in *Figure 4.62* having 70 kV test level (Ferranti Engineering Ltd)

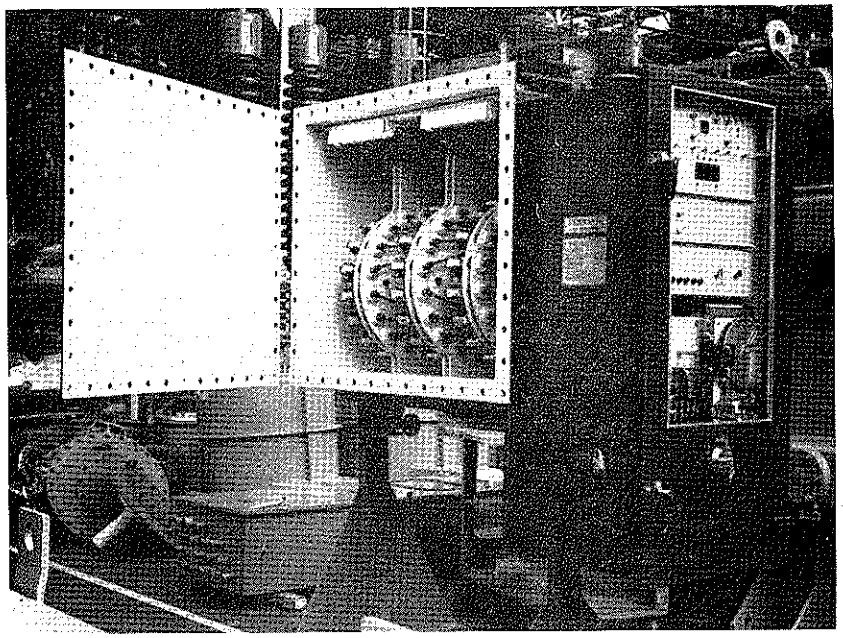


Figure 4.71 Three-phase, 600 A, single compartment tapchanger fitted with an integrated solid-state voltage and temperature control unit (Ferranti Engineering Ltd)

for a 'raise' tap change and in the reverse direction for a 'lower' tap change. In some cases a brake is employed to bring the motor to rest while in others clutching and declutching are carried out electrically or mechanically. It is, however, the initiation of the tap change and the control of transformers operating in parallel where the main interest lies and where operational problems can arise if the tapchangers are 'out of step'.

Manual operation must always be available for emergency use and in some cases tapchangers are supplied for hand operation only.

Many installations are designed for simple pushbutton control but there has been a tendency towards unattended automatic voltage control at substations so that a predetermined constant or compensated busbar voltage can be maintained. In general, with these schemes a tapchanger is provided on a transformer for maintaining a predetermined outgoing voltage where the incoming voltage is subject to variations due to voltage drops and other system variations.

It is reasonable to expect that with the advent of digital control it will become possible to perform all the operations necessary for the control and operation of tapchangers and the monitoring of their performance by a single device using digital computer technology coupled to low-burden output voltage and current transformers, thereby enabling very accurate control to be obtained with much simplified equipment. At the present time, however, the basic control devices remain within the class which is generally termed 'relays', even though these may utilise solid-state technology, and tapchanger control continues to operate on principles which have developed since the early days of on-load tap changing, with individual circuit elements performing discrete functions. The following descriptions therefore describe these traditional systems.

Voltage control of the main transformer requires a voltage transformer energised from the controlled voltage side of the main transformer. The voltage transformer output is used to energise a voltage relay with output signals which initiate a tap change in the required direction as the voltage to be controlled varies outside predetermined limits. It is usual to introduce a time delay element either separately or within the voltage relay itself to prevent unnecessary operation or 'hunting' of the tapchanger during transient voltage changes.

The 'balance' voltage of the relay, namely the value at which it remains inoperative, can be preset using a variable resistor in the voltage-sensing circuit of the relay so that any predetermined voltage within the available range can be maintained.

Often it is required to maintain remote busbars at a fixed voltage and to increase the transformer output voltage to compensate for the line drop which increases with load and this is achieved by means of a line drop compensator. This comprises a combination of a variable resistor and a tapped reactor fed from the secondary of a current transformer whose primary carries the load current. By suitable adjustment of the resistance and reactance components, which depend upon the line characteristics it is possible to obtain a constant voltage at some distant point on a system irrespective of the load or power factor.

Figure 4.72 shows the principle of the compensator which for simplicity is shown as a single-phase circuit. The voltage transformer is connected between lines and the current transformer is connected as shown to the variable resistance and reactance components. These are so connected in the voltage relay circuit that the voltage developed across them is subtracted from the supply voltage, then as load current increases the voltage regulating relay becomes unbalanced and operates the main regulating device to raise the line voltage at the sending end by an amount equal to the line impedance drop and so restore the relay to balance. The reverse action takes place when the load current decreases. The regulating relay and compensator are usually employed in three-phase circuits, but since the relay voltage coil is single phase, usually connected across two phases, the only difference between the arrangement used and that shown in *Figure 4.72* is that the arrangement of the voltage and current transformer primary connections must be such as to provide the proper phase relation between the voltage and the current.

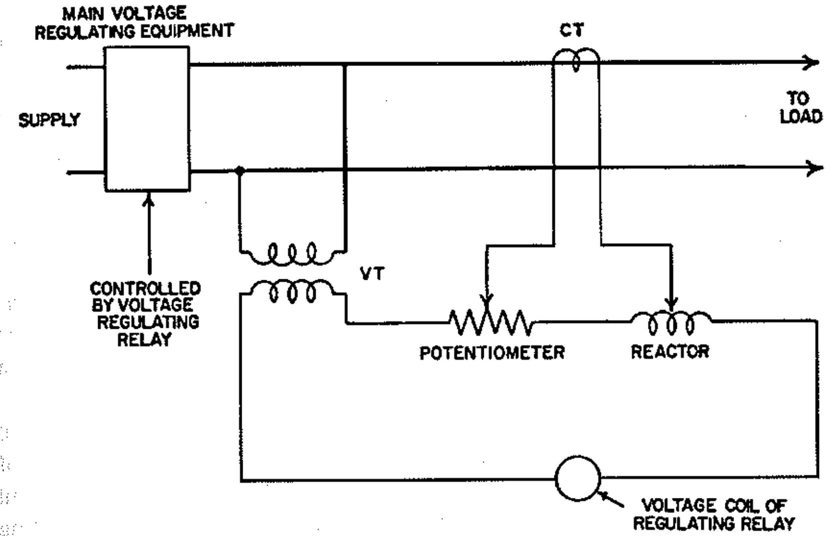


Figure 4.72 Single-phase diagram showing the principle of line drop compensation

The voltage transformer is connected across the A and C phases and the current transformer in the A phase. Different phases may be used provided the phase relationship is maintained. The compensation afforded by this method is not strictly correct since there is a 30° phase displacement between the voltage and the load current at unity load power factor. Since line drop compensation is usually a compromise this method is acceptable in many cases.

In *Figure 4.72* a single current transformer is shown in the line connection for the current supply to the compensator. It is usual practice to have an interposing current transformer in order to obtain the correct full-load secondary current but, at the same time, provide protection against damage due to overloads or fault currents in the line. The interposing current transformer is specifically designed to saturate under such conditions, thus avoiding the introduction of high overload currents to the compensator circuit. If greater accuracy is desired another method may be used with this scheme – the voltage transformer is connected across A and B phases with the main current transformer primary in C phase. Alternative phases may be used provided the phase relationship between voltage and the current is maintained. With this connection, since the current and voltage are in quadrature at unity load power factor, the resistor and reactor provide the reactance and resistance compensation respectively. In all other respects this compensator is identical with that described for the first scheme but there is no phase angle error.

For many years the automatic voltage relay (AVR) used was the balanced plunger electromechanical type and many of these are still in service. Nowadays a solid-state voltage relay is used. For the former type a standard arrangement of line drop compensator has the external series resistor and mean setting adjustment rheostat for the regulating element of the voltage regulator mounted in the compensator, which has three adjustable components providing the following: variation of 90–110% of the nominal no-load voltage setting, continuously variable range of 0–15% compensation for resistance and 0–15% reactive compensation.

If compensation is required for line resistance only, a simple potentiometer resistor is used instead of the complete compensator and the external resistor and mean setting adjustment are supplied separately. When the compensator has been installed and all transformer polarities correctly checked, the regulating relay may be set to balance at the desired no-load voltage. The resistance and reactance voltage drops calculated from the line characteristics may then be set at the appropriate values.

A voltage control cubicle with voltage regulating relay and line drop compensation is shown in *Figure 4.73*. The voltage regulating relay is of the balanced plunger electromechanical type and a simplified arrangement of the relay is shown in *Figure 4.74*. The design of the solenoid regulating element ensures that the magnetic circuit is open throughout the operating range. Therefore, the reluctance of the circuit is now appreciably affected by movement of the core and the unit operates with a very small change in ampere-turns.

Basically the element consists of a solenoid C with a floating iron core guided by two leaf springs LS which permit vertical but not lateral movement. Control spring S, which has one end anchored to the relay frame and the other attached to the moving iron core 'a', is carefully adjusted to balance

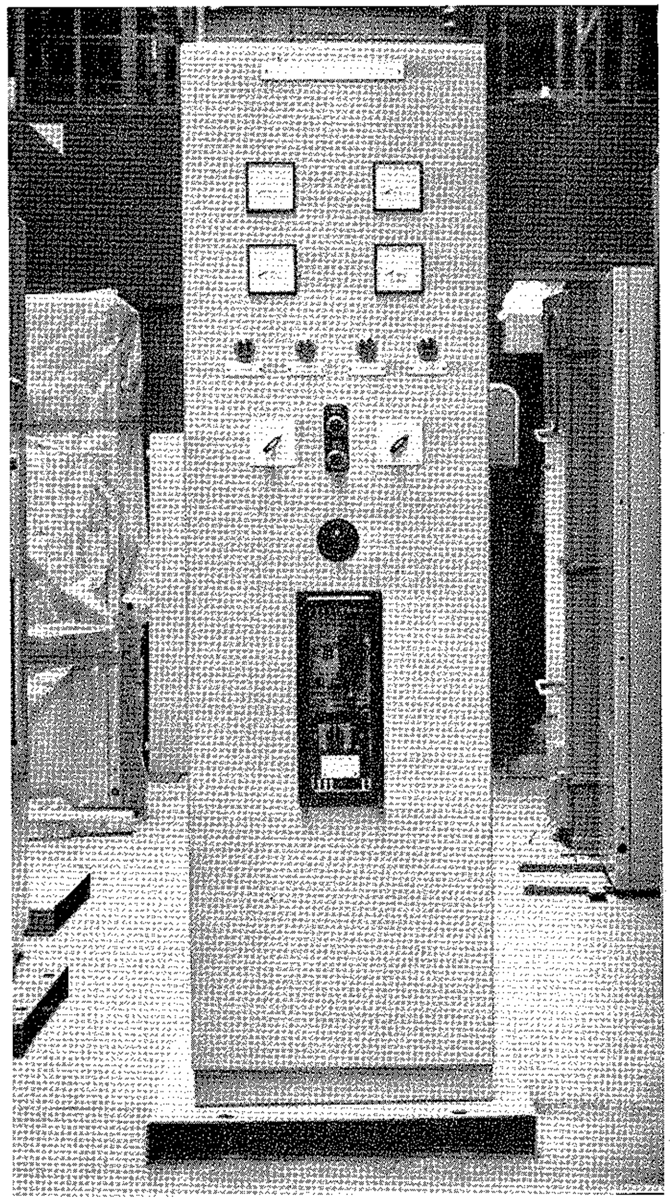


Figure 4.73 Control cubicle with voltage regulating relay (Bonar Long Ltd)

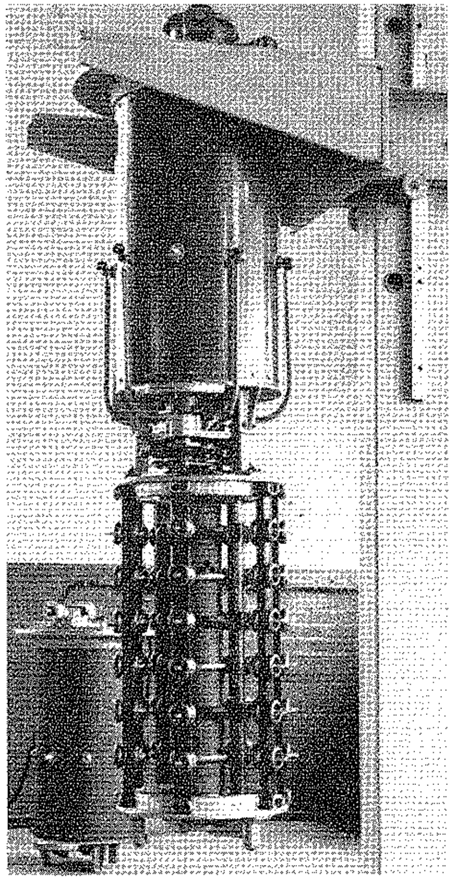


Figure 4.53 Three-phase 300 A, 60 kV, 17 position linear in-tank tapchanger

The whole diverter switch assembly may be lifted out of the upper housing for inspection or contact changing, and this housing is completely sealed from the oil in the main tank with the exception of the drive to the selector switches. The selectors are built in a 'cage' whose vertical insulating bars retain the fixed contacts and the transformer tapping connections are bolted directly to these terminals, with the odd and even selectors concentrically driven by independent Geneva mechanisms. The cage design eliminates the need for a barrier board as on an externally mounted tapchanger, but access to the selectors necessitates removal of part or all of the transformer oil in the main tank.

If required, it is possible to install the equipment with separate tanks and barrier boards to improve selector accessibility but, of course, the main benefit of using an in-tank tapchanger is lost. *Figure 4.54* illustrates an in-tank type tapchanger mounted from the tank cover and showing the leads from the HV winding.

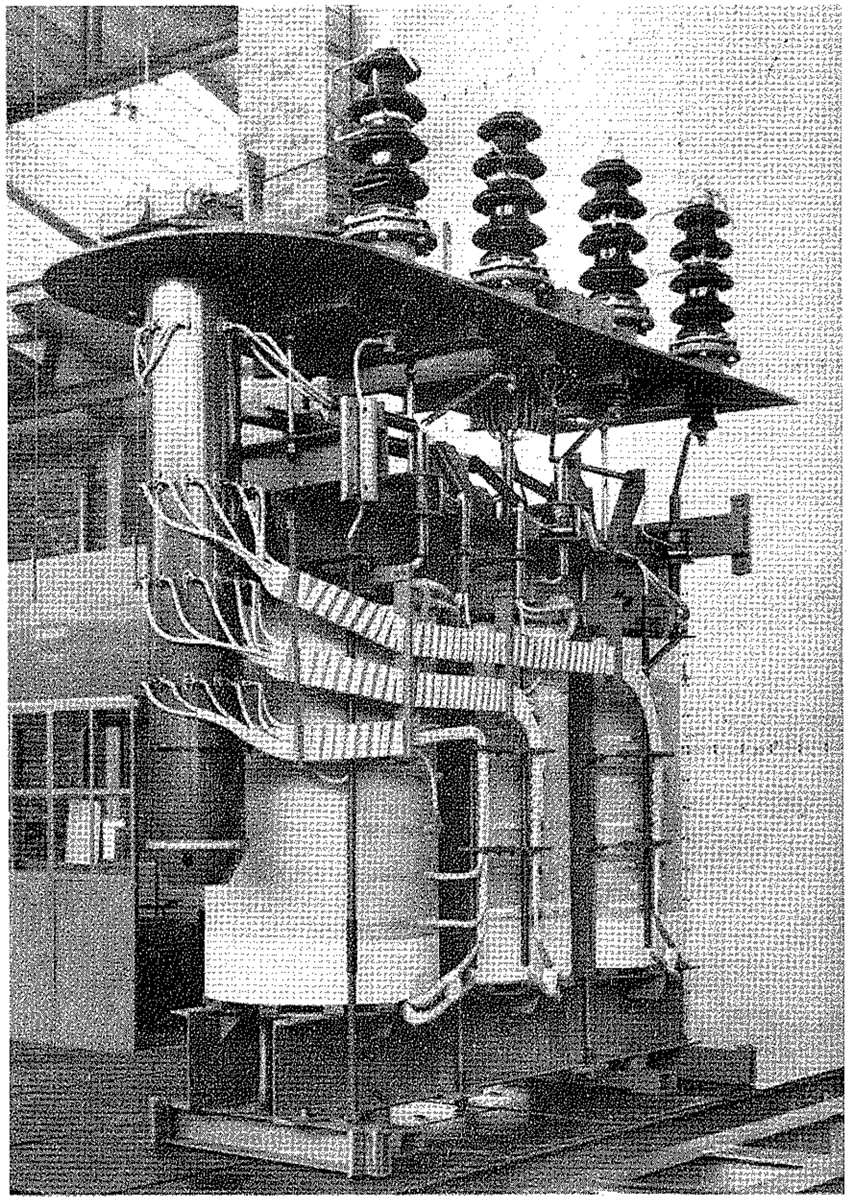


Figure 4.54 20 MVA, 33/11 kV three-phase core and windings fitted with an in-tank tapchanger (Bonar Long Ltd)

High-speed resistor tapchangers can be divided into two types, those which carry out selection and switching on the same contacts and generally use one resistor, and others which have selectors and separate diverter switches and which normally use two resistors. With a single resistor, load current and resistor circulating current have to be arranged to be subtractive, which dictates use with unidirectional power flow or reduced rating with reverse power flow. When two resistors are employed the duty imposed on the diverter switch is unchanged by a change in the direction of power flow. Recently versions of the combined diverter/selector types have been developed having double resistors and thus overcoming the unidirectional power flow limitation.

The two types fall into two classes, single and double compartment tapchangers. Most designs of the single compartment type employ a rotary form of selector switch and *Figure 4.55* shows diagrammatically the various switching arrangements for resistor-type changers. *Figure 4.55(a)* illustrates the method employed for the single compartment tapchanger and is known as the *pennant cycle*, while *Figures 4.55(b)* to *(d)* show the connections when two resistors and separate diverter switches are employed and is known as the *flag cycle*. (The derivation of the terms 'flag cycle' and 'pennant cycle' and the precise definition of these terms are explained in BS 4571. They arise from the appearance of the phasor diagrams showing the change in output voltage of the transformer in moving from one tapping to the adjacent one. In the 'flag cycle' the change of voltage comprises four steps, while in the 'pennant cycle' only two steps occur.)

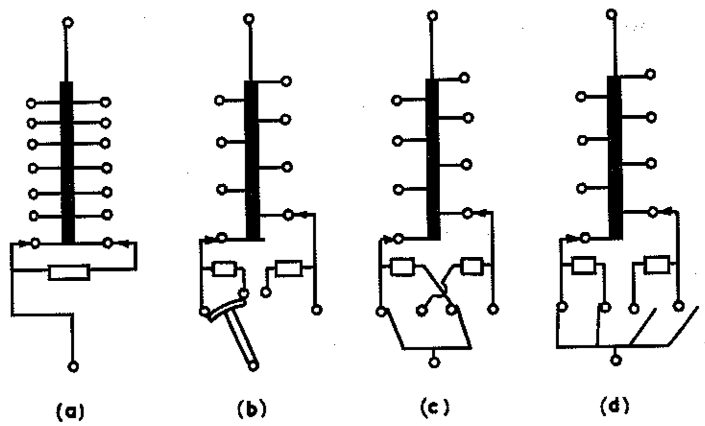


Figure 4.55 Types of resistor transition tap changing. (a) Pennant cycle; (b), (c) and (d) flag cycle

Single compartment tapchangers were largely developed in order to provide an economical arrangement for medium-sized local distribution transformers. On larger transformers, for example those used at bulk supply points, the on-load tap changing equipment is usually the double compartment type with

separate tap selectors and diverter switches. The tap selectors are generally arranged in a circular form for a reversing or coarse/fine configuration, but are generally in line or in a crescent arrangement if a linear tapping range is required.

Figure 4.56 illustrates a double resistor-type tapchanger and a typical schematic and sequence diagram arrangement is shown in *Figure 4.57*. Switches $S1$ and $S2$ and the associated tapping winding connections are those associated with the selectors. These selectors are the contacts which do not make or break current and therefore can be contained in transformer oil fed from the main tank conservator. $M1$, $M2$, $T1$, $T2$, $R1$, $R2$ are the components of the diverter switch. Mounted on the diverter switch also are the main current-carrying contacts which, like the selector switches, do not make or break current.

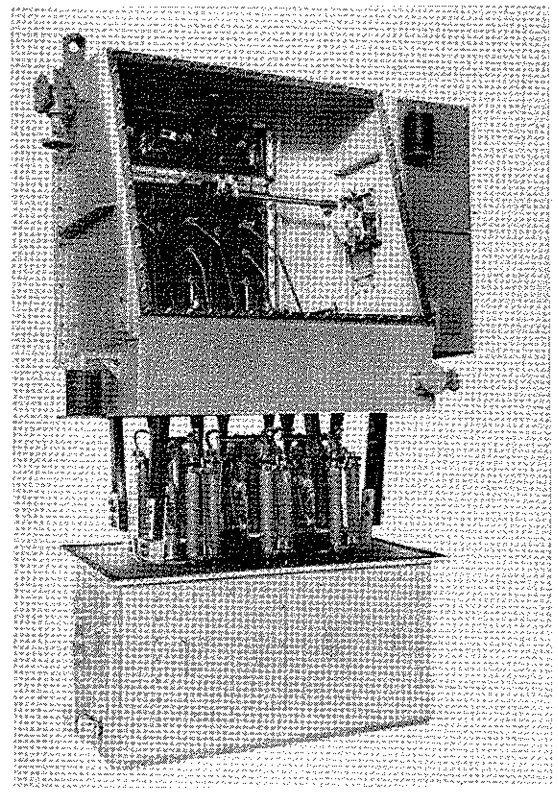


Figure 4.56 Three-phase 400 A, 44 kV high-speed resistor-type double compartment tapchanger with the diverter tank lowered (Associated Tapchangers Ltd)

The schematic diagram indicates that the right-hand selector switch $S1$ is on tap position 1 and the left-hand selector switch $S2$ is on tap 2 while the diverter switch is in the position associated with tap 1. A tap change from

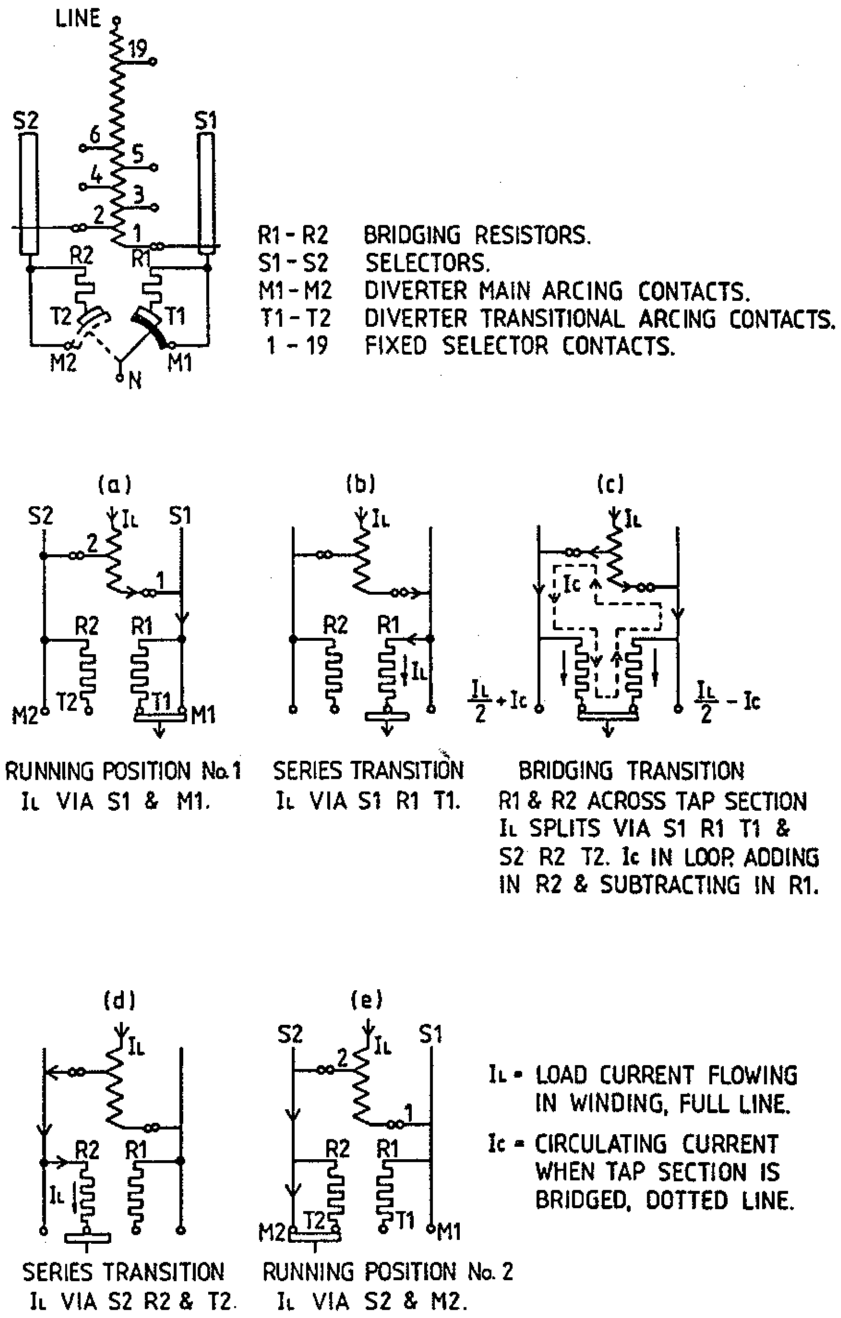


Figure 4.57 Typical schematic and sequence diagram of a double resistor-type tapchanger

position 1 to 2 requires a movement of the diverter switch from the right-hand side to the left side while a further tap change from tap position 2 to tap position 3 requires a movement of selector switch $S1$ from position 1 to position 3 before the diverter switch moves from the left-hand side to the right side. In order to produce this form of sequence the tapchanger utilises a mechanism known as a lost motion device. The sequence diagram assumes the tapchanger to be fitted to the neutral end of the HV winding of a step-down transformer.

Load current flows from the main winding through $S1$ and $M1$ of the diverter switch to the neutral. Initiation of a tap change causes the moving arcing contact to move from the right-hand side to the left-hand side. At (*b*) the moving contact has opened contact with the main fixed arcing contact *I*; arcing will continue across the gap between these two contacts until the first current zero is reached. After this the current will flow through the diverter resistor $R1$. This current passing through $R1$ induces a recovery voltage between $M1$ and the moving arcing contact. The value of the recovery voltage is $I_L R1$. Although initial examination at this point would suggest that the value of $R1$ be kept as low as possible in order to keep the recovery voltage down to a relatively low value, an examination at other positions produces a conflicting requirement to minimise the circulating current by maximising the resistor value, and therefore the actual value of the diverter resistor is a compromise.

At (*c*) the moving arcing contact is connected to both transition resistors $R1$ and $R2$. A circulating current now passes between tap position 2 and tap position 1 via $R2$–$R1$. The value of this circulating current is the step voltage between tap positions 2 and 1 divided by the value of $R1$ plus $R2$. Hence there is a requirement to make $R1$ plus $R2$ as high as possible to limit the circulating current.

At (*d*) the moving contact has now moved far enough to have broken contact with $T1$. Arcing will again have taken place between these two contacts until a current zero is reached. The recovery voltage across this gap will be the step voltage between the tap positions 2 and 1 minus the voltage drop across $R2$. It should be noted that when changing from tap 1 to tap 2 (*b*) produces a similar condition to that which occurs at (*d*) but the recovery voltage between the transition contact of $R2$ and the moving contact is the step voltage plus the voltage drop across $R1$. At (*e*) on the sequence diagram the tap change has been completed and load current I_L is now via $S2$–$M2$ to the neutral point of the winding.

If the sequence is continued through to the end of the tapping range it can be seen that the more onerous conditions of current switching and high recovery voltages occur on alternate sides. Should the power flow be reversed the same conditions will apply but occur on the other alternate positions of switching. The diagram shown for the movement between two tap positions is of the same configuration shown in IEC 214 for the flag cycle. For the single compartment tapchanger using only one diverter resistance there is considerable difference between that sequence and that of the double resistor unit.

Referring to *Figure 4.58* an explanation of the single resistor switching sequence is as follows. Assuming that the tapchanger is in the neutral end of the HV winding of a step-down transformer then position (*a*) is the normal operating position 1. Initiation of a tap change movement causes the transitional arcing contact to make connection with the fixed arcing contact of

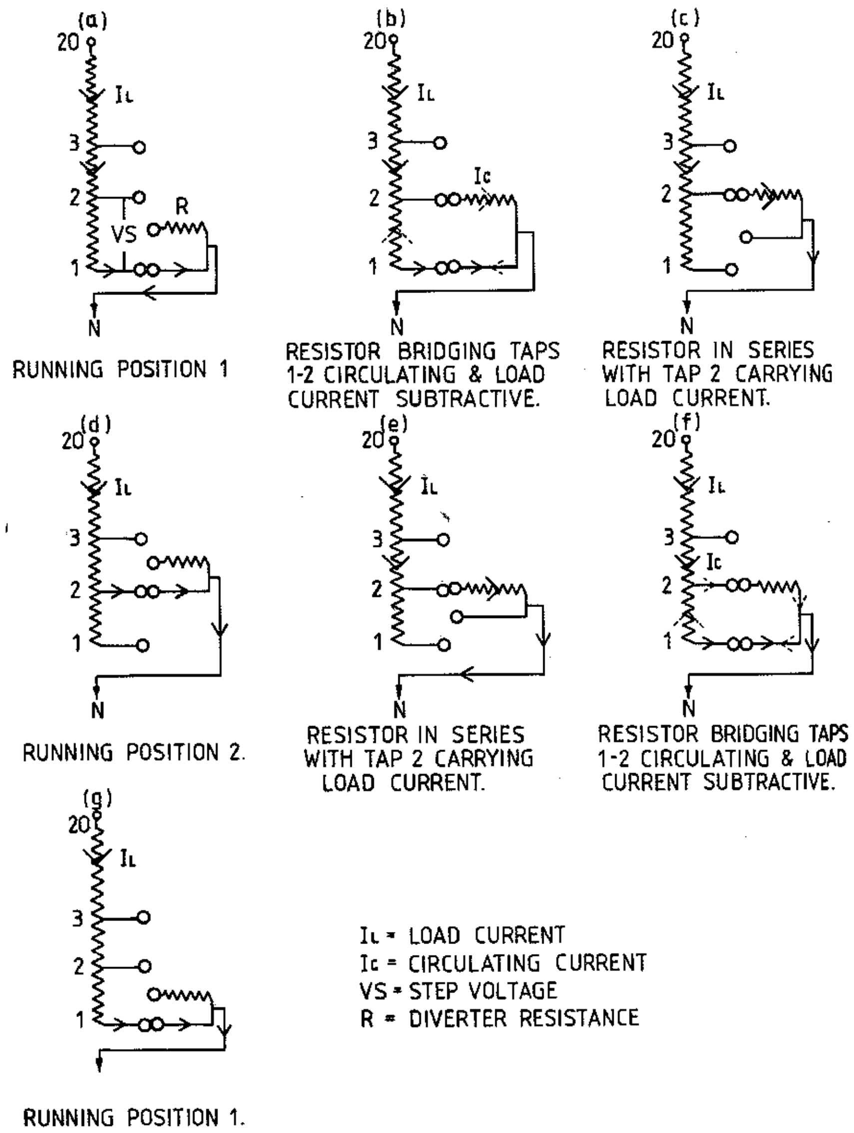
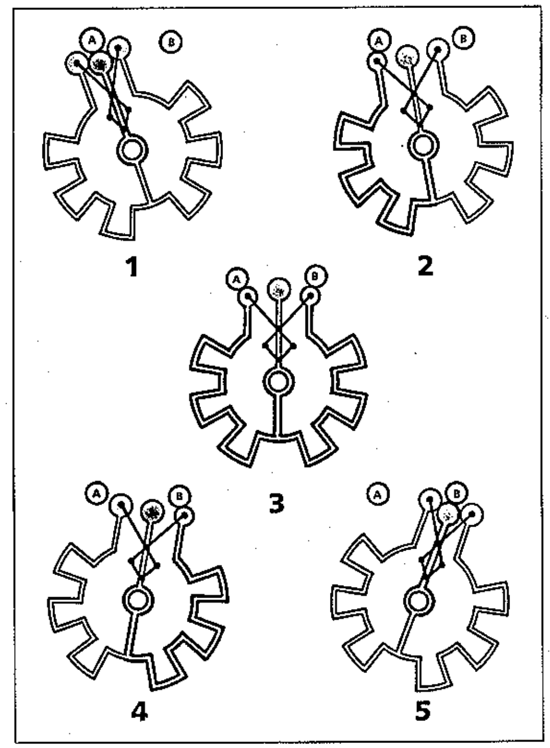


Figure 4.58 Typical schematic and sequence diagram of a single resistor-type tapchanger

tap position 2, the load current still passing to the neutral via tap position 1 but a circulating current now flows from tap position 2 to tap position 1. Diagram (*c*) now shows the position when the main arcing contact has left tap position 1, and it should be noted that the current interrupted by the opening of these contacts is the difference between I_L and I_c, the load and the circulating currents. The recovery voltage between the moving arc contact is the step

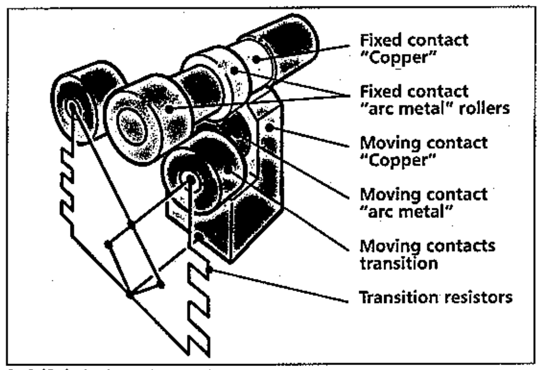


Switching sequence.

SWITCHING SEQUENCE

1. Shows in diagrammatic form the static condition of the tap-changer with the load current carried by copper fixed and moving contacts at tap A.
2. Current has been transferred from the main contact to the left-hand transition resistor arcing contact. The scissor action has increased the pitch between the two resistor moving contacts.
3. Right-hand transition resistor roller has made contact with tap B, establishing a circulating current between the two transition resistors.
4. Left-hand arcing contact has moved from tap A interrupting the circulating current and all power is carried through the right-hand transition resistor.
5. Tap-change is complete and power is carried through the main contact to tap B. The pitch of the transition resistor contacts is restored.

The same switching sequence applies with power flow in either direction.



Detail of fixed and moving contact arrangement.

Figure 4.59 switching sequence for single compartment tapchanger (Associated Tapchangers) voltage minus I_LR, the voltage drop across the diverter resistance. The main arcing contact continues its movement until it too makes connection with the fixed arcing contact of tap position 2; when this is achieved the load current now flows to the neutral via tap position 2 and the transitional arcing contact moves to an open position.

There is a difference of function when moving from a higher voltage tapping position to a lower position and this is explained as follows. Diagram (*d*) is the normal operating position for tap 2. When a tap change is initiated the transitional arcing contact moves from its open position to tap position 2, the main arcing contact moves off towards tap position 1. When it leaves tap position 2 arcing takes place, the current interrupted is I_L, and the recovery voltage between the main arcing moving contacts and the tap position 2 is I_LR.

Diagram (*f*) shows the condition when the main moving arcing contact has made connection at tap position 1, load current flow is via the main winding and tap position 1 to the neutral. Circulating current flows from tap position 2 to tap position 1; thus when the transitional moving contact leaves tap position 2 the current interrupted is the circulating current and the recovery voltage is the step voltage.

Figure 4.59 shows the switching sequence for a single compartment tapchanger which uses double resistor switching. Diagram (*a*) shows the condition with the transformer operating on tap position 1 with the load current carried by fixed and moving contacts. The first stage of the transition to tap position 2 is shown in diagram (*b*). Current has been transferred from the main contact to the left-hand transition resistor arcing contact and flows via resistor R_1. The next stage is shown in diagram (*c*) in which the right-hand transition contact has made contact with the tap 2 position. Load current is now shared between resistors R_1 and R_2 which also carry the tap circulating current. In diagram (*d*) the left-hand arcing contact has moved away from tap 1 interrupting the circulating current and all load current is now carried through the transition resistor R_2. The tap change is completed by the step shown in diagram (*e*) in which main and transition contacts are all fully made on tap 2. A single compartment tapchanger utilising this arrangement is shown in *Figure 4.60*.

As indicated above, when the tapping range is large or the system voltage very high, thus producing a considerable voltage between the extreme tappings, it is an advantage to halve the length of the tapping winding and to introduce a reversing or transfer switch. This not only halves the number of tappings to be brought out from the main winding of the transformer but also halves the voltage between the ends of tapping selector switch as shown in *Figure 4.61*.

In diagram B the tapped portion of the winding is shown divided into nine sections and a further untapped portion has a length equal to 10 sections. In the alternative diagrams C and D a section of the transformer winding itself is reversed. The choice of the tapchanger employed will depend on the design of the transformer. In diagrams A, B and C the tappings are shown at the neutral

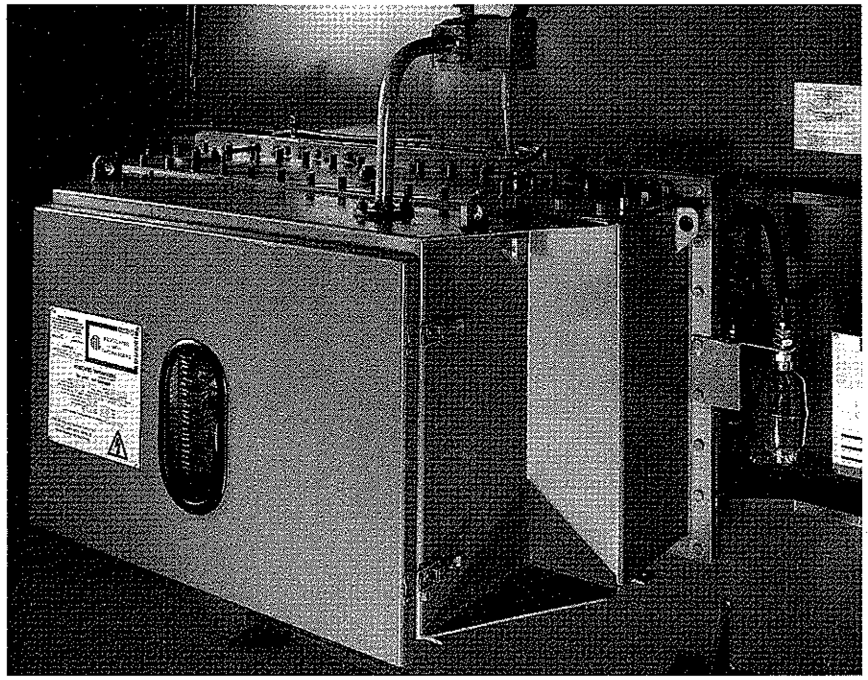


Figure 4.60(a) A small single compartment tapchanger suitable for 300 A, 44 kV, 66 kV and 132 kV applications (Associated Tapchangers)

end of the star-connected winding and in diagram D the tapchanger is shown connected to an autotransformer with reversing tappings at the line end of the winding.

In the three examples where a changeover selector is shown, the tapping selectors are turned through two revolutions, one revolution for each position of the changeover selectors, thus with the circuits shown 18 voltage steps would be provided.

As also mentioned previously variation of impedance over the tapping range can often be reduced by the use of reversing arrangements or the coarse/fine switching circuits described earlier.

The working levels of voltage and the insulation test levels to which the tapping windings and thus the on-load tapchanger are to be subjected will have a great deal of bearing on the type of tapchanger selected by the transformer designer. It will be readily appreciated that a tapchanger for use at the line end of a transformer on a 132 kV system will be a very different type of equipment from an on-load tapchanger for use at the grounded neutral end of a star-connected 132 kV winding. The test levels to which both of these on-load tapchangers are likely to be subjected vary considerably as shown by the values given in *Table 4.1*.

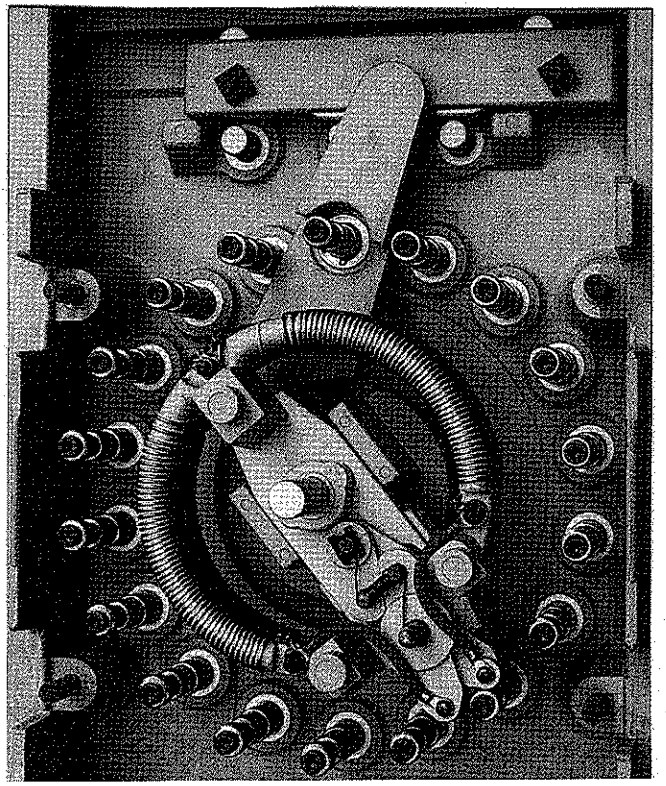


Figure 4.60(b) 1 phase of moving selector switch assembly for above tapchanger showing scissor contact mechanism and change over selector (at top) for coarse/fine or reversing regulation. (Associated Tapchangers)

The test figures for the 132 kV line end as taken from the insulation test levels (line end) for windings and connected parts designed for impulse voltage tests given in IEC 76 are given in *Table 4.1*.

Figures 4.62 and 4.63(a) indicate the basic difference due to the insulation requirements between an earthed neutral end tapchanger for a 132 kV system compared with a line end tapchanger for the same voltage. In *Figure 4.62* the selectors are in the compartment which runs along the side of the transformer and the diverter switch compartment is mounted at the end of the selector compartment. Examination of *Figure 4.63(a)* illustrates a 240 MVA, 400/132 kV three-phase autotransformer with three individual 132 kV line end on-load tapchangers. The selector bases are mounted on the transformer tank and the diverter switches are contained in the tanks which are mounted on the top of the 132 kV bushings. *Figure 4.63(b)* is a cross-sectional view of the tapchanger illustrated in *Figure 4.63(a)*. The main tank housing the selector switches are arranged for bolting to the transformer tank together with the

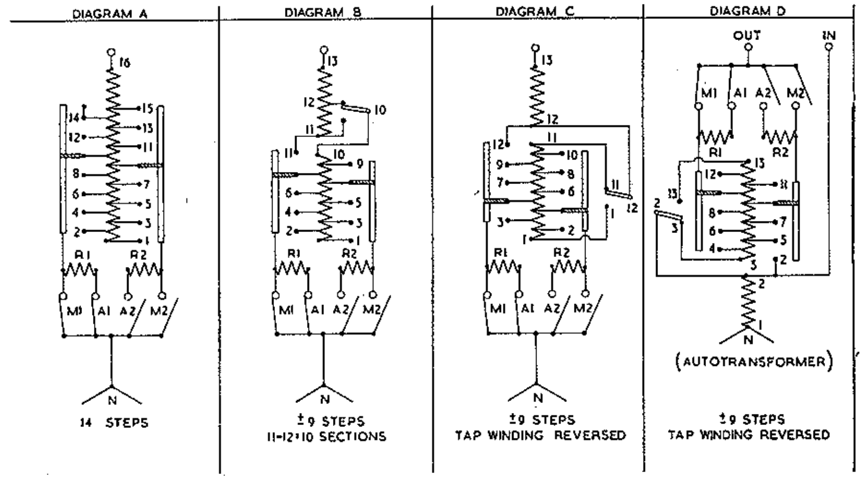


Figure 4.61 On-load tap changing circuits for resistor transition using diverter switches

Table 4.1

System highest voltage	*Insulation level*			
	Impulse test voltage (kV peak)		*Power frequency test voltage (kV rms)*	
kV rms	Standard 1	Standard 2	Standard 1	Standard 2
123	550	450	230	185
145	650	550	275	230

The Former British Electricity Boards Specification for tap changing specifies the following insulation levels:

Application	*Uniform (fully insulated)*	*Non-uniform (neutral end)*
Nominal system voltage between phases kV	132	132
Routine withstand to earth. 1 min power freq. kV	265	45
Minimum impulse withstand (1/50 wave) kV peak	640	110

terminal barrier board. Mounted directly on this tank is the porcelain bushing which supports the high-speed diverter switch assembly.

The main supporting insulation is a resin-bonded paper cylinder mounted at the base of the selector tank, and the mechanical drive is via a torsional porcelain insulator within this cylinder. Connections from the selector switches to the diverter switch are made by means of a double concentric condenser bushing and the mechanical drive shaft passes through the centre of this bushing.

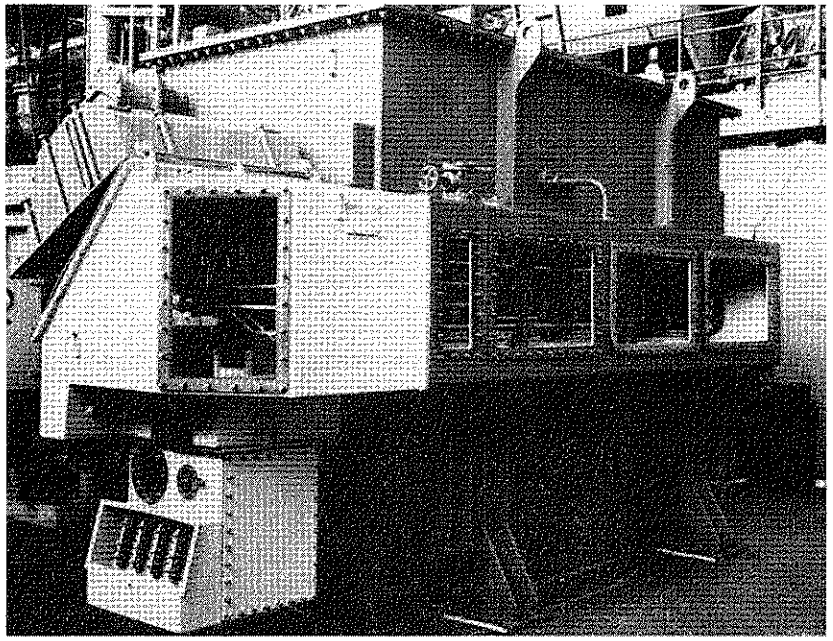


Figure 4.62 Three-phase, HV earthed neutral end, 132 kV tapchanger (Ferranti Engineering Ltd)

On the UK Grid System there are many 275/132 kV and 400/132 kV autotransformers installed where the on-load tapchangers are at the 132 kV point of the auto-winding. Earlier designs employed a reversing arrangement as shown in *Figure 4.64(a)* utilising a separate reversible regulating winding. More recently a linear arrangement has been used with the tapping sections of the winding forming part of the main winding as shown in *Figure 4.64(b)*.

In either case the tapping winding is usually a separate concentric winding. As mentioned earlier in this chapter, because of the high cost, particularly of the porcelain insulators required for the line end tapping arrangements, earthed neutral end tappings have also been used more particularly on the 400/132 kV autotransformers despite the fact that this introduces simultaneous changes in the effective number of turns in both primary and secondary and also results in a variation in the core flux density. The arrangement also introduces the complication of variable tertiary voltages. The latter can be corrected by introduction of a tertiary booster fed from the tapping windings.

On-load tapchangers have to be designed to meet the surge voltages arising under impulse conditions. In earlier high-voltage tapchangers it was quite a common practice to fit non-linear resistors (surge diverters) across individual tappings or across a tapping range.

These non-linear resistors have an inherent characteristic whereby the resistance decreases rapidly as the surge voltage increases. In modern

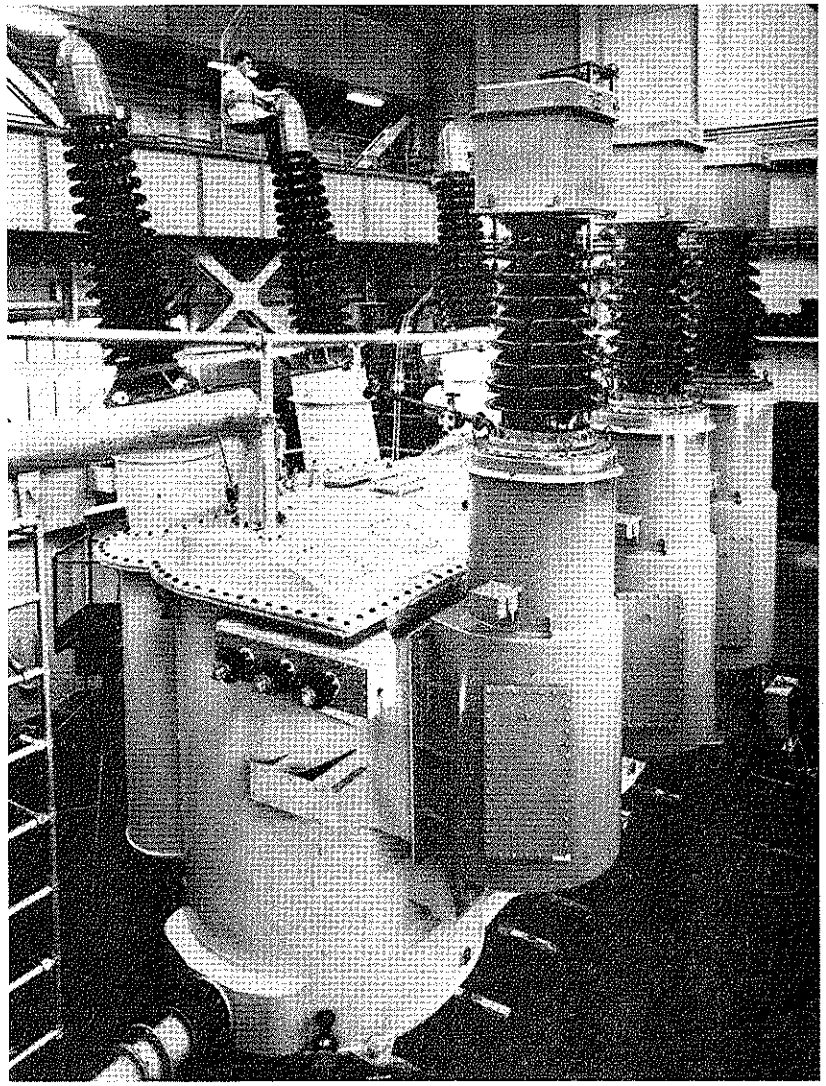


Figure 4.63(a) Three single-phase, fully insulated tapchangers fitted to the 132 kV tapping points of a 240 MVA, 400/132 kV autotransformer (Hawker Siddeley Power Transformers Ltd)

tapchangers this characteristic has, in general, been eliminated by improvement in design and positioning of contacts, such that appropriate clearances are provided where required. There is also now a much better understanding of basic transformer design and in particular the ways of improving surge voltage distribution to ensure that excessive values do not arise within tapping windings.

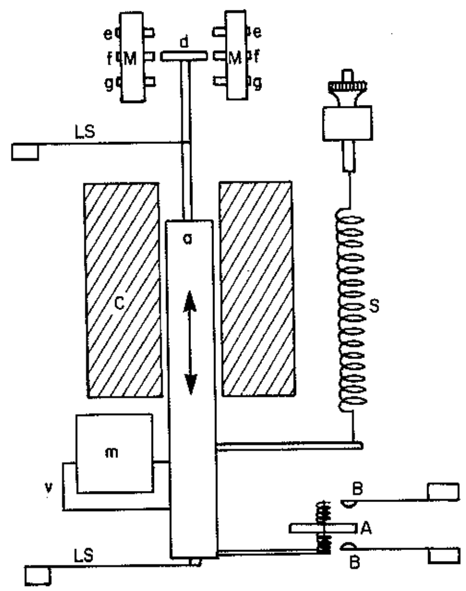


Figure 4.74 Simplified arrangement of the regulating element of the AVE 5 relay (GEC Measurements)

the weight of the core and the magnetic pull of the solenoid holding the disc 'd' at the mid-position 'f' with contact A between contacts B with nominal voltage applied. When the voltage increases or decreases the magnetic forces move the core up or down along the axis of the solenoid. The moving core carries a contact A which makes with the 'high-volt' and 'low-volt' fixed contacts B.

Positive action is ensured by the 'hold-on' device. This consists of an iron disc 'd' attached to the core, which moves between the poles of a permanent magnet M. Pole pieces 'e', 'f' and 'g' concentrate the flux of the permanent magnet, and therefore its influence on the disc 'd', at positions corresponding to high, normal and low positions of the core and tend to restrain it at these positions. An eddy-current damper consisting of fixed magnet 'm' and moving copper vane 'v' minimises oscillations set up by momentary voltage fluctuations.

To eliminate errors due to the variations of coil resistance with temperature, a comparatively high value of resistance having a negligible temperature coefficient is connected in series with the coil.

There is an advantage in providing means by which a sudden wide change in voltage can be more quickly corrected and solid-state voltage relays can provide this characteristic. These relays have a solid-state voltage-sensing circuit and an inverse time characteristic so that the delay is inversely proportional to the voltage change. Two such relays are the VTJC and STAR, both of which are illustrated in *Figure 4.75*. They can be used with a line drop

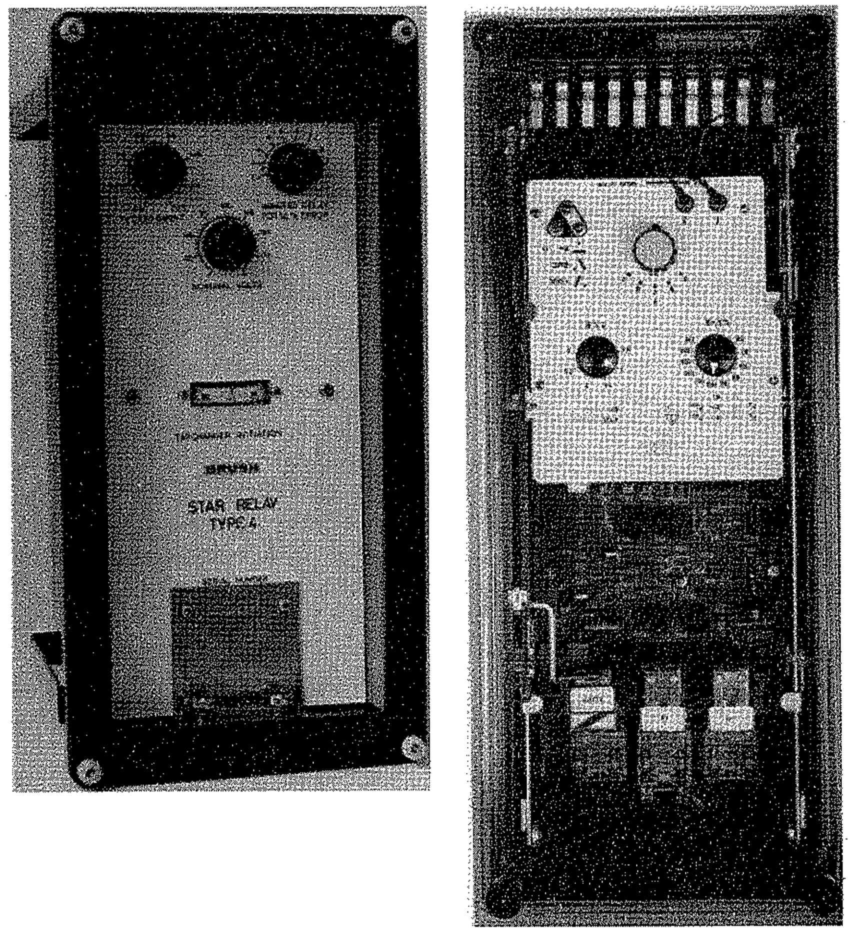


Figure 4.75 Static voltage regulating relays. (a) Brush STAR relay (Brush Electrical Machines Ltd); (b) GEC VTJC relay (GEC Measurements)

compensator and a voltage reduction facility to give specified load shedding features.

Where two or more transformers with automatically controlled on-load tapchangers are operating in parallel, it is normally necessary to keep them either on the same tapping position or a maximum of one tap step apart. If transformers are operated in parallel on different tappings circulating currents will be set up and in general one step is the most that can be tolerated.

Many different schemes of parallel control have been devised, several of which are in regular use. If it is considered necessary that all transformers must operate on the same tapping this can be achieved by a master-follower system or by a simultaneous operation method.

Master/follower control

With this type of control system one of the units is selected as the master and the remaining units operate as followers. Built-in contacts in the on-load tapchanger mechanisms are connected so that once a tap change has been completed on the master unit each follower is initiated in turn from the interconnected auxiliary contacts to carry out the tap change in the same direction as that carried out by the master. A simplified schematic diagram of the master and follower circuit is shown in *Figure 4.76*. The disadvantage of master/follower schemes is their complexity, so that nowadays they are very seldom used.

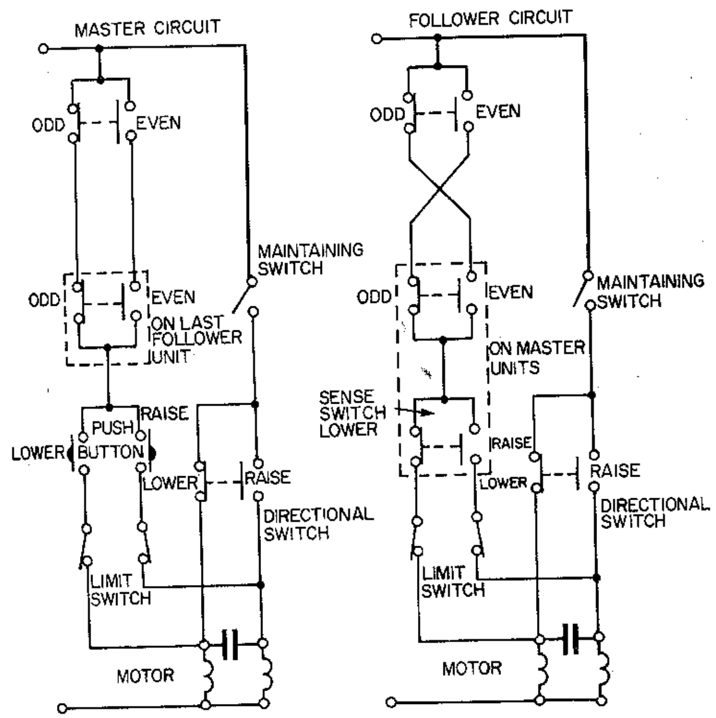


Figure 4.76 Master/follower circuit diagrams

With simultaneous operation, all tapchangers of a group are arranged to start their operation at the same time. This is a simpler arrangement although it is still necessary to provide lock-out arrangements to take care of any individual failure.

Circulating current control

Where two or more transformers of similar impedance are operated in parallel they will each provide an equal share of the load current. In the event of one of

these transformers changing to a higher tapping position, a circulating current will flow between this transformer and the remaining units. This circulating current will appear as a lagging current from the unit which has changed taps. It will be equally divided between the other transformers which are in parallel and will appear to these transformers as a leading current.

It is possible by judicious connection of current transformers to separate this circulating current from the load current and introduce it into components in the automatic voltage regulating (AVR) circuit. These are so connected into the AVR circuit such as to provide an additional voltage to the AVR which has tapped up and a subtractive voltage to the remaining AVRs controlling the parallel-connected transformers. Using this method and carefully adjusted components, transformers can be kept within close tapping positions of each other.

There has been much development in the supervisory control of system voltages, and on some systems centralised control has been achieved by the operations of tapchangers by remote supervisory methods. This is usually confined to supervisory remote pushbutton control, with an indication of the tapchanger position, but more complicated schemes have been installed and are being satisfactorily operated where tapchangers are controlled from automatic relays on their respective control panels, with supervisory adjustment of their preset voltage and selection of groups operating in parallel, and with all necessary indications reported back by supervisory means to the central control room.

Runaway prevention

The danger with any automatic voltage control scheme is that a fault in the control circuitry, either the voltage-sensing relay or, more probably, fuse failure of a voltage transformer, can cause a false signal to be given to the control equipment thus incorrectly driving it to one end of the range. Such a fault not only causes incorrect voltage to be applied to the system fed by the transformer but can also result in the transformer itself having an incorrect voltage applied to it. For example, the failure of a fuse of a voltage transformer monitoring the transformer low-voltage system will send a signal to the control scheme to raise volts. This will result in the transformer tapping down on the high-voltage side and it will continue to do so until it reaches the minimum tap position. The applied voltage on the transformer high-voltage side could, in fact, be at or near nominal, or even above nominal, so that this can result in the transformer being seriously overfluxed. Various schemes can be devised to guard against this condition, the most reliable being possible when two or more transformers are controlled in parallel. In this situation the AVC scheme outputs for each one can be compared. If they attempt to signal their respective transformer tapchangers to become more than two steps out of step then both schemes are locked out and an alarm given. All such schemes can only be as reliable as their input information and the principal requirement of any reliable scheme such as the one described must be that controls compared should operate from *independent* voltage transformer signals. Where the provision of an independent voltage transformer signal is difficult, as can be the case for a single transformer with on-load tapchanger supplying a tail-end feeder, it is possible to utilise a VT fuse monitoring relay. This usually compares phase voltages of the VT output and alarms if any one of these does not match the other two.

Moving coil regulator

The moving coil regulator does not suffer from the limitations of the on-load tapchangers finite voltage steps and has a wide range of application. It can be used in both low- and medium-voltage distribution systems, giving a smooth variable range of control. A shell-type core carries two coils connected in series opposition mounted vertically above the other. An outer third coil is short-circuited and mounted concentrically so that it can be moved vertically from a point completely covering the top coil to a lower position covering the bottom coil. This arrangement produces an output voltage proportional to the relative impedance between the fixed and moving coil which is smoothly variable over the range. *Figure 4.77* illustrates the principle of the moving coil regulator and the core and windings of two three-phase 50 Hz regulators are shown in *Figure 4.78*. They are designed for a variable input of 11 kV ± 15%, an output of 11 kV ± 1% and a throughput of 5 MVA.

The Brentford linear regulating transformer

The Brentford voltage regulating transformer is an autotransformer having a single layer coil on which carbon rollers make electrical contact with each successive turn of the winding. It can be designed for single- or three-phase operation and for either oil-immersed or dry-type construction. The winding is of the helical type which allows three-phase units to be built with a three-limb core as for a conventional transformer.

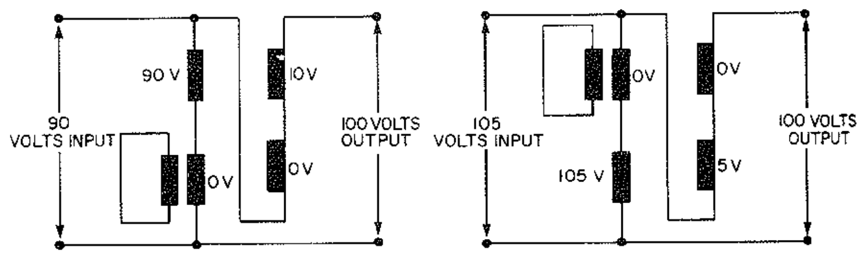


Figure 4.77 Principle of the moving coil type of voltage regulator

The helical winding permits a wide range of copper conductor sizes, winding diameter and length. The turns are insulated with glass tape and after winding the coils are varnish impregnated and cured. A vertical track is then machined

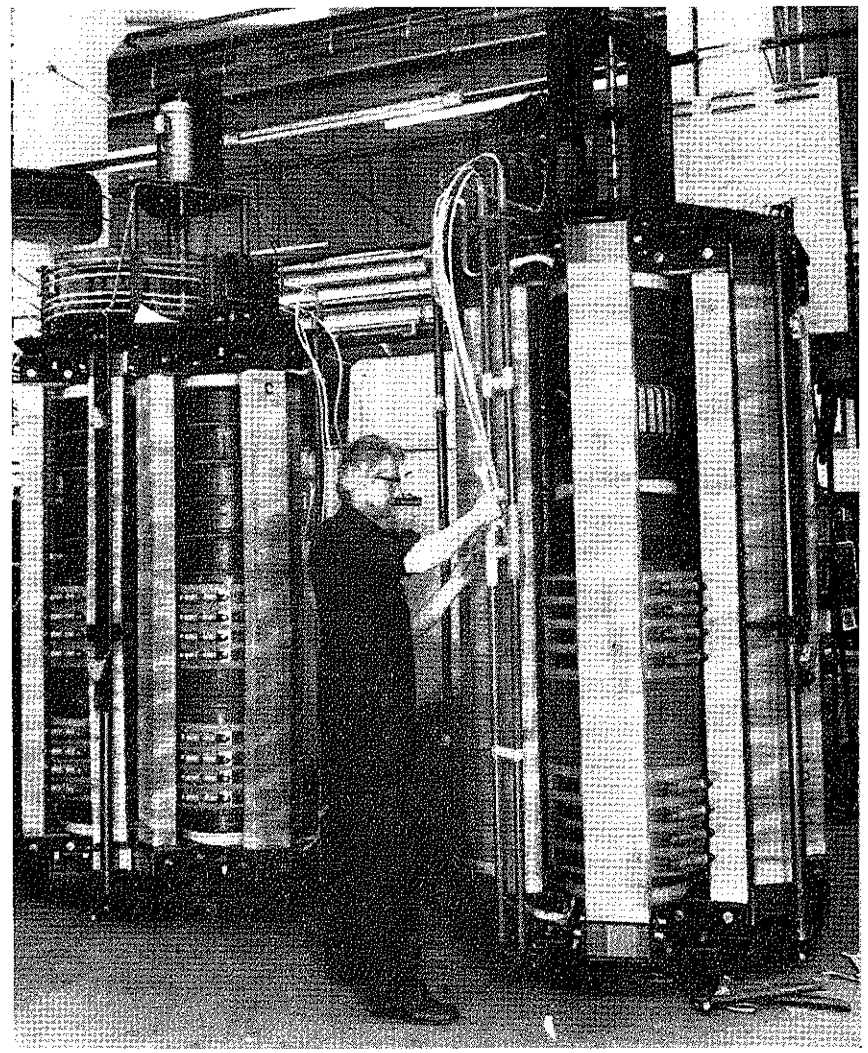


Figure 4.78 Core and coil assemblies of a three-phase, 5 MVA throughput 50 Hz moving coil regulator (Allenwest Brentford Ltd)

through the surface insulation to expose each turn of the winding. The chain driven carbon roller contacts supported on carriers operate over the full length of the winding to provide continuously variable tapping points for the output voltage.

As the contacts move they short-circuit a turn and a great deal of research has been carried out to obtain the optimum current and heat transfer conditions at the coil surface. These conditions are related to the voltage between adjacent turns and the composition of the material of the carbon roller contacts.

The short-circuit current does not affect the life of the winding insulation or the winding conductor. The carbon rollers are carried in spring-loaded, self-aligning carriers and rotate as they travel along the coil face. Wear is minimal and the rolling action is superior to the sliding action of brush contacts. In normal use the contact life exceeds 100 km of travel with negligible wear on the winding surface. *Figure 4.79* illustrates how the sensitivity of a regulator may be varied to suit a particular system application. If it is required to stabilise a 100 V supply which is varying by ±10% a voltage regulating transformer (VRT) would have say 100 turns so that by moving the roller contact from one turn to the next the output would change by 1 volt or 1%. However, if the VRT supplies a transformer which bucks or boosts 10% the roller contact needs to move 10 turns to change the voltage by 1%, hence the sensitivity of the regulator is increased 10 times.

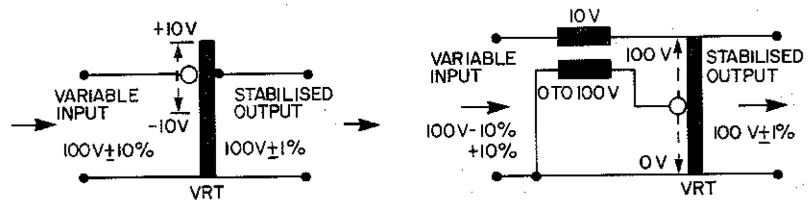


Figure 4.79 Diagram illustrating variation of sensitivity of a voltage regulating transformer

The contacts are easily removed for inspection by unscrewing the retaining plate and turning the contact assembly away from the coil face: contacts are then lifted vertically out of their carrier. Replacement is straightforward and with normal usage an operating life of three to five years can be expected.

Linear voltage regulators are available in ratings up to 1 MVA as a single frame and up to 15 MVA with multiple unit construction. Also on HV systems designs of regulators can be combined with on-load tapping selector switches connected to the transformer windings to provide power ratings in excess of 25 MVA.

Control of regulators over the operating range can be arranged for manual, pushbutton motor operation or fully automatic control regulating the output by means of a voltage-sensing relay.

Figure 4.80 shows the core and windings of a 72 kVA three-phase regulator designed for an input of 415 V 50 Hz and a stepless output of 0–415 V with a current over the range of 100 A. For the smaller low-voltage line–end boosters built into rural distribution systems, the regulator is often a single-sided equipment, and contact is made only to one side of the helical winding. For larger units, and those for networks up to 33 kV, the regulator is used in conjunction with series-booster and shunt-connected main transformers to give a wider range of power and voltage capabilities.

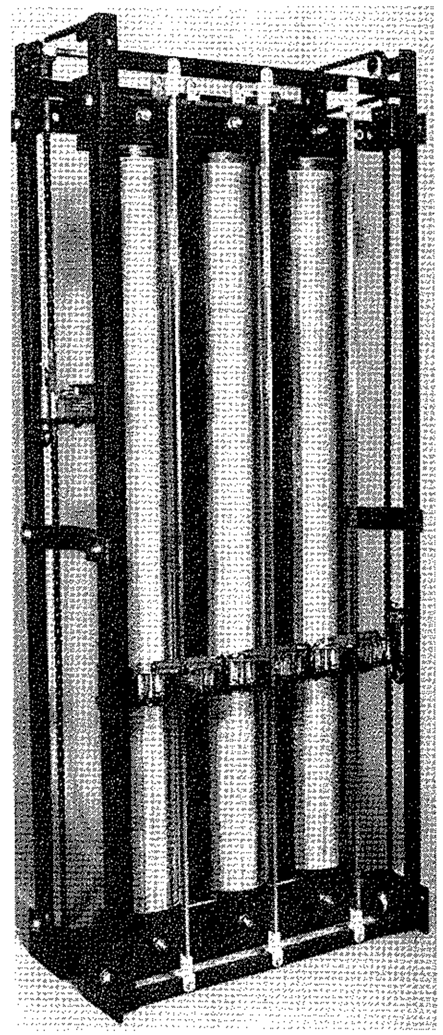


Figure 4.80 Three-phase, 100 A, 72 kVA, 415/0–415 V regulator (Allenwest Brentford Ltd)

The schematic diagram, *Figure 4.81*, shows the basic connection for an Interstep regulating equipment designed to provide stepless control of its output voltage from zero to 100%.

For the purposes of simplifying the explanation, the main transformer is auto-wound and provided with 8 tappings but depending upon the rating up to a maximum of 16 tappings can be used. Also for those applications where because of other considerations it is necessary to use a double wound transformer, it is often more economical for a restricted voltage range to utilise

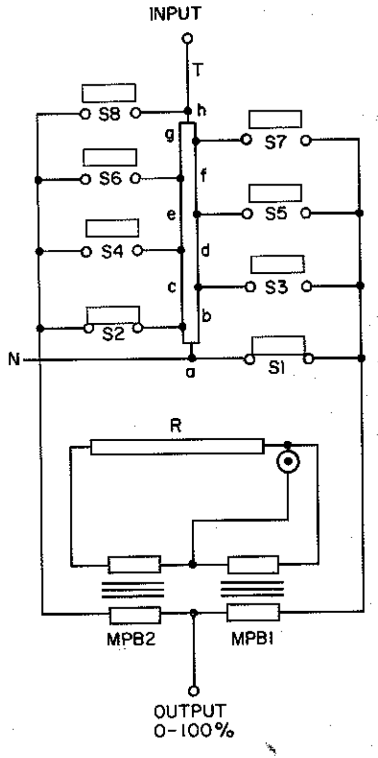


Figure 4.81 Schematic diagram of one phase of a three-phase Brentford interstep regulating unit employing an 8-position selector switch. Output voltage range is 0–100%. T – Main tapped transformer; R – Brentford stepless regulator; *MPB*1, *MPB*2 – double wound booster transformers connected in series; (a) to (h) – tappings on main transformer; *S*1 to *S*8 – 8-position selector switch

tappings on the primary winding, and not employ a separate tapped autotransformer.

Also provided in the equipment is a coordinating gear box which mechanically synchronises the operation of the switches and the regulator. The tappings on the autotransformer are connected to the selector switches *S*1 to *S*8 and the regulator and booster transformers are arranged to act as a trimming device between any two adjacent tappings. For example, if switches *S*1 and *S*2 are closed and the regulator is in the position shown, then the secondary winding of booster transformer *MPB*1 is effectively short-circuited and the voltage at the output terminal is equal to tap position (a), i.e. zero potential.

To raise the output voltage, the contact of the regulator is moved progressively across the winding and this action changes the voltage sharing of the two booster transformers until *MPB*2 is short-circuited and the output voltage

is equal to tap position (b). Under these conditions switch contact *S*1 can be opened, because effectively there is no current flowing through it and switch *S*3 can be closed.

To increase the output voltage further, the contact of the regulator is moved to the opposite end of the winding when *S*2 opens and *S*4 closes, and this procedure is repeated until the maximum voltage position is reached, which corresponds to switches *S*7 and *S*8 being closed with the secondary winding of *MPB*2 short-circuited. For this application the regulator is double sided, having both sides of the regulating winding in contact with the roller contacts and driven in opposite directions on either side of the coil to produce a 'buck' and 'boost' output from the regulator which is fed to the low-voltage side of the series transformer. The most common arrangement of this is shown diagrammatically in *Figure 4.82* but a number of alternative arrangements with patented 'double boost' connections are available.

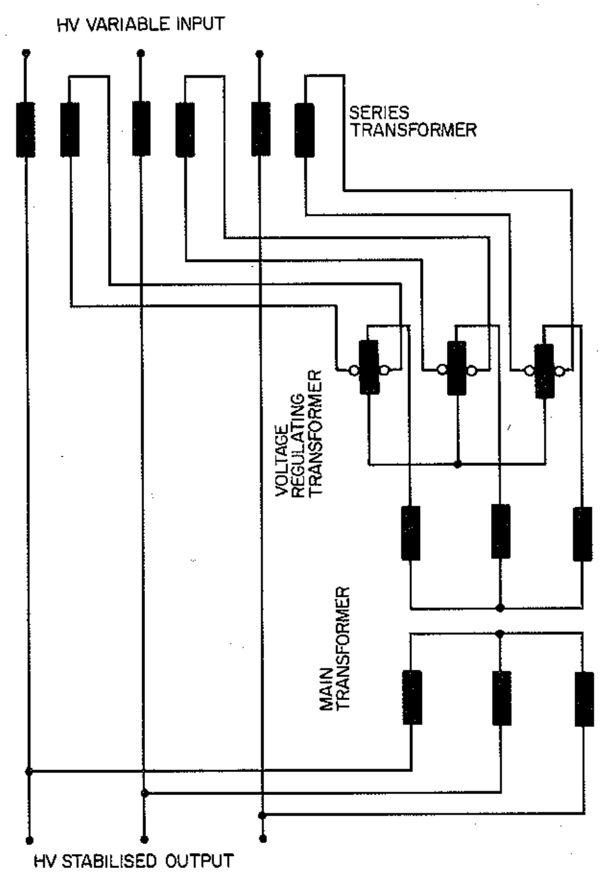


Figure 4.82 Diagram of connections of a regulator employed in conjunction with series and main transformers

4.7 WINDING FORCES AND PERFORMANCE UNDER SHORT-CIRCUIT

The effect of short-circuit currents on transformers, as on most other items of electrical plant, fall into two categories:

- Thermal effects.
- Mechanical effects.

Thermal effects

It is a fairly simple matter to deal with the thermal effects of a short-circuit. This is deemed to persist for a known period of time, BS 171, Part 5 specifies 2 s, allowing for clearance of the fault by back-up protection. During this brief time, it is a safe assumption that all the heat generated remains in the copper. Therefore knowing the mass of the copper, its initial temperature and the heat input, the temperature which it can reach can be fairly easily calculated. It simply remains to ensure that this is below a permitted maximum which for oil-immersed paper-insulated windings is taken to be 250°C, in accordance with Table III of BS 171, Part 5. Strictly speaking, the resistivity of the copper will change significantly between its initial temperature, which might be in the region of 115°C, and this permitted final temperature, and there is also some change in its specific heat over this temperature range; hence, a rigorous calculation would involve an integration with respect to time of the I^2R loss, which is increasing, plus the eddy-current loss, which is decreasing, divided by the copper weight times the specific heat, which is also increasing with temperature. In reality the likely temperature rise occurring within the permitted two seconds will fall so far short of the specified figure that an approximate calculation based on average resistivity and specific heat will be quite adequate. Current on short-circuit will be given by the expression:

$$F = \frac{1}{(e_z + e_s)} \tag{4.1}$$

where F = factor for short-circuit current as multiple of rated full-load current
e_z = *per unit* impedance of transformer
e_s = impedance of supply, *per unit*, expressed on the basis of the transformer rating

The supply impedance is normally quoted in terms of system short-circuit apparent power (fault level) rather than as a percentage. This may be expressed in percentage terms on the basis of the transformer rating in MVA as follows:

$$e_s = \frac{\text{MVA}}{S} \tag{4.2}$$

where S is the system short-circuit apparent power in MVA.

An approximate expression for the temperature rise of the conductor after t seconds is then:

$$\theta = \frac{t\left(1 + \frac{e}{100}\right) D^2 \rho F^2}{dh}$$

where θ = temperature rise in degrees centigrade
e = winding eddy-current loss, %
D = current density in windings, A/mm^2
ρ = resistivity of the conductor material
d = density of the conductor material
h = specific heat of the conductor material

For copper the density may be taken as 8.89 g/cm^3 and the specific heat as 0.397 J/g°C. An average resistivity value for fully cold-worked material at, say, 140°C may be taken as 0.0259 Ω mm^2/m. Substituting these and a value of t equal to 2 s in the above expression gives:

$$\theta = 0.0147\left(1 + \frac{e}{100}\right) D^2 F^2 \tag{4.3}$$

An indication of the typical magnitude of the temperature rise produced after 2 s can be gained by considering, for example, a 60 MVA, 132 kV grid transformer having an impedance of 13.5%. The UK 132 kV system can have a fault level of up to 5000 MVA. Using expression (4.2) this equates to 1.2% based on 60 MVA and inserting this together with the transformer impedance in expression (4.1) gives a short-circuit current factor of 6.8 times. A 60 MVA ODAF transformer might, typically, have a current density of up to 6 A/mm^2. The winding eddy-current losses could, typically, be up to 20%. Placing these values in expression (4.3) gives:

$$\theta = 0.0147\left(1 + \frac{20}{100}\right) 6^2 6.8^2 = 29.4°\text{C}$$

which is quite modest. With a hot spot temperature before the short-circuit of 125°C (which is possible for some designs of OFAF transformer in a maximum ambient of 40°C) the temperature at the end of the short-circuit is unlikely to exceed 155°C, which is considerably less than the permitted maximum.

The limiting factor for this condition is the temperature reached by the insulation in contact with the copper, since copper itself will not be significantly weakened at a temperature of 250°C. Although some damage to the paper will occur at this temperature, short-circuits are deemed to be sufficiently infrequent that the effect on insulation life is considered to be negligible. If the winding were made from aluminium, then this amount of heating of the conductor would not be considered acceptable and risk of distortion or creepage of the aluminium would be incurred, so that the limiting temperature for aluminium is restricted to 200°C.

Mechanical effects

Mechanical short-circuit forces are more complex. Firstly, there is a radial force which is a mutual repulsion between LV and HV windings. This tends to crush the LV winding inwards and burst the HV winding outwards. Resisting the crushing of the LV winding is relatively easy since the core lies immediately beneath and it is only necessary to ensure that there is ample support, in the form of the number and width of axial strips, to transmit the force to the core. The outwards bursting force in the HV winding is resisted by the tension in the copper, coupled with the friction force produced by the large number of HV turns which resists their slackening off. This is usually referred to as the 'capstan effect'. Since the tensile strength of the copper is quite adequate in these circumstances, the outward bursting force in the HV winding does not normally represent too serious a problem either. An exception is any outer winding having a small number of turns, particularly if these are wound in a simple helix. This can be the case with an outer tapping winding or sometimes the HV winding of a large system transformer where the voltage is low in relation to the rating. Such a transformer will probably have a large frame size, a high volts per turn and hence relatively few turns on both LV and HV. In these situations it is important to ensure that adequate measures are taken to resist the bursting forces under short-circuit. These might involve fitting a tube of insulation material over the winding or simply securing the ends by means of taping, not forgetting the ends of any tapping sections if included. Another alternative is to provide 'keeper sticks' over the outer surface of the coil which are threaded through the interturn spacers. Such an arrangement is shown in *Figure 4.83* in which keeper sticks are used over the helical winding of a large reactor.

Secondly, there may also be a very substantial axial force under short-circuit. This has two components. The first results from the fact that two conductors running in parallel and carrying current in the same direction are drawn together, producing a compressive force. This force arises as a result of the flux produced by the conductors themselves. However, the conductors of each winding are also acted upon by the leakage flux arising from the conductors of the other winding. As will be seen by reference to *Figure 4.84(a)*, the radial component of this leakage flux, which gives rise to the axial force, will in one direction at the top of the leg and the other direction at the bottom. Since the current is in the same direction at both top and bottom this produces axial forces in opposite directions which, if the primary and secondary windings are balanced so that the leakage flux pattern is symmetrical, will cancel out as far as the resultant force on the winding as a whole is concerned. Any initial magnetic unbalance between primary and secondary windings, i.e. axial displacement between their magnetic centres (*Figure 4.84(b)*) will result in the forces in each half of the winding being unequal, with the result that there is a net axial force tending to increase the displacement even further.

In very large transformers the designer aims to achieve as close a balance as possible between primary and secondary windings in order to limit these

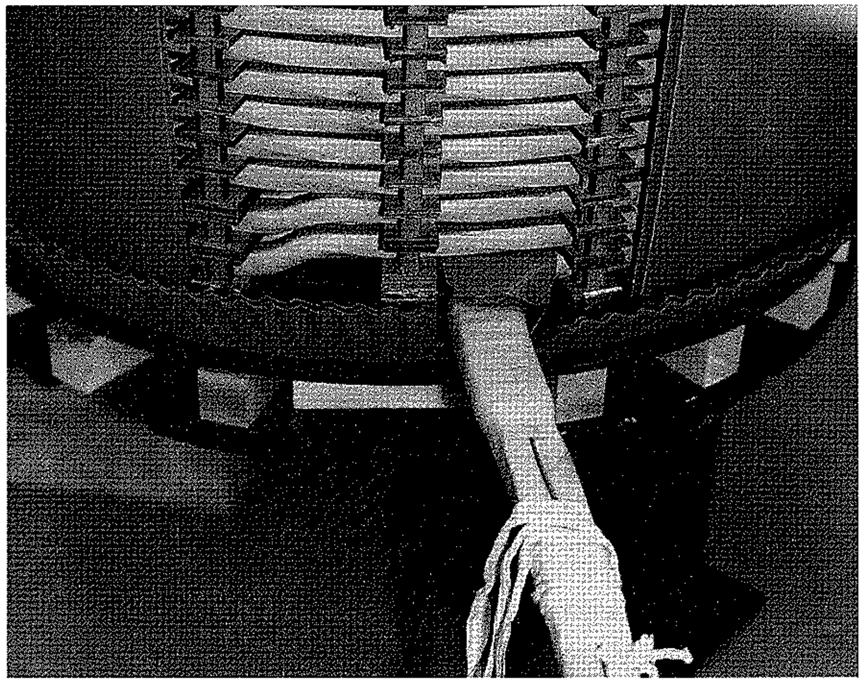


Figure 4.83 Part of a winding of a saturated reactor showing detail of external bracing (GEC Alsthom)

axial forces and he will certainly aim to ensure that primary and secondary windings as a whole are balanced, but complete balance of all elements of the winding cannot be achieved entirely for a number of reasons. One is the problem of tappings. Putting these in a separate layer so that there are no gaps in the main body of the HV when taps are not in circuit helps to some extent. However, there will be some unbalance unless each tap occupies the full winding length in the separate layer. One way of doing this would be to use a multistart helical tapping winding but, as mentioned above, simple helical windings placed outside the HV winding would be very difficult to brace against the outward bursting force. In addition spreading the tapping turns throughout the full length of the layer would create problems if the HV line lead were taken from the centre of the winding. Another factor which makes it difficult to obtain complete magnetic balance is the dimensional accuracy and stability of the materials used. Paper insulation and pressboard in a large winding can shrink axially by several centimetres during dry-out and assembly of the windings. Although the manufacturer can assess the degree of shrinkage expected fairly accurately, and will attempt to ensure that it is evenly distributed, it is difficult to do this with sufficient precision to ensure complete balance.

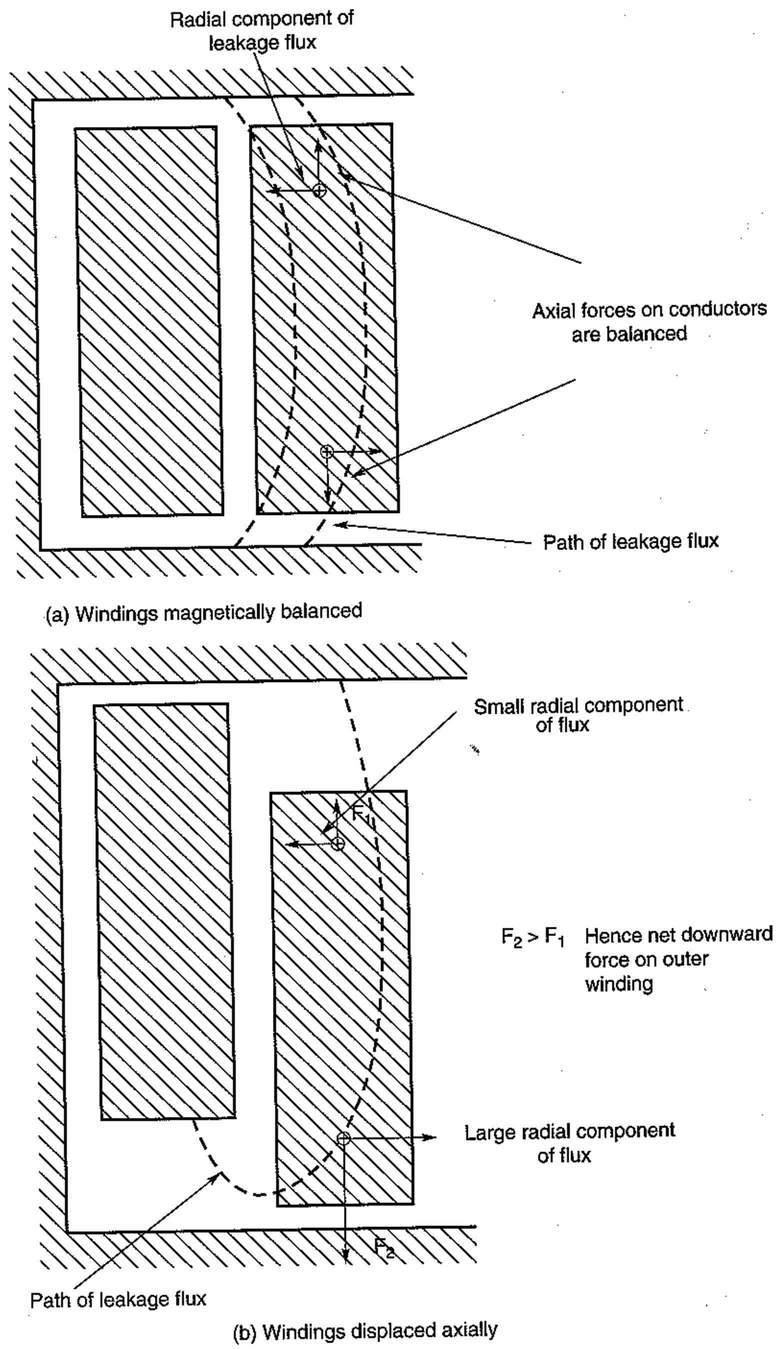


Figure 4.84 Forces within windings

Furthermore, shrinkage of insulation continues to occur in service and, although the design of the transformer should ensure that the windings remain in compression, it is more difficult to ensure that such shrinkage will be uniform. With careful design and manufacture the degree of unbalance will be small. Nevertheless it must be remembered that short-circuit forces are proportional to current squared and that the current in question is the initial peak asymmetrical current and not the r.m.s. value. Considering the 60 MVA transformer of the previous example for which the r.m.s. short-circuit current was calculated as 6.8 times full-load current. BS 171, Part 5, lists in Table V values of asymmetry factor, $k\sqrt{2}$, against X/R ratio for the circuit. These are reproduced in *Table 4.2*. For most grid transformer circuits this is likely to fall into the greater than or equal to 14 category, so that $k\sqrt{2}$ is 2.55. Thus the first peak of the current is $2.55 \times 6.8 = 17.34$ times full-load current. Force is proportional to the square of this, i.e. over 300 times that occurring under normal full-load current conditions.

Table 4.2 Values of factor $k\sqrt{2}$

X/R	1	1.5	2	3	4	5	6	8	10	≥14
$k\sqrt{2}$	1.51	1.64	1.76	1.95	2.09	2.19	2.27	2.38	2.46	2.55

Note. For other values of X/R between 1 and 14, the factor $k\sqrt{2}$ may be determined by linear interpolation.

Expressing the above in general terms, the first peak of the short-circuit current will be:

$$\hat{I}_{sc} = \frac{k\sqrt{2}\ \text{MVA}\ 10^6}{\sqrt{3}V(e_z + e_s)} \text{amperes} \quad (4.4)$$

where $k\sqrt{2}$ is the asymmetry factor
MVA is the transformer rating in mega voltamperes
V is the transformer rated voltage in volts

Axial forces under short-circuit are resisted by transmitting them to the core. The top and bottom core frames incorporate pads which bear on the ends of the windings, these pads distributing the load by means of heavy-section pressboard or compressed laminated-wood platforms. The top and bottom core frames, in turn, are linked together by steel tie-bars which have the dual function of resisting axial short-circuit forces and ensuring that when the core and coils are lifted via the top core frames on the assembly, the load is supported from the lower frames. These tie-bars can be seen in *Figure 4.7* which shows a completed core before fitting of the windings.

Calculation of forces

The precise magnitude of the short-circuit forces depends very much upon the leakage flux pattern, and the leakage flux pattern also determines such important parameters as the leakage reactance and the magnitude of the stray losses. Manufacturers nowadays have computer programs based on finite element analysis which enable them to accurately determine the leakage flux throughout the windings. These computer programs can be very simply extended for the calculation of short-circuit forces to enable manufacturers to accurately design for these. Occasionally, however, it might be necessary to make a longhand calculation and in this case the following, which is based on an ERA Report Ref. Q/T134, 'The measurement and Calculation of Axial Electromagnetic Forces in Concentric Transformer Windings', by M. Waters, BSc, FIEE., and a paper with the same title published in the *Proceedings of the Institution of Electrical Engineers*, Vol. 10, Part II, No. 79, February 1954, will be of assistance.

Short-circuit currents

The calculations are based on the first peak of short-circuit current derived in expression (4.4) above.

The limb current I_{max} corresponding to this value is used in force calculations.

The impedance voltage e_z is dependent upon the tapping position, and to calculate the forces accurately it is necessary to use the value of impedance corresponding to the tapping position being considered. For normal tapping arrangements the change in the percentage impedance due to tappings is of the order of 10%, and if this is neglected the force may be in error by an amount up to ±20%.

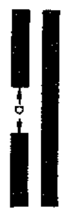
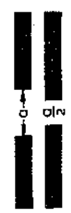
For preliminary calculations, or if a margin of safety is required, the minimum percentage impedance which may be obtained on any tapping should be used, and in the case of tapping arrangements shown in column one of *Table 4.3* this corresponds to the tapping giving the best balance of ampere-turns along the length of the limb. However, in large transformers, where a good ampere-turn balance is essential to keep the forces within practical limits, the change in percentage impedance is small and can usually be neglected.

When calculating forces the magnetising current of the transformer is neglected, and the primary and secondary windings are assumed to have equal and opposite ampere-turns. All forces are proportional to the square of the ampere-turns, with any given arrangement of windings.

Mechanical strength

It has been suggested by other authors that the mechanical strength of a power transformer should be defined as the ratio of the r.m.s. value of the symmetrical short-circuit current to the rated full-load current. The corresponding stresses which the transformer must withstand are based upon the peak value of the short-circuit current assuming an asymmetry referred to earlier. A transformer

Table 4.3

Arrangement of tappings	Residual ampere-turn diagram	P_A, kN	$\Lambda \left(\frac{\text{Window height}}{\text{Core circle}}\right) = 4.2$	$\Lambda \left(\frac{\text{Window height}}{\text{Core circle}}\right) = 2.3$
A 		$\frac{2\pi a(NI_{max})^2\Lambda}{10^{10}}$	5.5	6.4
B 		$\frac{\pi a(NI_{max})^2\Lambda}{2\times 10^{10}}$	5.8	6.6
C 		$\frac{\pi a(NI_{max})^2\Lambda}{4(1-\frac{1}{2}a)\times 10^{10}}$	5.8	6.6
D 		$\frac{\pi a(NI_{max})^2\Lambda}{8\times 10^{10}}$	6.0	6.8
E 		$\frac{\pi a(NI_{max})^2\Lambda}{16(1-\frac{1}{2}a)\times 10^{10}}$	6.0	6.8

designed to withstand the current given by equation (4.4) would thus have a strength of $i/(e_z + e_s)$.

It will be appreciated that the strength of a transformer for a single fault may be considerably greater than that for a series of faults, since weakening of the windings and axial displacement may be progressive. Moreover, a transformer will have a mechanical strength equal only to the strength of the weakest component in a complex structure. Progressive weakening also implies a short-circuit 'life' in addition to a short-circuit strength. The problem of relating system conditions to short-circuit strength is a complex one and insufficient is yet known about it for definite conclusions to be drawn.

Radial electromagnetic forces

These forces are relatively easy to calculate since the axial field producing them is accurately represented by the simple two-dimensional picture used for reactance calculations. They produce a hoop stress in the outer winding, and a compressive stress in the inner winding.

The mean hoop stress σ_{mean} in the conductors of the outer winding at the peak of the first half-wave of short-circuit current, assuming an asymmetry factor of 2.55 and a supply impedance e_s is given by:

$$\hat{\sigma}_{\text{mean}} = \frac{0.031 W_{cu}}{h(e_z + e_s)^2} \text{ kN/mm}^2\text{(peak)} \qquad (4.5)$$

where $W_{cu} = I^2R_{dc}$ loss in the winding in kW at rated full load and at 75°C
h = axial height of the windings in mm

Normally this stress increases with the kVA per limb but it is important only for ratings above about 10 MVA per limb. Fully annealed copper has a very low mechanical strength and a great deal of the strength of a copper conductor depends upon the cold working it receives after annealing, due to coiling, wrapping, etc. It has been suggested that 0.054 kN/mm^2 represents the maximum permissible stress in the copper, if undue permanent set in the outer winding is to be avoided. For very large transformers, some increase in strength may be obtained by lightly cold working the copper or by some form of mechanical restraint. Ordinary high-conductivity copper when lightly cold worked softens very slowly at transformer temperatures and retains adequate strength during the life of an oil-filled transformer.

The radial electromagnetic force is greatest for the inner conductor and decreases linearly to zero for the outermost conductor. The internal stress relationship in a disc coil is such that considerable levelling up takes place and it is usually considered that the mean stress as given in equation (4.5) may be used in calculations.

The same assumption is often made for multilayer windings, when the construction is such that the spacing strips between layers are able to transmit the pressure effectively from one layer to the next. If this is not so then the stress in the layer next to the duct is twice the mean value.

Inner windings tend to become crushed against the core, and it is common practice to support the winding from the core and to treat the winding as a continuous beam with equidistant supports, ignoring the slight increase of strength due to curvature. The mean radial load per mm length of conductor of a disc coil is:

$$W = \frac{0.031\hat{\sigma}A_c}{D_w} \text{ kN/mm length}$$

or alternatively,

$$W = \frac{510U \times 1}{(e_z + e_s)fd_1\pi D_m N} \text{ kN/mm length} \qquad (4.6)$$

where A_c = cross-sectional area of the conductor upon which the force is required, mm^2
D_w = mean diameter of winding, mm
U = rated kVA per limb
f = frequency, Hz
$\hat{\sigma}$ = peak value of mean hoop stress, kN/mm^2, from equation (4.5)
d_1 = equivalent duct width, mm
D_m = mean diameter of transformer (i.e. of HV and LV windings), mm
N = number of turns in the winding

Equation (4.6) gives the total load per millimetre length upon a turn or conductor occupying the full radial thickness of the winding. In a multilayer winding with k layers the value for the layer next to the duct would be $(2k - 1)/k$ times this value, for the second layer $(2k - 3)/k$, and so on.

Where the stresses cannot be transferred directly to the core, the winding itself must be strong enough to withstand the external pressure. Some work has been carried out on this problem, but no method of calculation proved by tests has yet emerged. It has been proposed, however, to treat the inner winding as a cylinder under external pressure, and although not yet firmly established by tests, this method shows promise of being useful to transformer designers.

Axial electromagnetic forces

Forces in the axial direction can cause failure by producing collapse of the winding, fracture of the end rings or clamping system, and bending of the conductors between spacers; or by compressing the insulation to such an extent that slackness occurs which can lead to displacement of spacers and subsequent failure.

Measurement of axial forces

A simple method is available, developed by ERA Technology Limited (formerly the Electrical Research Association), for measuring the total axial force upon the whole or part of a concentric winding. This method does not indicate how the force is distributed round the circumference of the winding but this is only a minor disadvantage.

If the axial flux linked with each coil of a disc winding at a given current is plotted against the axial position, the curve represents, to a scale which can be calculated, the axial compression curve of the winding. From such a curve the total axial force upon the whole or any part of a winding may be read off directly.

The flux density of the radial component of leakage field is proportional to the rate of change of axial flux with distance along the winding. The curve of axial flux plotted against distance thus represents the integration of the radial flux density and gives the compression curve of the winding if the points of zero compression are marked.

The voltage per turn is a measure of the axial flux, and in practice the voltage of each disc coil is measured, and the voltage per turn plotted against the mid-point of the coil on a diagram with the winding length as abscissa. The method can only be applied to a continuous disc winding by piercing the insulation at each crossover.

The test is most conveniently carried out with the transformer short-circuited as for the copper-loss test.

The scale of force at 50 Hz is given by

$$1 \text{ volt (r.m.s.)} = \frac{\text{r.m.s. ampere-turns per mm}}{15\,750} \text{ kN (peak)}$$

To convert the measured voltages to forces under short-circuit conditions the values must be multiplied by $(2.55I_{sc}/I_t)^2$ where I_{sc} is the symmetrical short-circuit current and I_t the current at which the test is carried out.

To obtain the compression curve it is necessary to know the points of zero compression, and these have to be determined by inspection. This is not difficult since each arrangement of windings produces zero points in well-defined positions.

A simple mutual inductance potentiometer can be used instead of a voltmeter, and a circuit of this type is described in ERA Report, Ref. Q/T 113, the balance being independent of current and frequency.

Figure 4.85 shows typical axial compression curves obtained on a transformer having untapped windings of equal heights. There are no forces tending to separate the turns in the axial direction. The ordinates represent the forces between coils at all points, due to the current in the windings. Since the slope of the curve represents the force developed per coil it will be seen that only in the end coils are there any appreciable forces. The dotted curve, which is the sum of the axial compression forces for the inner and outer curves, has a maximum value given by:

$$P_c = \frac{510U}{(e_z + e_s)fh} \text{ kN} \qquad (4.7)$$

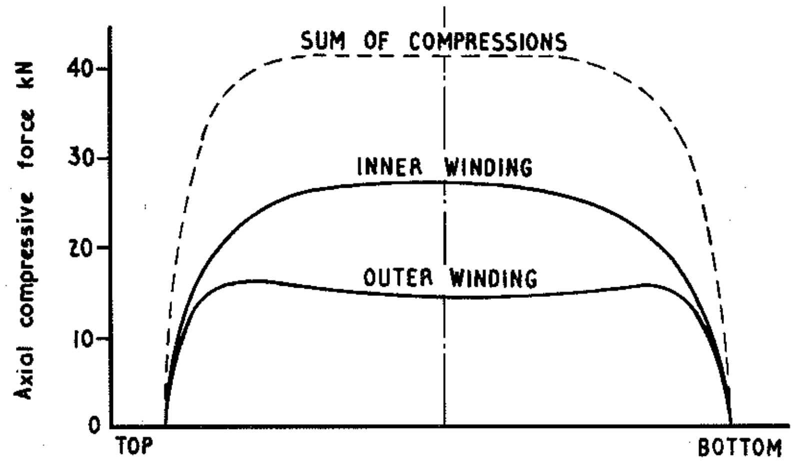


Figure 4.85 Axial compression curves for untapped transformer windings

in terms of U the rated kVA per limb and h the axial height of the windings in millimetres. This is the force at the peak of the first half cycle of fault current, assuming an asymmetry factor of 2.55.

The results shown in *Figure 4.85* and other similar figures appearing later in this chapter were obtained on a three-phase transformer constructed so that the voltage across each disc coil in both inner and outer coil stacks could easily be measured. To ensure very accurate ampere-turn balance along the whole length of the windings, primary and secondary windings consisted of disc coils identical in all respects except diameter, and spacing sectors common to both windings were used so that each disc coil was in exactly the same axial position as the corresponding coil in the other winding.

It will be noted that the forces in a transformer winding depend only upon its proportions and on the total ampere-turns, and not upon its physical size. Thus, model transformers are suitable for investigating forces, and for large units where calculation is difficult it may be more economical to construct a model and measure the forces than to carry out elaborate calculations.

The voltage per turn method has proved very useful in detecting small accidental axial displacements of two windings from the normal position.

Calculation of axial electromagnetic forces

The problem of calculating the magnitude of the radial leakage field and hence the axial forces of transformer windings has received considerable attention and precise solutions have been determined by various authors. These methods are complex and a computer is necessary if results are to be obtained quickly and economically. The residual ampere-turn method gives reliable results, and attempts to produce closer approximations add greatly to the complexity without a corresponding gain in accuracy. This method does not give the force on individual coils, but a number of simple formulae of reasonable accuracy are available for this purpose.

Residual ampere-turn method

The axial forces are calculated by assuming the winding is divided into two groups, each having balanced ampere-turns. Radial ampere-turns are assumed to produce a radial flux which causes the axial forces between windings.

The radial ampere-turns at any point in the winding are calculated by taking the algebraic sum of the ampere-turns of the primary and secondary windings between that point and either end of the windings. A curve plotted for all points is a residual or unbalanced ampere-turn diagram from which the method derives its name. It is clear that for untapped windings of equal length and without displacement there are no residual ampere-turns or forces between windings. Nevertheless, although there is no axial thrust between windings, internal compressive forces and forces on the end coils are present. A simple expedient enables the compressive forces present when the ampere-turns are balanced to be taken into account with sufficient accuracy for most design purposes.

The method of determining the distribution of radial ampere-turns is illustrated in *Figure 4.86* for the simple case of a concentric winding having a fraction a of the total length tapped out at the end of the outer winding. The two components I and II of *Figure 4.86(b)* are both balanced ampere-turn groups which, when superimposed, produce the given ampere-turn arrangement. The diagram showing the radial ampere-turns plotted against distance along the winding is a triangle, as shown in *Figure 4.86(c)*, having a maximum value of $a(NI_{max})$, where (NI_{max}) represents the ampere-turns of either the primary or secondary winding.

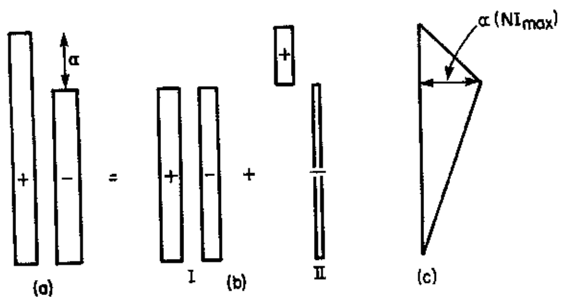


Figure 4.86 Determination of residual ampere-turn diagram for winding tapped at one end

To determine the axial forces, it is necessary to find the radial flux produced by the radial ampere-turns or, in other words, to know the effective length of

path for the radial flux for all points along the winding. The assumption is made that this length is constant and does not vary with axial position in the winding. Tests show that this approximation is reasonably accurate, and that the flux does, in fact, follow a triangular distribution curve of a shape similar to the residual ampere-turn curve.

The calculation of the axial thrust in the case shown in *Figure 4.86* can now be made as follows. If l_{eff} is the effective length of path for the radial flux, and since the mean value of the radial ampere-turns is $\frac{1}{2}a(NI_{max})$, the mean radial flux density at the mean diameter of the transformer is:

$$B_r = \frac{4\pi}{10^4}\frac{a(NI_{max})}{2l_{eff}} \text{ teslas}$$

and the axial force on either winding of NI_{max} ampere-turns is:

$$P_A = \frac{2\pi a(NI_{max})^2}{10^{10}}\frac{\pi D_m}{l_{eff}} \text{ kN} \tag{4.8}$$

The second factor of this expression, $\pi D_m/l_{eff}$, is the permeance per unit axial length of the limb for the radial flux, referred to the mean diameter of the transformer. It is independent of the physical size of the transformer and depends only upon the configuration of the core and windings. Forces are greatest in the middle limb of a three-phase transformer, and therefore the middle limb only need be considered. A review of the various factors involved indicates that the forces are similar in a single-phase transformer wound on two limbs. Thus if equation (4.8) is written as:

$$P_A = \frac{2\pi a(NI_{max})^2}{10^{10}}\Lambda \text{ kN} \tag{4.9}$$

where $\Lambda = \pi D_m/l_{eff}$ and is the permeance per unit axial length of limb, it gives the force for all transformers having the same proportions whatever their physical size. Since the ampere-turns can be determined without difficulty, in order to cover all cases it is necessary to study only how the constant Λ varies with the proportions of the core, arrangement of tappings, dimensions of the winding duct and proximity of tank.

Reducing the duct width increases the axial forces slightly, and this effect is greater with tapping arrangements which give low values of residual ampere-turns. However, for the range of duct widths used in practice the effect is small.

Where the equivalent duct width is abnormally low, say less than 8% of the mean diameter, forces calculated using the values given in *Table 4.3* should be increased by approximately 20% for tappings at two points equidistant from the middle and ends, and 10% for tappings at the middle. The axial forces are also influenced by the clearance between the inner winding and core. The closer the core is to the windings, the greater is the force.

The effect of tank proximity is to increase Λ in all cases, and for the outer limbs of a three-phase transformer by an appreciable amount; however, the middle limb remains practically unaffected unless the tank sides are very close to it. As would be expected, the presence of the tank has the greatest effect for tappings at one end of the winding, and the least with tappings at two points equidistant from the middle and ends of the winding. As far as limited tests can show, the presence of the tank never increases the forces in the outer limbs to values greater than those in the middle limb, and has no appreciable effect upon the middle limb with practical tapping arrangements. The only case in which the tank would have appreciable effect is in that of a single-phase transformer wound on one limb, and in this case the value of Λ would again not exceed that for the middle phase of a three-phase transformer.

The location of the tappings is the predominating influence on the axial forces since it controls the residual ampere-turn diagram. Forces due to arrangement E in *Table 4.3* are only about one-thirty-second of those due to arrangement A. The value of Λ is only slightly affected by the arrangement of tappings so that practically the whole of the reduction to be expected from a better arrangement of tappings can be realised. It varies slightly with the ratio of limb length to core circle diameter, and also if the limbs are more widely spaced.

In *Table 4.3* values of Λ are given for the various tapping arrangements and for two values of the ratio, window height/core circle diameter. The formula for calculating the axial force on the portion of either winding under each triangle of the residual ampere-turn diagram is given in each case. The values of Λ apply to the middle limb with three-phase excitation, and for the tapping sections in the outer winding.

Axial forces for various tapping arrangements

Additional axial forces due to tappings can be avoided by arranging the tappings in a separate coil so that each tapping section occupies the full winding height. Under these conditions there are no ampere-turns acting radially and the forces are the same as for untapped windings of equal length. Another method is to arrange the untapped winding in a number of parallel sections in such a way that there is a redistribution of ampere-turns when the tapping position is changed and complete balance of ampere-turns is retained.

(i) Transformer with tappings at the middle of the outer winding

To calculate the radial field the windings are divided into two components as shown in *Figure 4.87*. Winding group II produces a radial field diagram as shown in (*c*). The two halves of the outer winding are subjected to forces in opposite directions towards the yokes while there is an axial compression of similar magnitude at the middle of the inner winding.

Measured curves are given in *Figure 4.88* for the case of $13\frac{1}{3}\%$ tapped out of the middle of the outer winding. The maximum compression in the outer winding is only slightly greater than the end thrust, and it occurs at four to five coils from the ends. The maximum compression in the inner winding is at the middle.

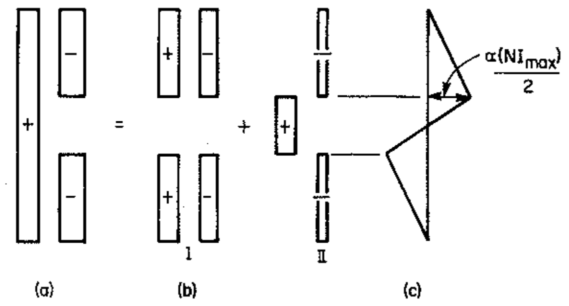


Figure 4.87 Determination of residual ampere-turn diagram for winding tapped at middle

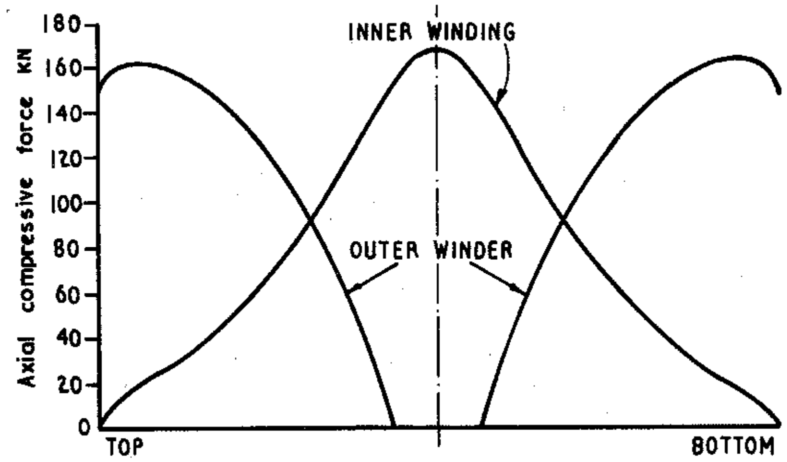


Figure 4.88 Axial compression curves for $13\frac{1}{3}\%$ tapped out of the middle of the outer winding

Axial end thrust The axial end thrust is given by:

$$P_A = \frac{\pi a (NI_{\max})^2 \Lambda}{2 \times 10^{10}}\ \text{kN} \tag{4.10}$$

Maximum compression If P_c is the sum of both compressions as given by equation (4.9) and it is assumed that two-thirds of this is the inner winding, then the maximum compression in the inner winding is given by:

$$P_{max} = \frac{2}{3} \times \frac{51U}{(e_z + e_s)fh} + \frac{\pi a(NI_{max})^2 \Lambda}{2 \times 10^{10}} \text{ kN} \tag{4.11}$$

The maximum compression in the outer winding is slightly less than this.

Figure 4.89 shows curves of maximum compression in the inner and outer windings, and of end thrust plotted against the fraction of winding tapped out for the same transformer. Equation (4.10) represents the line through the origin.

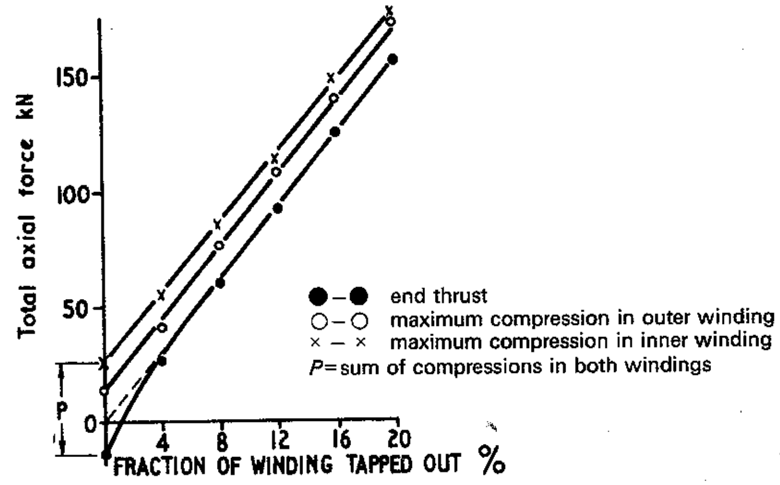


Figure 4.89 Curves of end thrust and maximum compression for tappings at the middle of the outer winding

Most highly stressed turn or coil The largest electromagnetic force is exerted upon the coils immediately adjacent to the tapped out portion of a winding and it is in these coils that the maximum bending stresses occur when sector spacers are used. The force upon a coil or turn in the outer winding immediately adjacent to the gap is given theoretically by:

$$P_A = 7.73qP_r \log_{10}\left(\frac{2a'}{w} + 1\right) \text{ kN} \tag{4.12}$$

where P_r = total radial bursting force of transformer, kN
q = fraction of total ampere-turns in a coil or winding
w = axial length of coil including insulation, mm
a' = axial length of winding tapped out

There is reasonable agreement between calculated and measured forces; the calculated values are 10–20% high, no doubt owing to the assumption that the windings have zero radial thickness.

The coils in the inner winding exactly opposite to the most highly stressed coils in the outer winding have forces acting upon them of a similar, but rather lower, magnitude.

(ii) Tappings at the middle of the outer winding but with thinning of the inner winding

The forces in the previous arrangement may be halved by thinning down the ampere-turns per unit length to half the normal value in the portion of the untapped winding opposite the tappings. Alternatively, a gap may be left in the untapped winding of half the length of the maximum gap in the tapped winding. With these arrangements there is an axial end thrust from the untapped winding when all the tapped winding is in circuit, and an end thrust of similar magnitude in the tapped winding when all the tappings are out of circuit. In the mid-position there are no appreciable additional forces compared with untapped windings.

(a) Axial end thrust When all tappings are in circuit the end thrust of the untapped winding may be calculated by means of equation (4.10), substituting for a the fractional length of the gap in the untapped winding. When all tappings are out of circuit the end thrust is given by:

$$P_A = \frac{\pi a(NI_{max})^2 \Lambda}{4(1 - \frac{1}{2}a)10^{10}} \text{ kN} \tag{4.13}$$

where a, the fraction of the axial length tapped out, is partially compensated by a length $\frac{1}{2}a$ omitted from the untapped winding. The constant Λ has the same value as in equation (4.10). The forces are similar when the ampere-turns are thinned down instead of a definite gap being used.

(b) Maximum compression In either of the two preceding cases the maximum compression exceeds the end thrust by an amount rather less than the force given by equation (4.7).

(c) Most highly stressed coil or turn When all tappings are in circuit, the force upon the coil or turn adjacent to the compensating gap in the untapped winding may be calculated by applying equation (4.12); in such a case a would be the length of the gap expressed as a fraction. It should be noted, however, that since thinning or provision of a compensating gap is usually carried out on the inner winding, the presence of the core increases the force slightly. Hence this equation is likely to give results a few per cent low in this case. On the other hand, when thinning is used, the force upon the coil adjacent to the thinned-out portion of winding is rather less than given by equation (4.12).

(iii) Two tapping points midway between the middle and ends of the outer winding

(a) Without thinning of the untapped winding A typical example of the compression in the inner and outer windings is given in *Figure 4.90* for the case of approximately 13% tapped out of the outer winding, half being at each of two points midway between the middle and ends of the winding.

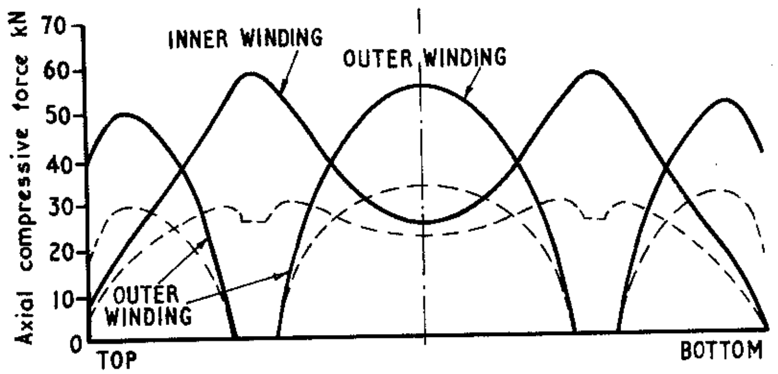


Figure 4.90 Axial compression curve for tappings at two points in the outer winding. Dashed curves show force with thinning of the inner winding opposite each tapping point

There are three points of maximum compression in the outer winding, the middle one being the largest. In the inner winding there are two equal maxima opposite the gaps in the outer winding.

The axial force upon each quarter of either winding due to the tappings is given by:

$$P_A = \frac{\pi a (NI_{\max})^2 \Lambda}{8 \times 10^{10}} \text{ kN} \tag{4.14}$$

where a is the total fraction of axial length tapped out, and the constant Λ has the value given in *Table 4.3*.

This force acts towards the yokes in the two end sections of the outer winding, so that equation (4.10) gives the axial end thrust for the larger values of a. The curve of end thrust plotted against the fraction tapped out can be estimated without difficulty since it deviates only slightly from the straight line of equation (4.14).

The forces with this arrangement of tappings are only about one-sixteenth of the forces due to tappings at one end of the winding, and they are of the same order as the forces in the untapped winding.

The most highly stressed coils are those adjacent to the tapping points, and the forces may be calculated from equation (4.12) by substituting $\frac{1}{2}a$ for a.

(b) With thinning of the untapped winding This practice represents the optimum method of reducing forces when a section is tapped out of a winding, and the dashed curves in *Figure 4.90* show the forces obtained when the inner winding is thinned opposite each of the two gaps in the outer winding to an extent of 50% of the total tapping range. The force upon each quarter of either winding is

$$P_A = \frac{\pi a (NI_{\max})^2 \Lambda}{16 \times 10^{10}} \text{ kN} \tag{4.15}$$

when all tappings are in circuit, and:

$$P_A = \frac{\pi a (NI_{\max})^2 \Lambda}{16(1 - \frac{1}{2}a)10^{10}} \text{ kN} \tag{4.16}$$

when all tappings are out of circuit. In these equations Λ has the value given in *Table 4.3*, and a represents the total fraction tapped out.

The forces upon the coils immediately adjacent to the gaps may be calculated as described in equation (4.12), since these forces are determined by the lengths of the gaps and not by their positions in the winding.

4.8 TANKS AND ANCILLARY EQUIPMENT

Transformer tanks

The transformer tank provides the containment for the core and windings and for the dielectric fluid. It must withstand the forces imposed on it during transport. On larger transformers, it usually also provides additional structural support for the core during transport. All but the smallest transformers are impregnated with oil under vacuum: the tank acts as the vacuum vessel for this operation.

Transformer tanks are almost invariably constructed of welded boiler plate to BS 4630 although in the case of some large transformers manufactured in the UK in the 1960s, aluminium was used in order to enable these to remain within the road transport weight limitations. The tank must have a removable cover so that access can be obtained for the installation and future removal, if necessary, of core and windings. The cover is fastened by a flange around the tank, usually bolted but on occasions welded – more on this aspect later – usually at a high level so that it can be removed for inspection of core and windings, if required, without draining all the oil. The cover is normally the simplest of fabrications, often no more than a stiffened flat plate. It should be inclined to the horizontal at about 1°, so that it will not collect rainwater: any stiffeners should also be arranged so that they will not collect water, either by the provision of drain holes or by forming them from channel sections with the open face downwards.

Even when they are to be finally sealed by means of continuous welding (see below) the joints between the main cover and the tank, and all smaller access covers, are made oil tight by means of gaskets. These are normally of synthetic rubber-bonded cork, or neoprene-bonded cork. This material consists of small cork chippings formed into sheets by means of a sythetic rubber compound. The thickness of the gaskets varies from around 6 to 15 mm according to the cross-section of the joint; however, the important feature is that the material is synthetic rubber based rather than using natural rubber since the former material has a far greater resistance to degradation by contact with mineral oil.

The tank is provided with an adequate number of smaller removable covers, allowing access to bushing connections, winding temperature CTs, core earthing links, off-circuit tapping links and the rear of tapping selector switches. Since the manufacturer needs to have access to these items in the works the designer ensures that adequate provision is made. All gasketed joints on the tank represent a potential source of oil leakage, so these inspection covers should be kept to a minimum. The main tank cover flange usually represents the greatest oil leakage threat, since, being of large cross-section, it tends to provide a path for leakage flux, with the resultant eddy-current heating leading to overheating and degradation of gaskets. Removable covers should be large enough to provide adequate safe access, able to withstand vacuum and pressure conditions and should also be small and light enough to enable them to be handled safely by maintenance personnel on site. This latter requirement usually means that they should not exceed 25 kg in weight.

Occasionally, the tanks of larger transformers may be provided with deep top main covers, so that the headroom necessary to lift the core and windings from the tank is reduced. This arrangement should be avoided, if possible, since a greater quantity of oil needs to be removed should it be necessary to lift the cover and it requires a more complex cover fabrication. It is also possible to provide a flange at low level, which may be additional to or instead of a high-level flange. This enables the cover to be removed on site, thus giving access to core and windings, without the need to lift these heavier items out of the tank. A tank having this arrangement of low-level flange is shown in *Figure 4.91*. It should be noted that while it can in certain circumstances be worthwhile incorporating such features into the design, it is never a straightforward matter to work on large high-voltage transformers on site so that this should not be considered as normal practice. (Nevertheless, in the UK, the CEGB has on a number of occasions carried out successful site repairs which have necessitated detanking of core and windings. Such on-site working does require careful planning and skilled operators and on these occasions was only undertaken when a clear knowledge of the scope of the work required and the ability to carry this out was evident. Often it is the ability to satisfactorily test on site the efficacy of the work after completion, which can be a critical factor in making the decision to do the work on site.)

Tanks which are required to withstand vacuum must be subjected to a type test to prove the design capability. This usually involves subjecting the first tank of any new design, when empty of oil, to a specified vacuum and measuring the permanent deformation remaining after the vacuum has been

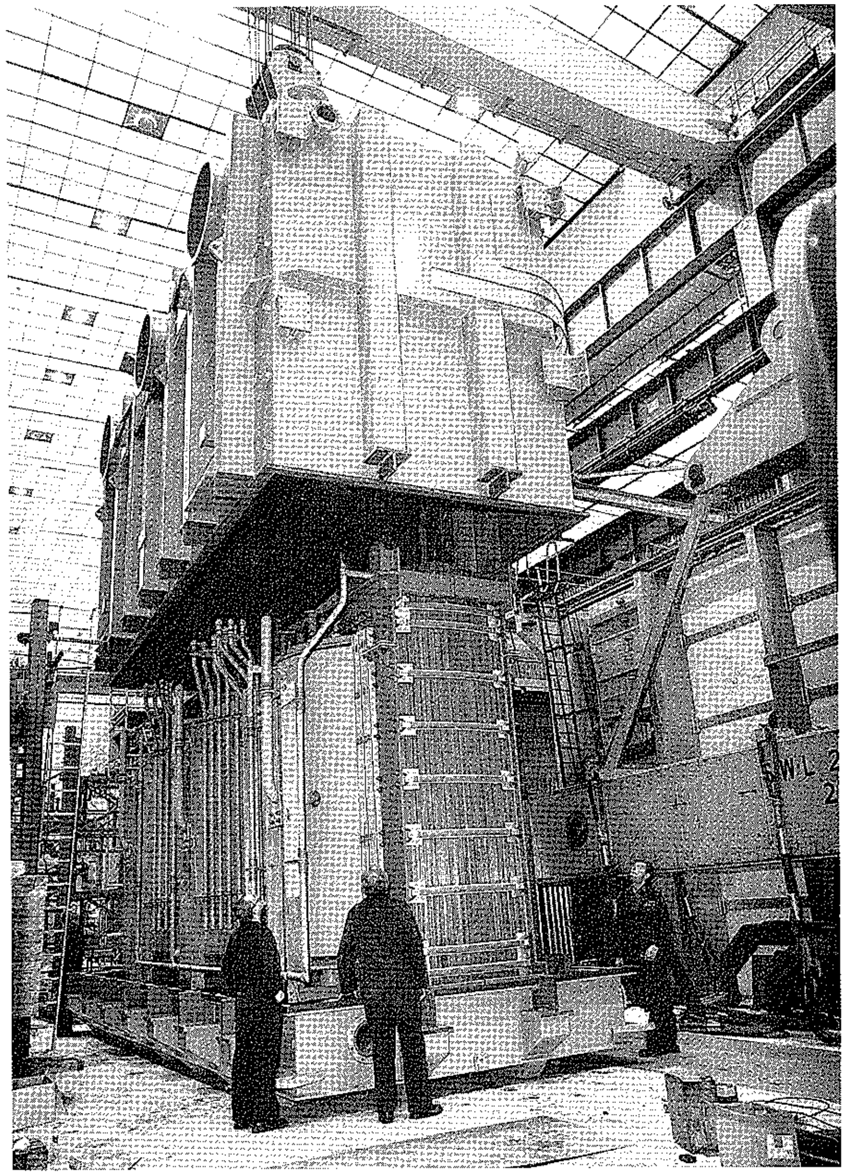


Figure 4.91 Transformer tank with low-level flange

released. The degree of vacuum applied usually depends on the voltage class which will determine the vacuum necessary when the tank is used as an impregnation vessel. Up to and including 132 kV transformer tanks, a vacuum equivalent to 330 mbar absolute pressure is usually specified and for higher voltage transformers the vacuum should be 25 mbar absolute. The acceptable permanent deflection after release of the vacuum depends on the dimensions of the tank. *Table 4.4* gives an indication of the levels of deflection which may be considered acceptable for particular sizes of tanks.

Table 4.4 Maximum permissible permanent deflection of tanks and other assemblies following vacuum withstand test

Minimum dimension of tank or fabricated assembly (metres)	*Maximum permanent deflection after release of vacuum (mm)*
Not exceeding 1.3	3
Exceeding 1.3 but not exceeding 2	6
Exceeding 2 but not exceeding 2.5	10
Exceeding 2.5	13

Mention has been made of the need to avoid, or reduce, the likelihood of oil leaks. The welding of transformer tanks does not demand any sophisticated processes but it is nevertheless important to ensure that those welds associated with the tank-lifting lugs are of good quality. These are usually crack tested, either ultrasonically or with dye penetrant. Tanks must also be given an adequate test for oil tightness during manufacture. Good practice is to fill with white spirit or some other fairly penetrating low-viscosity liquid and apply a pressure of about 700 mbar, or the normal pressure plus 350 mbar, whichever is the greater, for 24 hours. This must be contained without any leakage.

The tank must carry the means of making the electrical connections. Cable boxes are usual for all voltages up to and including 11 kV, although for pole-mounted distribution transformers the preferred arrangement is to terminate the connecting cable in an air sealing-end and jumper across to 11 or 3.3 kV bushings on the transformer. Such an arrangement is shown in *Figure 4.92*. Above this voltage air bushings are normally used, although increasing use is now being made of SF_6-filled connections between transformer and switchgear at 132 kV and above this can be particularly convenient in poluted locations or on sites where space is not available for the necessary air clearances required by bushings.

Tanks must be provided with valves for filling and draining, and to allow oil sampling when required. These also enable the oil to be circulated through external filtration and drying equipment prior to initial energisation on site, or during service when oil has been replaced after obtaining access to the core and windings. Lifting lugs or, on small units, lifting eyes must be provided, as well as jacking pads and haulage holes to enable the transformer to be manoeuvred on site. On all but the smaller distribution transformers an oil sampling valve must also be provided to enable a sample of the oil to be taken for analysis with the minimum of disturbance or turbulence, which might cause changes to the dissolved gas content of the sample and thereby lead to erroneous diagnosis. Periodic sampling and analysis of the oil is the most reliable guide to the condition of the transformer in service and an important part of the

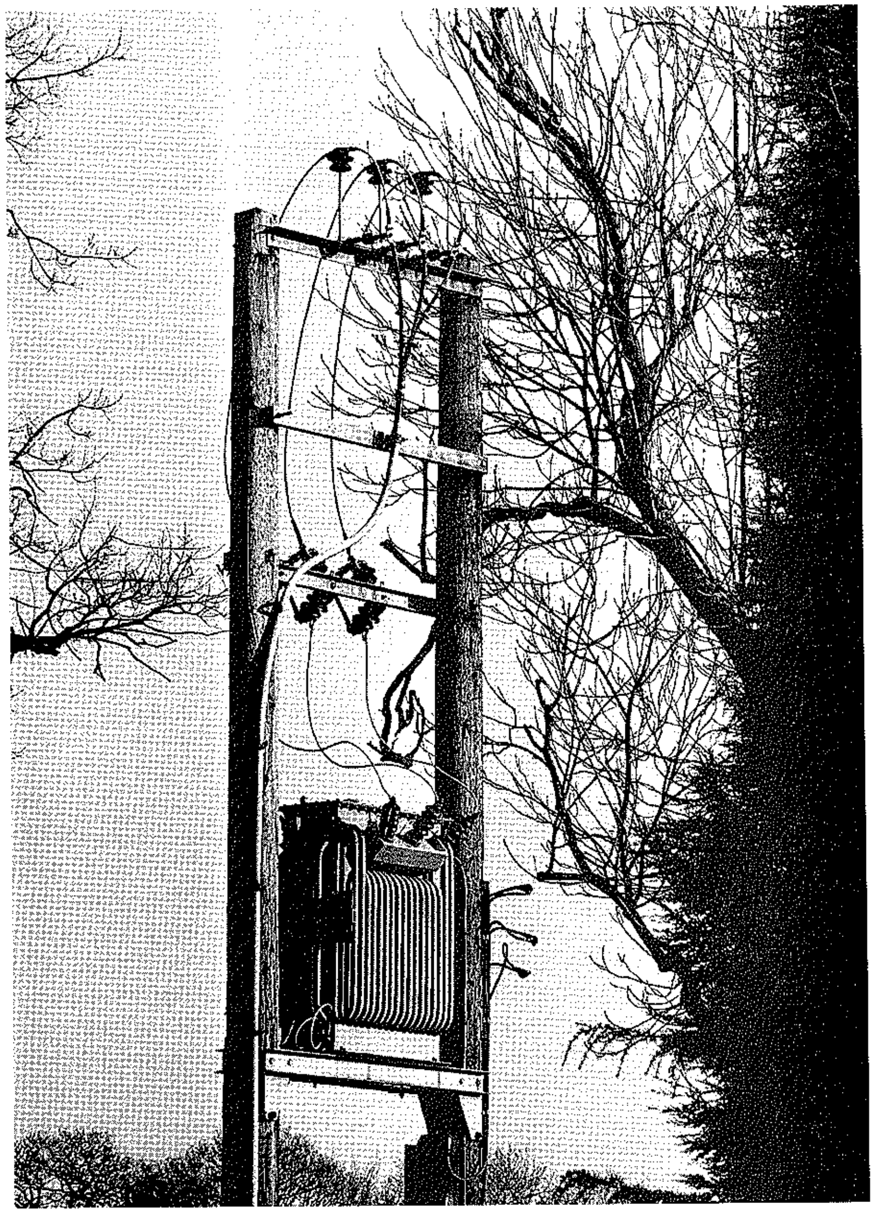


Figure 4.92 11 kV pole mounted transformer supplied via 11 kV cable having air sealing ends connected via jumpers to bushings on the transformer. Note that the 415 V output from the transformer is also taken away via a cable

maintenance routine. This subject is dealt with in Section 7 of Chapter 6. The sampling valve is normally located about one metre above the tank base in order to obtain as representative a sample as possible.

Transformer tanks must also have one or more devices to allow the relief of any sudden internal pressure rise, such as that resulting from an internal fault. Until a few years ago, this device was usually a bursting diaphragm set in an upstand pipe mounted on the cover and arranged to discharge clear of the tank itself. This had the disadvantage that, once it had burst, it allowed an indefinite amount of oil to be released, which might aggravate any fire associated with the fault, and also it left the windings open to the atmosphere. The bursting diaphragm has been superseded by a spring-operated self-resealing device which only releases the volume of oil necessary to relieve the excess pressure before resealing the tank. As shown in *Figure 4.93*, it is essentially a spring-loaded valve providing instantaneous amplification of the actuation force.

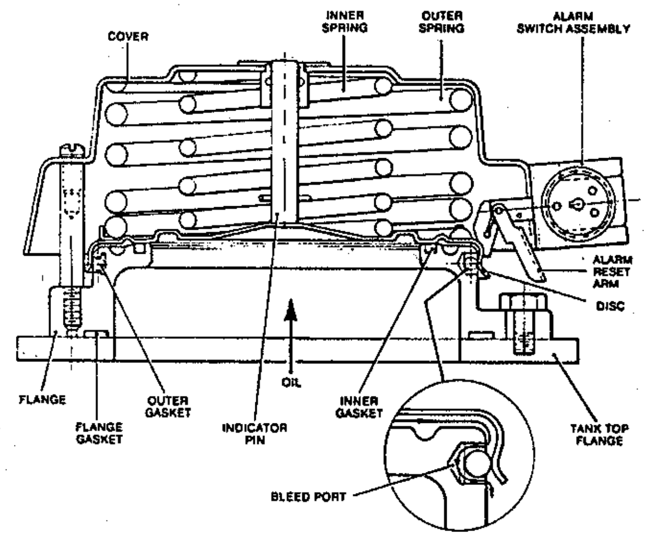


Figure 4.93 Qualitrol pressure relief device

The unit is mounted on a transformer by lugs on the flange and sealed by a mounting gasket. A spring-loaded valve disc is sealed against inner and outer gasket rings by the springs. The valve operates when the oil pressure acting on the area inside the inner gasket ring exceeds the closing force established by the springs. As the disc moves upwards slightly from the inner gasket ring, the oil pressure quickly becomes exposed to the disc area over the diameter of the outer gasket ring, resulting in a greatly increased force, and causing immediate full opening of the valve corresponding to the closed height of the springs. The transformer pressure is rapidly reduced to normal and the springs then return the valve disc to the closed position. A minute bleed port

to the outside atmosphere from the volume entrapped between the gasket rings prevents inadvertent valve opening if foreign particles on the inner gasket ring prevent a perfect ring-to-disc seal. A mechanical indicator pin in the cover, although not fastened to the valve disc, moves with it during operation and is held in the operated position by an O-ring in the pin bushing. This remains clearly visible, indicating that the valve has operated.

No pressure relief device can provide complete protection against all internal pressure transients. On the largest tanks, two such devices at opposite ends of the tank improve the protection. It is usual to place the pressure relief device as high on the tank as possible. This minimises the static head applied to the spring, thus reducing the likelihood of spurious operation in the event of a 'normal' pressure transient, for example the starting of an oil pump.

However, with the pressure relief device located at high level, there is the risk that operation might drench an operator with hot oil; to prevent this, an enclosure is provided around the device to contain and direct the oil safely down to plinth level. Such enclosure must not, of course, create any significant back pressure which would prevent the relief device from performing its function properly: a minimum cross-section for any ducting of about 300 cm^2 is usually adequate.

To complete the list of fittings on the transformer tank, it is usual to provide a pocket, or pockets, in the cover to take a thermometer for measurement of top oil temperature, a diagram/nameplate to provide information of transformer details, and an earthing terminal for the main tank earth connection.

Oil preservation equipment–conservators

Although it is now common for many of the smaller distribution transformers to dispense with a conservator all of the larger more important oil-filled transformers benefit greatly by the use of a conservator.

The use of a conservator allows the main tank to be filled to the cover, thus permitting cover-mounted bushings, where required, and it also makes possible the use of a Buchholz relay (see below). However, the most important feature of a conservator is that it reduces the surface area of the oil exposed to atmospheric air. This reduces the rate of oxidisation of the oil and also reduces the level of dissolved oxygen, which would otherwise tend to shorten insulation life. The full significance of this aspect of conservators will be made clear in Section 7 of Chapter 6. (See also Section 5 of Chapter 3.)

Recent investigation, for example that of Shroff and Stannett (1985) [4.2], has highlighted the part played by dissolved oxygen in accelerating insulation ageing. Although to date there are no published reports of specific measures which have been implemented to reduce levels of dissolved oxygen beyond the use of conservators, it is possible that some arrangement might be introduced to reduce further the degree of contact between oil and air; for example, this could be simply achieved by the use of a parallel-sided conservator having a 'float' covering the surface of the oil. (Some transformer operators in areas with high ambient temperatures and high humidity do, of course, incorporate measures mainly aimed at reducing moisture ingress into the oil. This is discussed further below and in Chapter 7.)

It is necessary to exclude moisture from the air space above the conservator oil level, in order to maintain the dryness of the transformer oil. For transformers below 132 kV, this space is vented through a device containing a drying agent (usually silica gel, impregnated with cobalt chloride) through which the air entering the conservator is passed. When the moisture content of the silica gel becomes excessive, as indicated by the change in colour of the cobalt chloride from blue to pink, its ability to extract further moisture is reduced and it must be replaced by a further charge of dry material. The saturated gel can be reactivated by drying it in an oven when the colour of the crystals will revert to blue.

The effectiveness of this type of breather depends upon a number of factors; the dryness of the gel, the moisture content of the incoming air and the ambient temperature being the most significant.

If optimum performance is to be obtained from a transformer having an HV winding of 132 kV and above or, indeed, any generator transformer operating at high load factor, then it is desirable to maintain a high degree of dryness of the oil, typically less than 10 parts per million by volume at 20°C. Although oil treatment on initial filling can achieve these levels, moisture levels tend to increase over and above any moisture which is taken in through breathing, since water is a product of normal insulation degradation, and this is taking place all the time that the transformer is on-load. It is desirable, therefore, to maintain something akin to a continuous treatment to extract moisture from the oil. This is the principle employed in the refrigeration type of breather, illustrated in *Figure 4.94*. Incoming air is passed through a low-temperature chamber which causes any water vapour present to be collected on the chamber walls. The chamber is cooled by means of thermoelectric modules in which a temperature difference is generated by the passage of an electric current (the Peltier effect). Periodically the current is reversed; the accumulated ice melts and drains away. In addition to the drying of the incoming air, this type of breather can be arranged such that the thermosyphon action created between the air in the cooled duct and that in the air space of the conservator creates a continuous circulation and, therefore, a continuous drying action. As the air space in the conservator becomes increasingly dried, the equilibrium level of moisture in the oil for the pressure and temperature conditions prevailing will be reduced so that the oil will give up water to the air in the space above the oil to restore the equilibrium and this, in turn, causes further moisture to migrate from the insulation to the oil, so that a continuous drying process takes place.

The conservator is provided with a sump by arranging that the pipe connecting with the transformer projects into the bottom by about 75.0 mm. This collects any sludge which might be formed over a period of years by oxidation of the oil. A lockable drain valve is normally fitted and one end of the conservator is usually made removable so that, if necessary, the internals

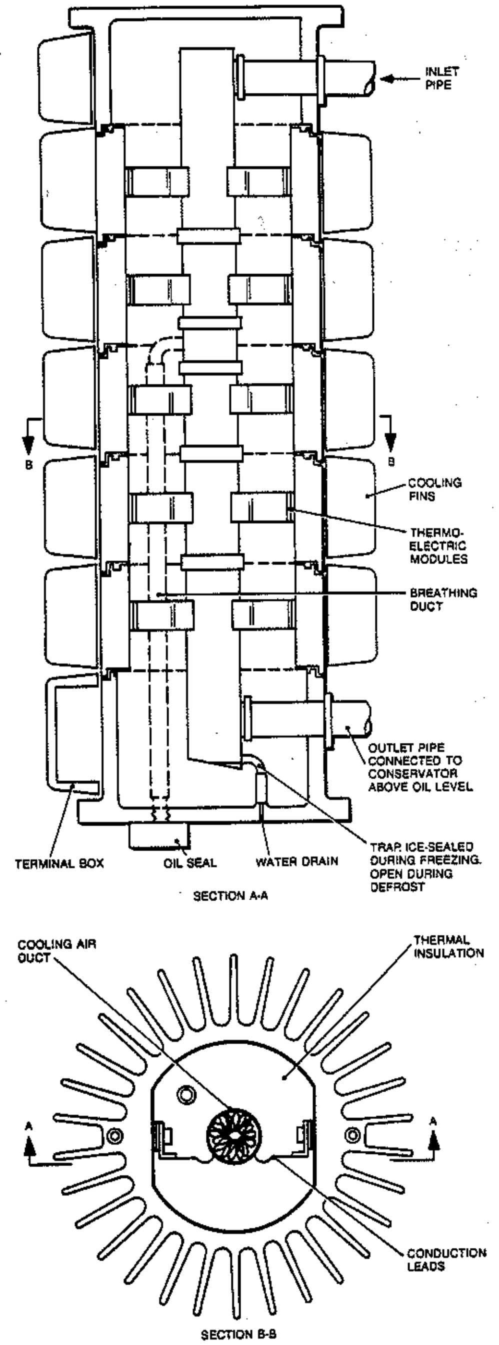


Figure 4.94 Refrigeration breather

may be cleaned out. One end face usually incorporates a prismatic oil level gauge or a magnetic dial-type gauge: these should be angled downwards by some 10–15° so that they can be easily viewed from plinth level. It is usual to show the minimum, cold oil, 75°C and maximum oil levels on whichever type of gauge is provided.

Alternative oil preservation systems

Refrigeration breathers are usually considered too costly to be used on any but the larger more expensive transformers operating at 132 kV or higher for which a high level of oil dryness is necessary. In very humid climates such as those prevailing in many tropical countries the task of maintaining a satisfactory level of dryness of the drying agent in a silica gel-type breather can be too demanding so that alternative forms of breathing arrangements must be adopted. The most common is the air-bag system shown diagramatically in *Figure 4.95*. With this arrangement the transformer has what is basically a normal conservator except that the space above the oil is filled with a synthetic rubber bag. The interior of the bag is then connected to atmosphere so that it can breathe in air when the transformer cools and the oil volume is reduced and breathe this out when the transformer heats up. With this arrangement the oil is prevented from coming into direct contact with the air and thereby lies its disadvantage. Water is one of the products of the degradation of paper insulation and as explained in Chapter 3 the presence of moisture also accelerates the degradation process. If the air space within

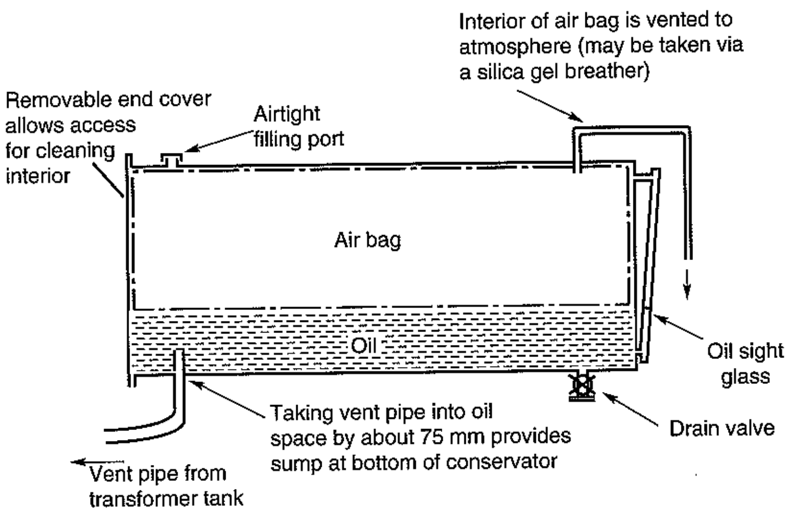


Figure 4.95 Coventionally designed conservator tank but with air space filled with synthetic rubber bag to prevent contact between oil and external air

the conservator is maintained in a dry condition, either by means of a well-maintained silica gel breather or by a refrigeration breather, this will allow moisture to migrate from the oil, and ultimately from the paper insulation to maintain this in a dry condition and minimise ageing. If this moisture remains trapped in the transformer by the presence of a synthetic rubber diaphragm or by other means, the rate of ageing will be increased.

A better arrangement than that just described is again to use basically a normal conservator but to arrange that the space above the oil is filled with dry nitrogen. This can be provided from a cylinder of compressed gas via a pressure reducing valve. When the transformer breathes in due to a reduction in load or ambient temperature the pressure reducing valve allows more nitrogen to be released. When the oil volume increases nitrogen is vented to atmosphere by means of a vent valve. Because the nitrogen is always maintained in a dry state, this arrangement has the great advantage that it maintains the oil and insulation in as dry a condition as possible. The only disadvantage is the supply and cost of the nitrogen needed to maintain a constant supply thus adding to the routine maintenance activities.

It is now common practice, not only in climates having high humidity, for smaller oil-filled distribution transformers to be permanently hermetically sealed. This has the great advantage of being cheap and of requiring virtually no maintenance. Since transformer oil is incompressible, with a sealed arrangement it is necessary to provide space above the oil, filled with either dry air or nitrogen, to act as a cushion for expansion and contraction of the oil. Without this cushion the tank internals would experience very large changes of pressure between the no-load and the loaded condition. (To some extent, this problem is reduced if the transformer has a corrugated tank, see below.) These pressure variations can cause joints to leak so that external air is drawn in at light load conditions, usually bringing in with it moisture or even water, or they can cause dissolved gas in the oil to be brought out of solution and thus form voids leading to internal electrical discharges and ultimate failure. The more sophisticated or strategically important sealed transformers are provided with a pressure gauge which shows an internal positive pressure when the transformer is loaded, thus indicating that the seal remains sound.

Corrugated tanks

A convenient way of providing some means of accommodating expansion and contraction of the oil as well as dissipating losses from small sealed distribution transformers is to use a corrugated tank as shown in *Figure 4.96*. The corrugations are formed from light gauge steel. They may be from 80 to 200 mm deep and about 400 mm high at about 20 mm spacing, thus forming the sides of the tank into cooling fins. The top and bottom edges are seam-welded and the fins are able to expand and close-up concertina fashion as the tank internal pressure varies, thus absorbing some of the pressure variation. The system is not without its disadvantages; it is necessary to maintain a high level of quality control on the seam-welded fin edges and, because of the thin

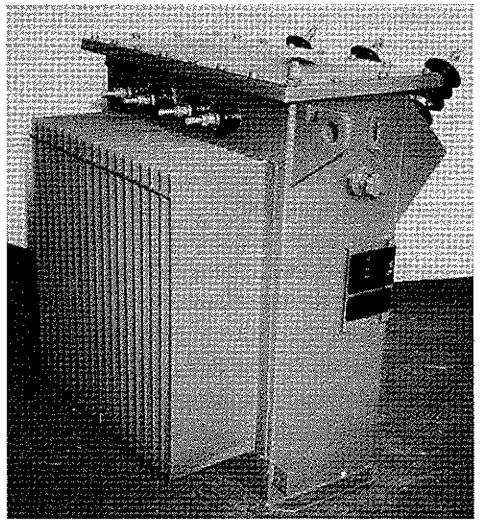


Figure 4.96 Three-phase 200 kVA, 11 kV, 50 Hz pole-mounted transformer showing a corrugated tank (Bonar Long Ltd)

gauge of the metal used, a good paint protective treatment is necessary. This might not be readily achieved if the fins are too deep and too closely spaced. To minimise these problems it is considered that the material thickness should be no less than 1.5 mm.

Gas and oil-actuated relays

As mentioned above, the provision of a conservator also permits the installation of a Buchholz relay. This is installed in the run of pipe connecting the conservator to the main tank. In this location, the relay collects any gas produced by a fault inside the tank. The presence of this gas causes a float to be depressed which is then arranged to operate a pair of contacts which can be set to 'alarm', or 'trip', or both, dependent upon the rate of gas production. A more detailed description of this device will be found in the section dealing with transformer protection (Section 6 of Chapter 6). In order to ensure that any gas evolved in the tank is vented to the conservator it is necessary to vent every high point on the tank cover, for example each bushing turret, and to connect these to the conservator feed pipe on the tank side of the Buchholz relay, normally using about 20 mm bore pipework. The main connecting pipe between tank and conservator is 75 or 100 mm bore, depending upon the size of the transformer.

Bushing connections

A bushing is a means of bringing an electrical connection from the inside to the outside of the tank. It provides the necessary insulation between the winding electrical connection and the main tank which is at earth potential. The bushing forms a pressure-tight barrier enabling the necessary vacuum to be drawn for the purpose of oil impregnation of the windings. It must ensure freedom from leaks during the operating lifetime of the transformer and be capable of maintaining electrical insulation under all conditions such as driving rain, ice and fog and has to provide the required current-carrying path with an acceptable temperature rise. Varying degrees of sophistication are necessary to meet these requirements, depending on the voltage and/or current rating of the bushing. *Figure 4.97* shows an 11 kV bushing with a current rating of about 1000 A. This has a central current-carrying stem, usually of copper, and the insulation is provided by a combination of the porcelain shell and the transformer oil. Under oil, the porcelain surface creepage strength is very much greater than in air, so that the 'below oil' portion of the bushing has a plain porcelain surface. The 'air' portion has the familiar shedded profile in order to provide a very much longer creepage path, a proportion of which is 'protected' so that it remains dry in rainy or foggy conditions.

At 33 kV and above, it is necessary to provide additional stress control between the central high-voltage lead and the external, 'earthy' metal mounting flange. This can take the form either of a synthetic resin-bonded paper multifoil capacitor or of an oil-impregnated paper capacitor of similar construction. This type of bushing is usually known as a condenser bushing. *Figure 4.98* shows a 400 kV oil-impregnated paper bushing in part section. The radial electrical stress is graded through the insulant by means of the concentric capacitor foils and the axial stress is controlled by the graded lengths of these. The capacitor is housed between an inner current conducting tube and the outer porcelain casing which is in two parts, the upper part is a weatherproof shedded porcelain and the lower part (the oil-immersed end) is plain porcelain. The interspace is oil filled and the bushing head, or 'helmet', provides oil-expansion space and is fitted with a prismatic sight glass to give indication of the bushing oil level. This head also allows space for an air or gas cushion to allow for expansion and contraction of the oil. This expansion space must be adequately sealed against the ingress of atmospheric air (and hence moisture) and it is usual in such designs to incorporate a spring pack, housed in the top cap, to maintain pressure loading on gasketed joints while allowing for expansion and contraction of the different components during temperature changes.

Clearly, this type of bushing is designed for installation at, or near, the vertical position. The bushing illustrated is of the so-called 're-entrant' pattern in that the connection to the line lead is housed within the lower end of the bushing. This has the effect of foreshortening the under-oil end of the bushing but requires a more complex lower porcelain section which adds considerably to the cost. In order to make the electrical connection to the bushing, the HV lead terminates in a flexible pigtail which is threaded through the central tube and connected inside the head of the bushing. In some higher current versions the pigtail is replaced by a copper tube, in which case it is necessary to incorporate some flexible section to accommodate relative movement, thermal

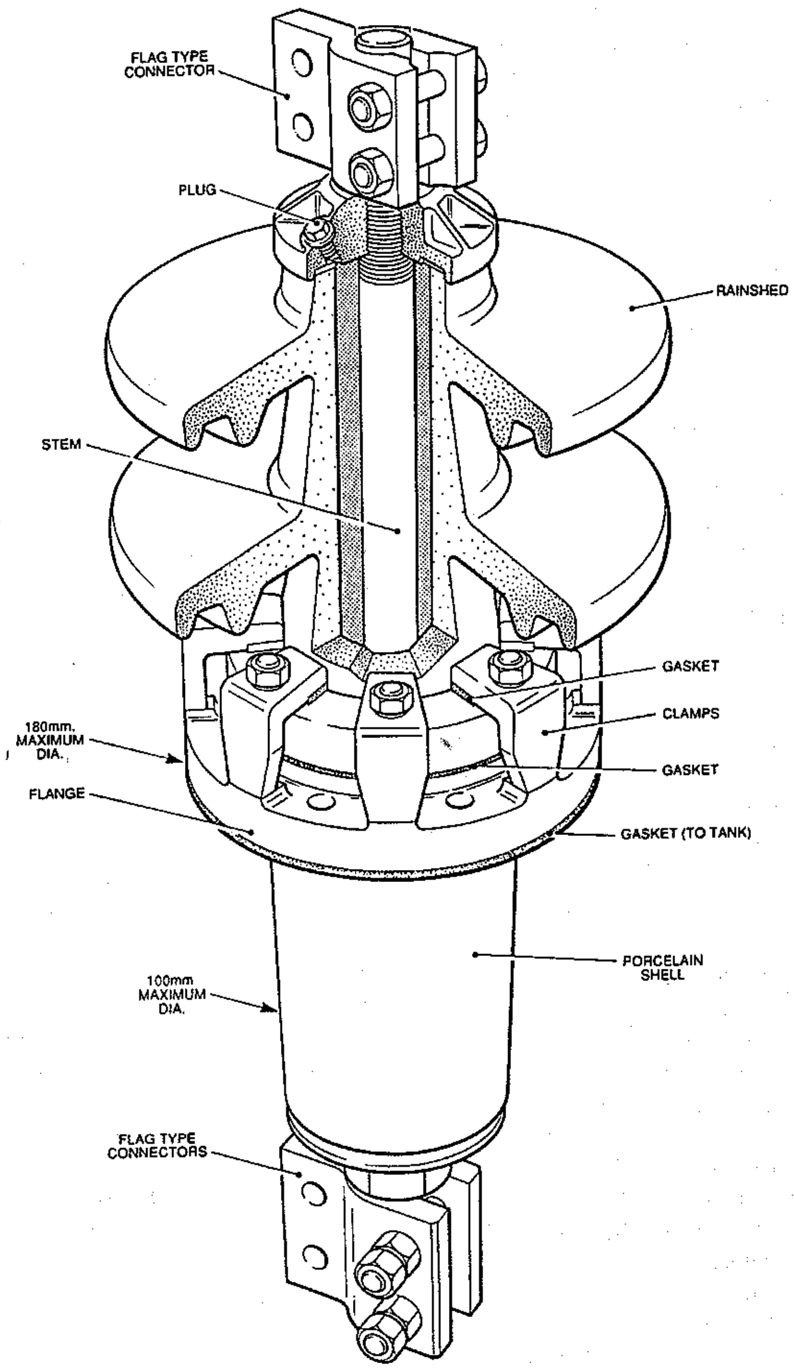


Figure 4.97 11 kV bushing

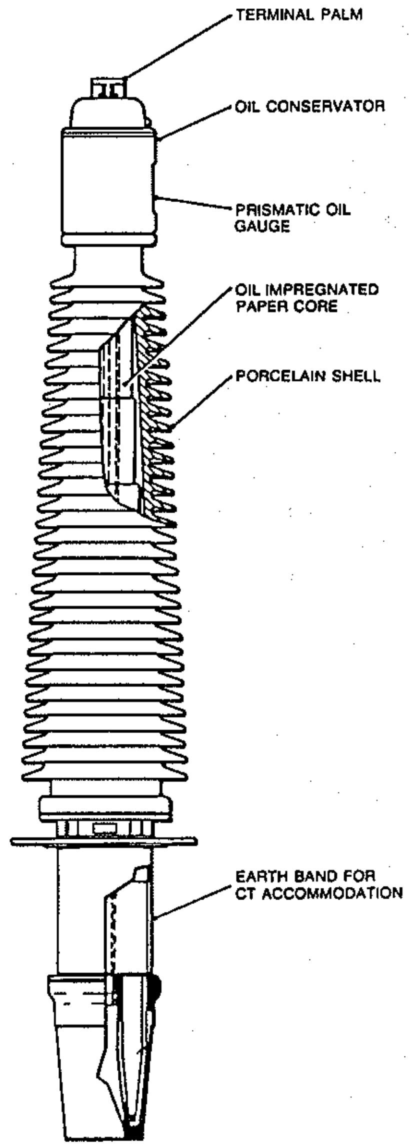


Figure 4.98 400 kV oil-impregnated paper bushing

and mechanical between the transformer internals and the head of the bushing. This must be capable of withstanding the mechanical vibration and of carrying the maximum rated current of the transformer. The heavy insulation on the line lead, is only taken just inside the re-entrant end of the bushing. With this arrangement, an inverted conical section gas-bubble deflector must be fitted beneath the re-entrant end of the bushing to ensure that any gas evolved within the transformer tank is directed to the Buchholz relay and not allowed to collect within the central stem of the bushing.

Versions of 400 kV oil-impregnated paper bushing have been developed in which the under-oil porcelain is replaced by a cast epoxy resin section. This material is able to withstand a higher electrical creepage stress under oil than porcelain which thus allows a plain tapered profile to be used instead of the re-entrant arrangement. With this type of bushing the transformer lead can be connected directly to a palm at the lower end of the bushing as shown in *Figure 4.99*.

The most recent development in EHV bushings is to replace the oil-impregnated paper capacitor by one using epoxy resin-impregnated paper (frequently abbreviated to e.r.i.p.). These bushings were originally developed for use with SF_6 but are now widely used for air/oil interfaces. These bushings still retain porcelain oil-filled upper casings, since it is difficult to find an alternative material with the weathering and abrasion resistance properties of porcelain, but the under-oil end is totally resin encapsulated.

In most EHV bushings provision is made for accommodation of a number of toroidally wound current transformers by incorporating an earth band at the oil-immersed end just below the mounting flange. The bushing is usually mounted on top of a 'turret' which provides a housing for the current transformers and the arrangement is usually such that the bushing can be removed without disturbing the separately mounted current transformers. The current transformer secondary connections are brought to a terminal housing mounted on the side of the turret.

In 400 and 275 kV bushings, the designer's main difficulty is to provide an insulation system capable of withstanding the high working voltage. The low-voltage bushings of a large generator transformer present a different problem. Here, the electrical stress is modest but the difficulty is in providing a current rating of up to 14 000 A, the phase current of an 800 MVA unit. *Figure 4.100* shows a bushing rated at 33 kV, 14 000 A. The current is carried by the large central copper cylinder, each end of which carries a palm assembly to provide the heavy current connections to the bushing. The superior cooling capability provided by the transformer oil at the 'under-oil' end of the bushing means that only two parallel palms are required. At the air end of the bushing, it is necessary to provide a very much larger palm surface area and to adopt a configuration which ensures a uniform distribution of the current. It has been found that an arrangement approximating to a circular cross-section – here, octagonal – achieves this better than one having plain parallel palms. These palms may be silver plated to improve their electrical contact with the external connectors, but if the contact face temperature can be limited to 90 °C a more reliable connection can be made to plain copper palms, provided that the joint is made correctly.

Insulation is provided by a synthetic resin-bonded paper tube and, as can be seen from the diagram, this also provides the means of mounting the flange.

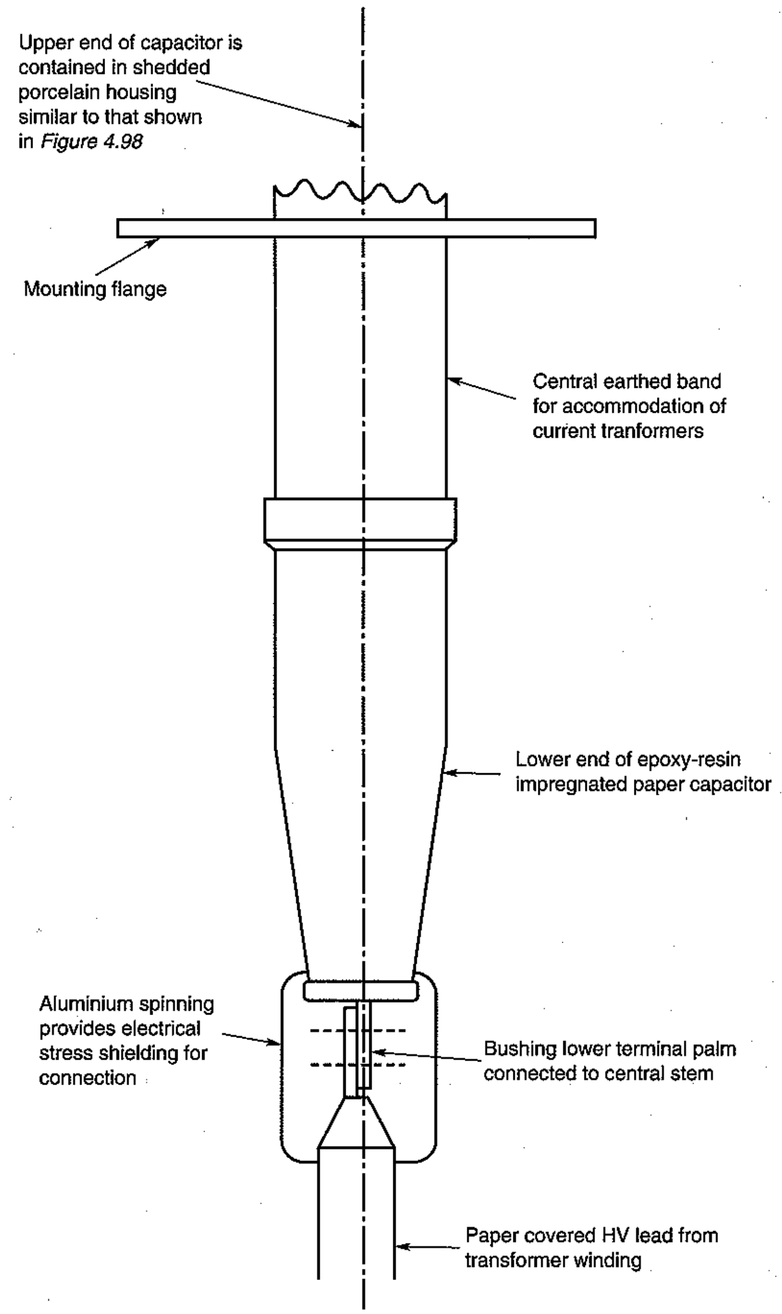


Figure 4.99 Arrangement of connection of transformer 400 kV HV lead to lower end of epoxy resin-impregnated paper (e.r.i.p.) bushing

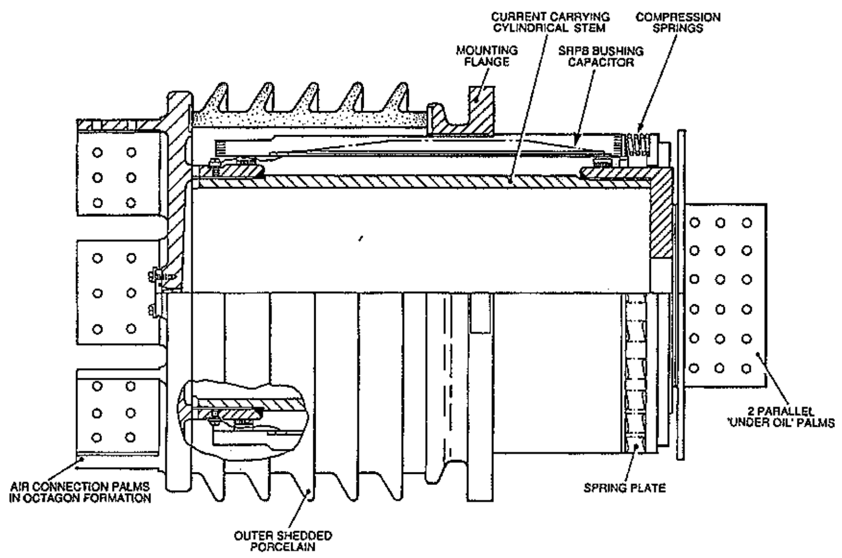


Figure 4.100 Simplified cross-section of a 33 kV, 14 000 A bushing

External weather protection for the air end is provided by the conventional shedded porcelain housing. Where the bushing is to be accommodated within external phase-isolated connections an air-release plug on the upper-end flange allows air to be bled from the inside of the assembly, so that it can be filled with oil under the head of the conservator.

SF_6 connections

With the introduction of 400 kV SF_6-insulated metalclad switchgear into the UK in the late 1970s, the benefits of making a direct connection between the switchgear and the transformer were quickly recognised. At the former CEGB's (now First Hydro company's) Dinorwig power station, for example, transformers and 400 kV switchgear are accommodated underground. The transformer hall is immediately below the 400 kV switchgear gallery and 400 kV metalclad connections pass directly through the floor of this to connect to the transformers beneath. Even where transformers and switchgear cannot be quite so conveniently located, there are significant space saving benefits if 400 kV connections can be made direct to the transformer, totally enclosed within SF_6 trunking. *Figure 4.101* shows a typical arrangement which might be used for the connection of a 400 kV generator transformer. The 400 kV cable which connects to the 400 kV substation is terminated with an SF_6 sealing end. SF_6 trunking houses line isolator, earth switch and surge diverter. By mounting the 400 kV SF_6/oil bushing horizontally, the overall height of the cable sealing-end structure can be reduced.

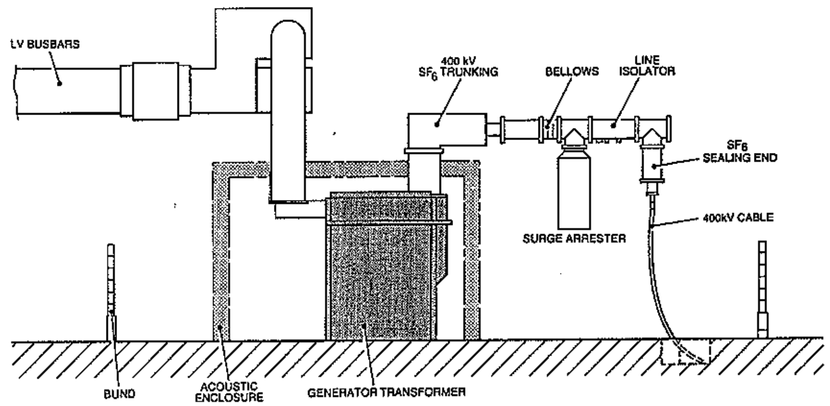


Figure 4.101 Simplified arrangement of 400 kV SF_6 connection to generator transformer

The construction of the 400 kV SF_6/oil bushing is similar to that of the air/oil bushing described previously in that stress control is achieved by means of an e.r.i.p. capacitor housed within a cast resin rather than a porcelain shell. The 'under-oil' end is 'conventional', i.e. it is not re-entrant, and, since there is no need for the lengthy air-creepage path used in an air/oil bushing, the SF_6 end is very much shorter than its air equivalent.

Cable box connections

Cable boxes are the preferred means of making connections at 11, 6.6, 3.3 kV and 415 V in industrial complexes, as for most other electrical plant installed in these locations. Cabling principles are not within the scope of this volume and practices differ widely, but the following section reviews what might be considered best practice for power transformer terminations on HV systems having high fault levels.

Modern polymeric-insulated cables can be housed in air-insulated boxes. Such connections can be disconnected with relative simplicity and it is not therefore necessary to provide the separate disconnecting chamber needed for a compound-filled cable box with a paper-insulated cable. LV line currents can occasionally be as high as 3000 A at 11 kV, for example on the station transformers of a large power station, and, with cable current ratings limited to 600–800 A, as many as five cables per phase can be necessary. For small transformers of 1 MVA or less on high fault level installations it is still advantageous to use one cable per phase since generally this will restrict faults to single phase to earth. On fuse-protected circuits at this rating three-core cables are a possibility. Since the very rapid price rise of copper which took place in the 1960s, many power cables are made of aluminium. The solid conductors tend to be bulkier and stiffer than their copper counterparts and this has to be taken into account in the cable box design if aluminium-cored cables are to be used. Each cable has its own individual glandplate so that the cable jointer can gland the cable, manoeuvre it into position and connect it to the terminal. Both cable core and bushing will usually have palm-type terminations which are connected with a single bolt. To give the jointer some flexibility and to provide the necessary tolerances, it is desirable that the glandplate-to-bushing terminal separation should be at least 320 mm.

For cable ratings of up to 400 A, non-magnetic glandplates should be used. For ratings above 400 A, the entire box should be constructed of non-magnetic material in order to reduce stray losses within the shell which would otherwise increase its temperature rise, with the possible risk of overheating the cable insulation. To enable the box to breathe and to avoid the build-up of internal condensation, a small drain hole, say 12 mm in diameter, is provided in one glandplate.

Figure 4.102 shows a typical 3.3 kV air-insulated cable box having a rating of about 2400 A with 4 × 400 mm^2 aluminium cables per bushing.

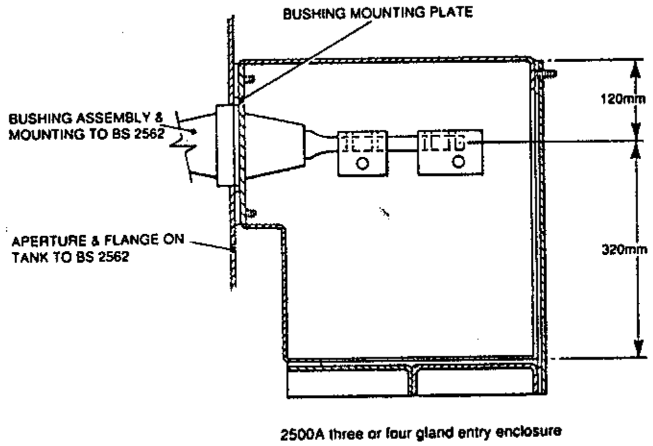


Figure 4.102 3.3 kV cable box

At 11 kV, some stress control is required in an air-insulated box, so the bushing and cable terminations are designed as an integrated assembly, as shown in *Figure 4.103(a)*.

Figure 4.103(b) shows a cross-section of a typical moulded-rubber socket connector which is fitted to the end of an 11 kV cable. This has internal and external semi conductive screens: the inner screen, the cable conductor connector and the outer provides continuity for the cable outer screen, so that this encloses the entire termination. The external screen is bonded to earth by connection to the external lug shown in the figure. The joint is assembled by fitting the socket connector over the mating bushing and then screwing the insulating plug, containing a metal threaded insert, onto the end

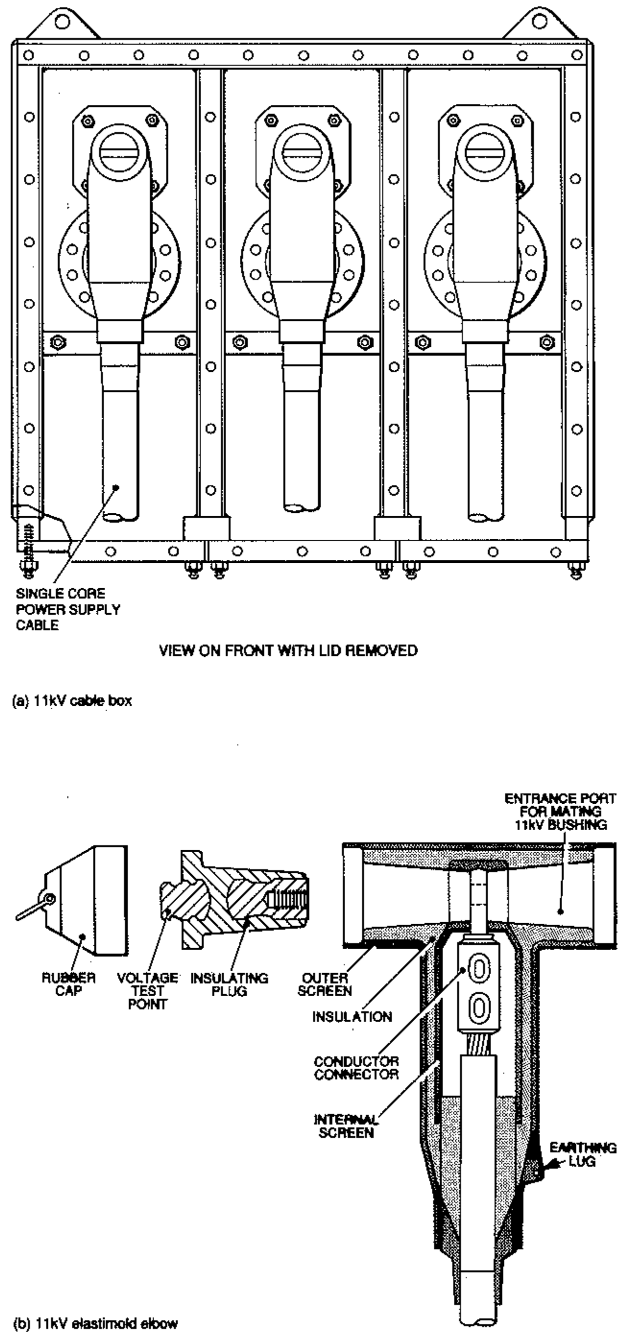


Figure 4.103 11 kV cable box and section of 11 kV elastimold elbow termination

of the bushing stem. This is tightened by means of a spanner applied to the hexagonal-nut insert in the outer end of this plug. This insert also serves as a capacitative voltage test point. After making the joint, this is finally covered by the semiconducting moulded-rubber cap.

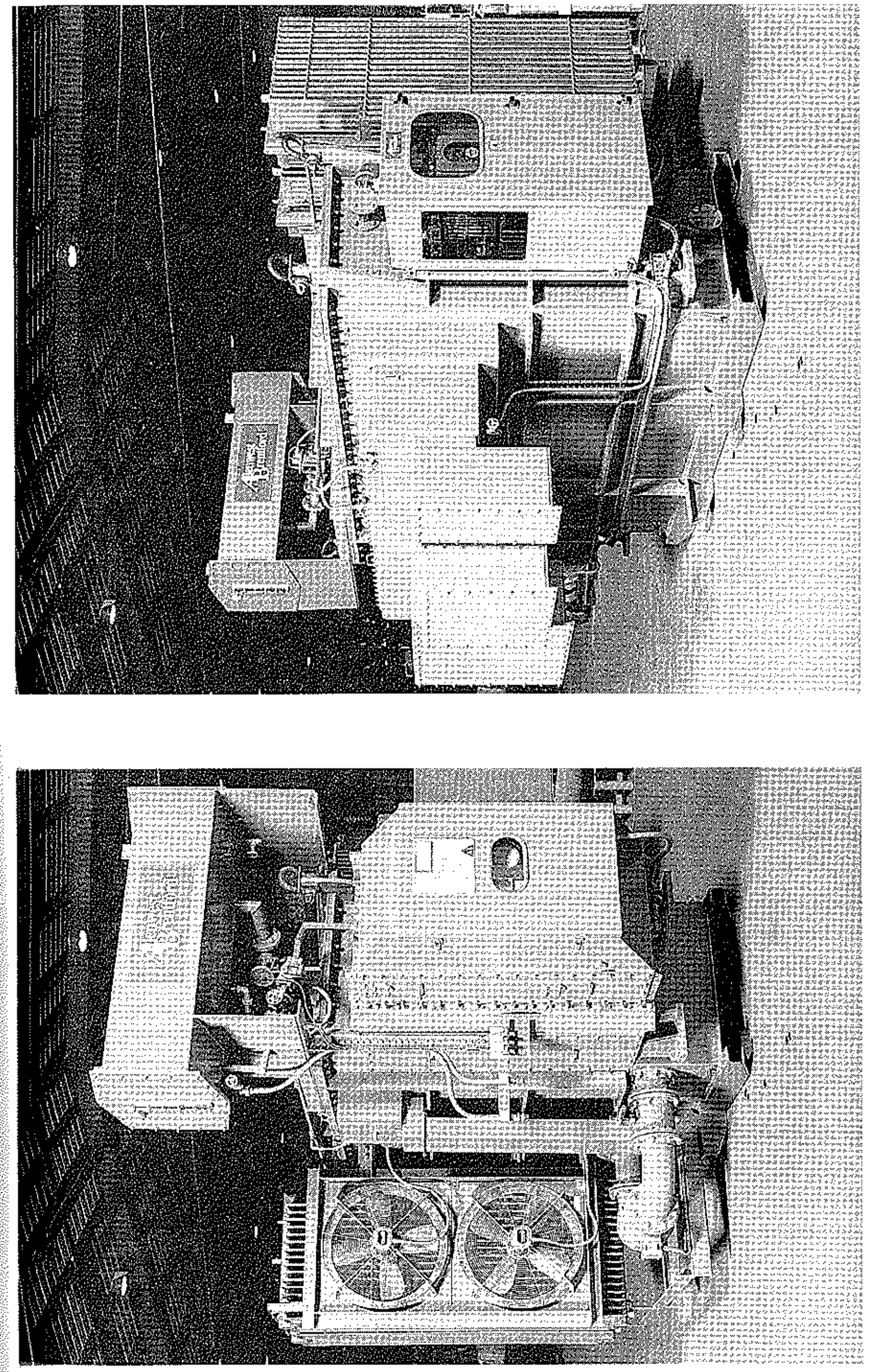
Since the external semiconductive coating of this type of connector is bonded to earth, there would be no electrical hazard resulting from its use without any external enclosure and, indeed, it is common practice for a connector of this type to be used in this way in many European countries provided that the area has restricted access. However, UK practice is usually to enclose the termination within a non-magnetic sheet-steel box to provide mechanical protection and phase isolation. Should a fault occur, this must be contained by the box which ensures that it remains a phase-to-earth fault, normally limited by a resistor at the system neutral point, rather than developing into an unrestricted phase-to-phase fault.

For higher voltage terminations, that is at 132, 275 and 400 kV, direct cable connections were occasionally made to transformers. These usually consisted of an oil-filled sealing-end chamber with a link connected to an oil/oil bushing through the transformer tank cover. Cable connections are now invariably made via an intermediate section of SF_6 trunking as described above.

Tank-mounted coolers

Tank-mounted pressed-steel radiators now represent the most widely used arrangement for cooling smaller transformers for which tank surface alone is not adequate. These can now be manufactured so cheaply and fitted so easily that they have totally replaced the arrangement of tubes which were commonly used for most distribution transformers. They are available in various patterns but all consist basically of a number of flat 'passes' of edge-welded plates connecting a top and bottom header. Oil flows into the top and out of the bottom of the radiators via the headers and is cooled as it flows downwards through the thin sheet-steel passes. The arrangement is most suited to transformers having natural oil and natural air circulations, i.e. ONAN cooling, as defined in BS 171.

For larger units it is be possible to suspend a fan below or on the side of the radiators to provide a forced draught, ONAF arrangement. This might enable the transformer rating to be increased by some 25%, but only at the extra cost and complexity of control gear and cabling for, say, two or four fans. Achievement of this modest uprating would require that the radiators be grouped in such as way as to obtain optimum coverage by the fans. With small transformers of this class, much of the tank surface is normally taken up with cable boxes, so that very little flexibility remains for location of radiators. For units of around 30 MVA the system becomes a more feasible option, particularly at 132/33 kV where connections are frequently via bushings on the tank cover rather than cable boxes on the sides. One problem with this arrangement is that in order to provide space below the radiator for installation



(a) (b)

Figure 4.104 Two views of a 4/8 MVA, three-phase, 33/11 kV, 50 Hz transformer with tank-mounted radiators of a fan the height of the radiator must be reduced, so that the area for self-cooling is reduced, the alternative of hanging the fans from the side of the radiators requires that careful consideration be given to the grouping of these to ensure that the fans blow a significant area of the radiator surface.

Figure 4.104 shows two views of a small 33/11 kV unit with tank-mounted radiators having side mounted fans. By clever design it has been possible to include an oil pump in the cooling circuit to provide forced circulation and, because the unit has been designed for low losses, only two radiators are necessary, leaving plenty of room for cable boxes. Note, however, that these are significantly higher than the transformer tank. The transformer has an ONAN rating of 4 MVA which can be increased to 8 MVA with the pump and fans in operation.

It is frequently a problem to accommodate tank-mounted radiators while leaving adequate space for access to cable boxes, the pressure relief vent pipe and the like. The cooling-surface area can be increased by increasing the number of passes on the radiators, but there is a limit to the extent to which this can be done, dictated by the weight which can be hung from the top and bottom headers. If fans are to be hung from the radiators this further increases the cantilever load. It is possible to make the radiators slightly higher than the tank so that the top header has a swan-necked shape: this has the added benefit that it also improves the oil circulation by increasing the thermal head developed in the radiator. However, this arrangement also increases the overhung weight and has the disadvantage that a swan-necked header is not as rigid as a straight header, so that the weight-bearing limit is probably reached sooner. The permissible overhang on the radiators can be increased by providing a small stool at the outboard end, so that a proportion of the weight bears directly onto the transformer plinth; however, since this support is not available during transport, one of the major benefits from tank-mounted radiators, namely, the ability to transport the transformer full of oil and fully assembled, is lost.

On all but the smallest transformers each radiator should be provided with isolating valves in the top and bottom headers as well as drain and venting plugs, so that it can be isolated, drained and removed should it leak. The valves may be of the cam-operated butterfly pattern and, if the radiator is not replaced immediately, should be backed up by fitting of blanking plates with gaskets.

Radiator leakage can arise from corrosion of the thin sheet steel, and measures should be taken to protect against this. Because of their construction it is very difficult to prepare the surface adequately and to apply paint protection to radiators under site conditions, so that if the original paint finish has been allowed to deteriorate, either due to weather conditions or from damage in transit, it can become a major problem to make this good. This is particularly so at coastal sites. Many users specify that sheet-steel radiators must be hot-dip galvanised in the manufacturer's works prior to receiving an etch prime, followed by the usual paint treatment in the works.

Separate cooler banks

As already indicated, one of the problems with tank-mounted radiators is that a stage is reached when it becomes difficult to accommodate all the required radiators on the tank surface, particularly if a significant proportion of this is taken up with cable boxes. In addition, with the radiators mounted on the tank, the only straightforward option for forced cooling is the use of forced or induced draught fans, and, as was explained in Section 5 of this chapter, the greater benefits in terms of increasing rating are gained by forcing and directing the oil flow. It is possible to mount radiators, usually in groups of three, around the tank on small sub-headers with an oil circulating pump supplying each of these sub-headers as shown in *Figure 4.105*. This is an arrangement used by many utilities worldwide. It has the advantage that the unit can be despatched from the works virtually complete and ready for service. The major disadvantage is the larger number of fans and their associated control gear which must be provided compared with an arrangement using a separate free-standing cooler bank. It is therefore worthwhile considering the merits and disadvantages of mounting all cooler equipment on the tank compared with a separate free-standing cooler arrangement favoured by many utilities in the UK.

Advantages of all tank-mounted equipment

- More compact arrangement saves space on site.
- The transformer can be transported ready filled and assembled as a single entity, which considerably reduces site-erection work.
- The saving of pipework and headers and frame/support structure reduces the first cost of the transformer.

Disadvantages

- Forced cooling must usually be restricted to fans only, due to the complication involved in providing a pumped oil system. If oil pumps are used a large number are required with a lot of control gear.
- Access to the transformer tank and to the radiators themselves for maintenance/painting is extremely difficult.
- A noise-attenuating enclosure cannot be fitted close to the tank.

If these advantages are examined more closely, it becomes apparent that these may be less real than at first sight. Although the transformer itself might well be more compact, if it is to achieve any significant increase in rating from forced cooling, a large number of fans will be required, and a considerable unrestricted space must be left around the unit to ensure a free airflow without the danger of recirculation. In addition, since the use of forced and directed oil allows a very much more efficient forced cooled design to be produced, the apparent saving in pipework and cooler structure can be easily offset. Looking at the disadvantages, the inability to fit a noise-attenuating

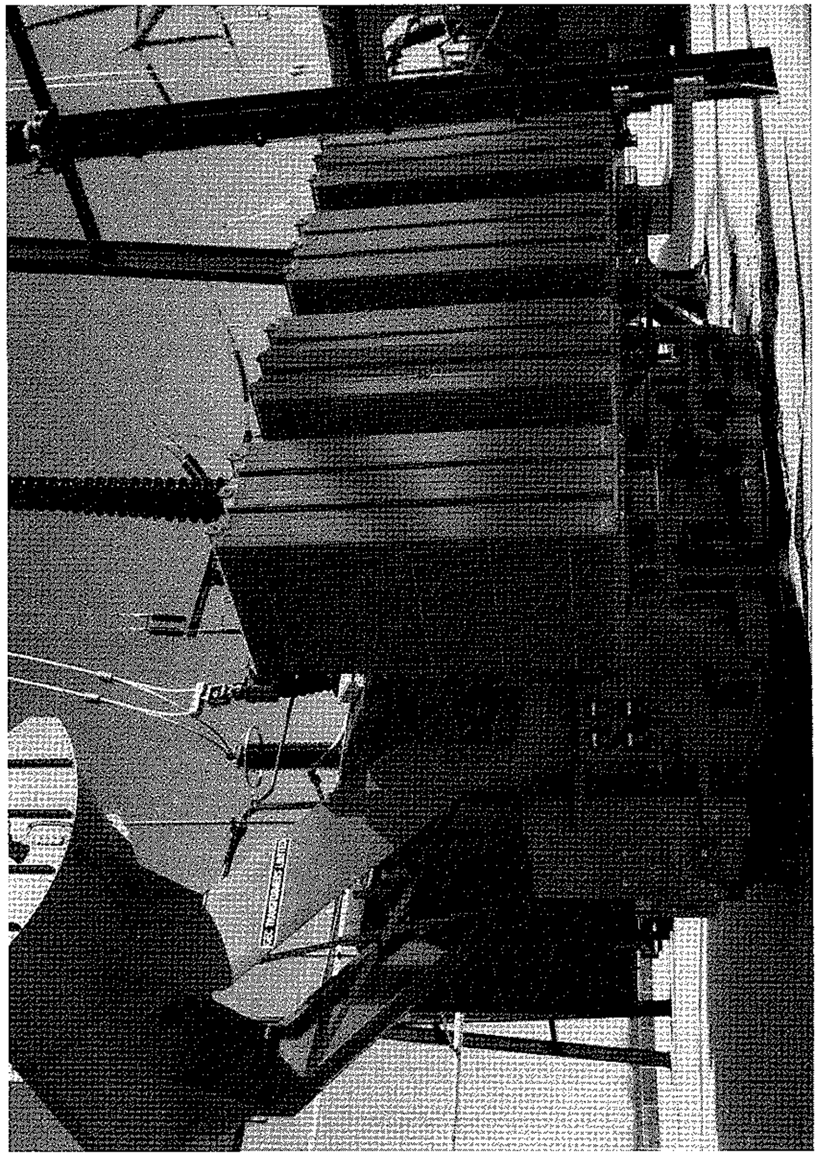


Figure 4.105 Single phase 765/242 kV 300 MVA autotransformer showing tank mounted radiators in groups on sub-headers with oil pumps and fans (GEC Alsthom)

enclosure can be a serious problem for larger transformers as environmental considerations acquire increasingly more prominence.

The protagonists of tank-mounted radiators tend to use bushings mounted on the tank cover for both HV and LV connections, thus leaving the tank side almost entirely free for radiators.

Having stated the arguments in favour of free-standing cooler banks, it is appropriate to consider the merits and disadvantages of forced cooling as against natural cooling.

The adoption of ODAF cooling for, say, a 60 MVA bulk supplies transformer, incurs the operating cost of pumps and fans, as well as their additional first cost and that of the necessary control gear and cabling. Also, the inherent reliability is lower with a transformer which relies on electrically driven auxiliary equipment compared with an ONAN transformer which has none. On the credit side, there is a considerable reduction in the plan area of the cooler bank, resulting in significant space saving for the overall layout. A typical ONAN/ODAF-cooled bulk supplies transformer is rated to deliver full output for conditions of peak system loading and then only when the substation of which it forms part is close to its maximum design load, i.e. near to requiring reinforcement, so for most of its life the loading will be no more than its 30 MVA ONAN rating. Under these circumstances, it is reasonable to accept the theoretical reduction in reliability and the occasional cooler equipment losses as a fair price for the saving in space. On the other hand, a 50 MVA unit transformer at a power station normally operates at or near to full output whenever its associated generator is on load, so reliance on other ancillary equipment is less desirable and, if at all possible, it is preferable to find space in the power station layout to enable it to be totally naturally cooled.

Where a transformer is provided with a separate free-standing cooler bank, it is possible to raise the level of the radiators to a height which will create an adequate thermal head to ensure optimum natural circulation. The longest available radiators can be used to minimise the plan area of the bank consistent with maintaining a sufficient area to allow the required number of fans to be fitted. It is usual to specify that full forced cooled output can be obtained with one fan out of action. Similarly, pump failure should be catered for by the provision of two pumps, each capable of delivering full flow. If these are installed in parallel branches of cooler pipework, then it is necessary to ensure that the non-running pump branch cannot provide a return path for the oil, thus allowing this to bypass the transformer tank. Normally this would be achieved by incorporating a non-return valve in each branch. However, such a valve could create too much head loss to allow the natural circulation necessary to provide an ONAN rating. One solution is to use a flap valve of the type shown in *Figure 4.106*, which provides the same function when a pump is running but will take up a central position with minimal head loss for thermally-induced natural circulation.

Water cooling

Water cooling of the oil is an option which is available for large transformers and in the past was a common choice of cooling for many power station transformers, including practically all generator transformers and many station and unit transformers. It is also convenient in the case of large furnace transformers, for example, where, of necessity, the transformers must be close

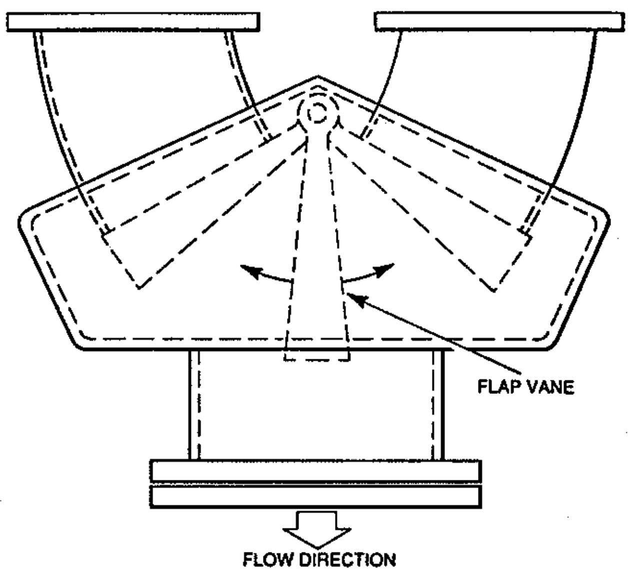


Figure 4.106 Oil flap value

to the load – the furnace – but in this location ambients are not generally conducive to efficient air cooling. The choice of oil/water was equally logical for power station transformers since there is usually an ample source of cooling water available in the vicinity and oil/water heat exchangers are compact and thermally efficient. The arrangement does not provide for a self-cooled rating, since the head loss in oil/water heat exchangers precludes natural oil circulation, but a self-cooled rating is only an option in the case of the station transformer anyway. Generally when the unit is on load both generator and unit transformers are near to fully loaded.

The risk of water entering the transformer tank due to a cooler leak has long been recognised as the principle hazard associated with water cooling. This is normally avoided by ensuring that the oil pressure is at all times greater than that of the water, so that leakage will always be in the direction of oil into water. It is difficult to ensure that this pressure difference is maintained under all possible conditions of operation and malfunction. Under normal conditions, the height of the transformer conservator tank can be arranged such that the minimum oil head will always be above that of the water. However, it is difficult to make allowance for operational errors, for example the wrong valve being closed, so that maximum pump discharge pressure is applied to an oil/water interface, or for equipment faults, such as a pressure reducing valve which sticks open at full pressure.

The precise cost of cooling water depends on the source, but at power stations it is often pumped from river or sea and when the cost of this is taken into consideration, the economics of water cooling become far less certain.

In the early 1970s, after a major generator transformer failure attributable to water entering the oil through cooler leaks, the UK Central Electricity Generating Board reassessed the merits of use of water cooling. The high cost of the failure, both in terms of increased generating costs due to the need to operate lower-merit plant and the repair costs, as well as pumping costs, resulted in a decision to adopt an induced draught air-cooled arrangement for the Littlebrook D generator transformers and this subsequently became the standard, whenever practicable.

In water cooling installations, it is common practice to use devices such as pressure reducing valves or orifice plates to reduce the waterside pressures. However, no matter how reliable a pressure reducing valve might be, the time will come when it will fail, and an orifice plate will only produce a pressure reduction with water flowing through it, so that should a fault occur which prevents the flow, full pressure will be applied to the system.

There are still occasions when it would be very inconvenient to avoid water cooling, for example in the case of furnace transformers mentioned above. Another example is the former CEGB's Dinorwig pumped-storage power station now owned by First Hydro where the generator transformers are located underground, making air cooling impracticable on grounds of space and noise as well as the undesirability of releasing large quantities of heat to the cavern environment. *Figure 4.107* shows a diagrammatic arrangement

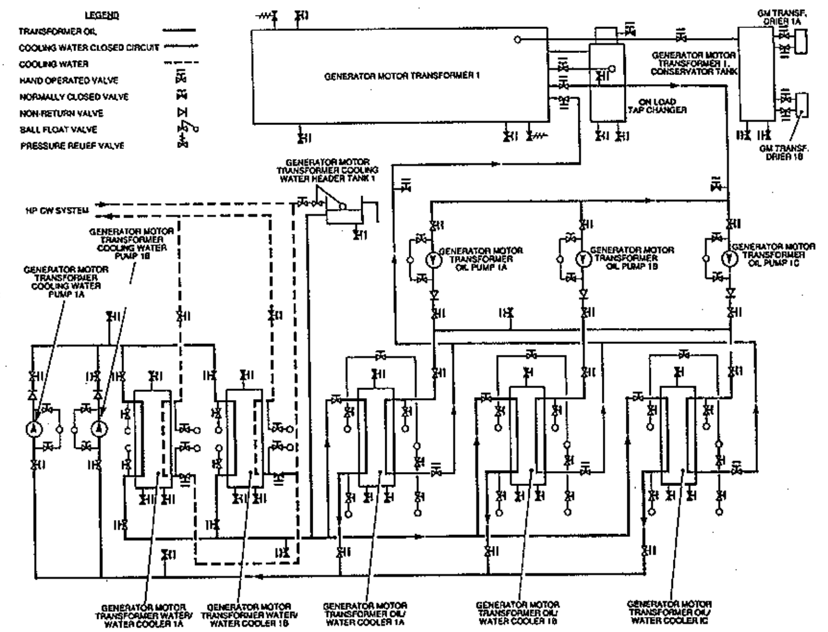


Figure 4.107 Diagrammatic arrangement of Dinorwig generator transformer cooler circuits

of the cooling adopted for the Dinorwig generator transformers. This uses a two-stage arrangement having oil/towns-water heat exchangers as the first stage, with second-stage water/water heat exchangers having high-pressure lake-water cooling the intermediate towns water. The use of the intermediate stage with recirculating towns water enables the pressure of this water to be closely controlled and, being towns water, waterside corrosion/erosion of the oil/water heat exchangers – the most likely cause of cooler leaks – is also kept very much under control. Pressure control is ensured by the use of a header tank maintained at atmospheric pressure. The level in this tank is topped up via the ball valve and a very generously sized overflow is provided so that, if this valve should stick open, the header tank will not become pressurised. The position of the water pump in the circuit and the direction of flow is such that should the water outlet valve of the oil/water heat exchanger be inadvertently closed, this too would not cause pressurisation of the heat exchanger. A float switch in the header tank connected to provide a high level alarm warns of either failure of the ball valve or leakage of the raw lake water into the intermediate towns-water circuit.

Other situations in which water cooling is justified such as those in which the ambient air temperature is high, so that a significantly greater temperature rise of the transformer can be permitted if water cooling is employed, might use an arrangement similar to that for Dinorwig described above, or alternatively, a double-tube/double-tubeplate cooler might be employed. With such an arrangement, shown diagrammatically in *Figure 4.108*, oil and water circuits are separated by an interspace so that any fluid leakage will be collected in this space and will raise an alarm. Coolers of this type are, of course, significantly more expensive than simple single-tube and plate types and heat transfer is not quite so efficient, so it is necessary to consider the economics carefully before adopting a double-tube/double-tubeplate cooler in preference to an air-cooled arrangement.

Another possible option which might be considered in a situation where water cooling appears preferable is the use of sophisticated materials, for example titanium-tubed coolers. This is usually less economic than a double-tubed/double-tubeplate cooler as described above.

Passing mention has been made of the need to avoid both corrosion and erosion of the waterside of cooler tubes. A third problem which can arise is the formation of deposits on the waterside of cooler tubes which impair heat transfer. The avoidance of all of these requires careful attention to the design of the cooling system and to carefully controlled operation. Corrosion problems can be minimised by correct selection of tube and tubeplate materials to suit the analysis of the cooling water. Deposition is avoided by ensuring that an adequate rate of water flow is maintained, but allowing this to become excessive will lead to tube erosion.

If the cooling medium is sea water, corrosion problems can be aggravated and these might require the use of measures, such as the installation of sacrificial anodes or cathodic protection. These measures have been used with

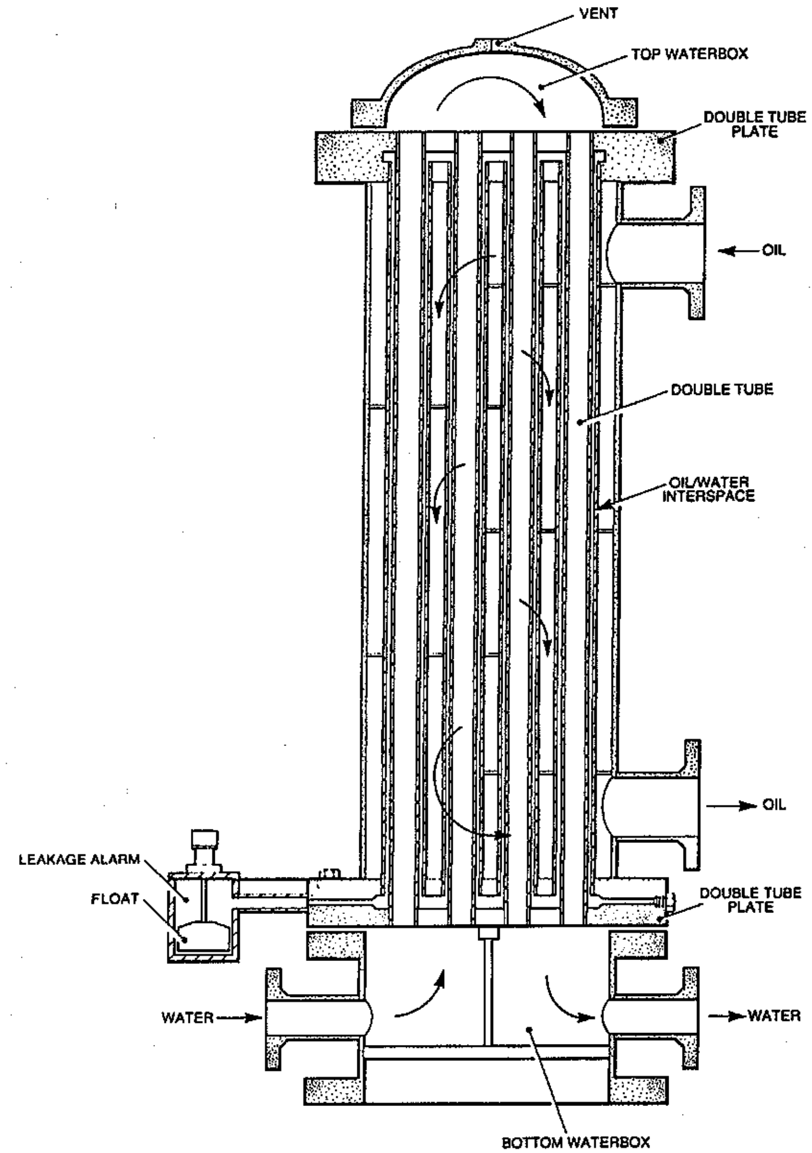


Figure 4.108 Double tube, double tubeplate oil/water heat exchanger

success in UK power stations, but it is important to recognise that they impose a very much greater burden on maintenance staff than does an air cooler, and the consequences of a small amount of neglect can be disastrous.

A fan and its control equipment can operate continuously or under automatic control for periods of two or three years or more, and maintenance usually means no more than greasing bearings and inspection of contactor contacts. By contrast, to ensure maximum freedom from leaks, most operators of oil/water heat exchangers in UK power stations routinely strip them down annually to inspect tubes, tubeplates and water boxes. Each tube is then non-destructively tested for wall thickness and freedom from defects, using an eddy-current probe. Suspect tubes can be blanked off but, since it will only be permissible to blank off a small proportion of these without impairing cooling, a stage can be reached when complete replacement tubenests are necessary.

In view of the significant maintenance requirement on oil/water heat exchangers, it is advisable to provide a spare cooler and standard practice has, therefore, been to install three 50%-rated coolers, one of which will be kept in a wet standby condition, i.e. with the oil side full of transformer oil and with the water side inlet and outlet valves closed but full of clean water, and the other two in service.

Cooler control

Ancillary plant to control and operate forced cooling plant must be provided with auxiliary power supplies and the means of control. At its most basic, this takes the form of manual switching at a local marshalling panel, housing auxiliary power supplies, fuses, overloads protection relays and contactors. In many utilities due to high labour costs the philosophy has been to reduce the amount of at-plant operator control and so it is usual to provide remote and/or automatic operation.

The simplest form of automatic control uses the contacts of a winding temperature indicator to initiate the starting and stopping of pumps and fans. Further sophistication can be introduced to limit the extent of forced cooling lost should a pump or fan fail. One approach is to subdivide the cooler bank into two halves, using two 50%-rated pumps and two sets of fans. Equipment failure would thus normally not result in loss of more than half of the forced cooling. As has been explained above, many forced-cooled transformers have a rating which is adequate for normal system operation when totally self-cooled, so an arrangement which requires slightly less pipework having parallel 100%-rated duty and standby pumps, as shown in *Figure 4.109*, can be advantageous. This means that flow switches must be provided to sense the failure of a duty pump and to initiate start-up of the standby should the winding temperature sense that forced cooling is required.

A large generator transformer has virtually no self-cooled rating, so pumps can be initiated from a voltage-sensing relay, fed from a voltage transformer which is energised whenever the transformer is energised. Two 100% duty and standby oil pumps are provided, with automatic initiation of the standby pump should flow failure be detected on the duty pump. Fans may still be controlled from a winding temperature indicator, but it is usual to divide these into two groups initiated in stages, the first group being switched on at a winding temperature of 80 °C and out at 70 °C. The second group is switched on at 95 °C and out at 80 °C. The total number of fans provided is such that

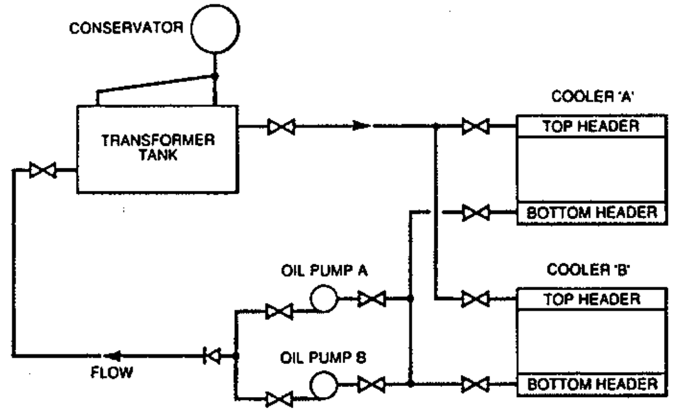


Figure 4.109 Oil circuit for ONAN/ODAF-cooled unit transformer

failure of any one fan still enables full rating to be achieved with an ambient temperature of 30 °C. The control scheme also allows each oil pump to serve either in the duty or standby mode and the fans to be selected for either first- or second-stage temperature operation. A multiposition mode selector switch allows both pumps and fans to be selected for 'test' to check the operation of the control circuitry. The scheme is also provided with 'indication' and 'alarm' relay contacts connected to the station data processor.

For water-cooled generator transformers, the fans are replaced by water pumps which are controlled from voltage transformer signals in the same way as the oil pumps. Two 100% duty and standby pumps are provided, with the standby initiated from a flow switch detecting loss of flow from the selected duty pump.

There is a view that automatic control of generator transformer air coolers is unnecessary and that these should run continuously whenever the generator transformer is energised. This would simplify control arrangements and reduce equipment costs but there is an operational cost for auxiliary power. Modern fans have a high reliability, so they can be run for long periods continuously without attention. For many large generator transformers, running of fans (whether required or not) results in a reduction of transformer load loss, due to the reduced winding temperature, which more than offsets the additional fan power requirement, so that this method of operation actually reduces operating cost. In addition, the lower winding temperature reduces the rate of usage of the transformer insulation life. An example will assist in making this clear.

An 800 MVA generator transformer might typically operate at a throughput of 660 MW and 200 MVAr, which is equivalent to 690 MVA. At 800 MVA, it will have resistance rise and top-oil rise of 70° and 60 °C, respectively, if the manufacturer has designed these to the BS limits. At 690 MVA, these could be reduced to 45 °C and 41 °C, respectively, dependent on the particular design. Then, as explained in Section 5 of this chapter, the winding hot-spot temperature at an ambient temperature of, say, 10 °C will be given by:

Ambient	10
Rise by resistance	45
Half (outlet–inlet) oil	6
Maximum gradient–average gradient	4
Total	65 °C

At this ambient, the first fan group will operate under automatic control, tripping in when the hot-spot temperature reaches 80 °C and out at 70 °C. It is reasonable to assume, therefore, that with these fans running intermittently, an average temperature of 75 °C will be maintained. Hence, continuous running of all fans will achieve a temperature reduction of about 10 °C.

For an actual case estimating the extra auxiliary power absorbed by running the fans continuously would probably involve making observations of operation in the automatic control mode first. However, by way of illustration, it is convenient to make some very approximate estimates.

The power absorbed by 12 fans on a transformer of this rating might typically be 36 kW. If, at this ambient, the first group would run for about 80% of the time and the second group would not run at all, the average auxiliary power absorbed would be 0.8 times 18 kW, equals 14.4 kW, say 15 kW. Running them all continuously therefore absorbs an extra (36 − 15) kW equals 21 kW.

The load loss of an 800 MVA generator transformer at rated power could be 1600 kW. At 690 MVA this would be reduced to about 1190 kW. If it is assumed that 85% of this figure represents resistive loss, then this equates to 1012 kW, approximately. A 10 °C reduction in the average winding temperature would produce a reduction of resistance at 75 °C of about 3.3%, hence about 33.4 kW of load loss would be saved. Strictly speaking, this reduction in resistance would cause an approximately 3.3% increase in the other 15% of the load losses, that is, about 6 kW additional stray losses would be incurred, so that the total power saved would be 33.4 kW at a cost of (21 + 6) equals 27 kW, i.e. 6.4 kW net saving. However, the figures used are only very approximate but they demonstrate that the cost of the increased auxiliary power is largely offset by load loss savings. The important feature, though, is that the lower hot-spot temperature increases insulation life. For example, referring to Section 5 of this chapter, the 10 °C reduction obtained in the above example would, theoretically, increase the life of the insulation somewhere between three and fourfold.

Winding temperature indicators

In the foregoing paragraphs mention was made of control of cooling equipment from a winding temperature indicator. Before leaving this section dealing with ancillary equipment it is perhaps appropriate to say a little more about winding

temperature indicators, or more precisely, transformer temperature controllers. One such device is shown in *Figure 4.110*. This consists of a liquid-filled bulb at the end of a steel capillary. The bulb is placed in the hottest oil in the top of the transformer tank and the capillary is taken to the transformer marshalling cubicle where it terminates in a steel bellows unit within the temperature controller. The controller contains a second bellows unit connected to another capillary which follows the same route as that from the transformer tank but this has no bulb at its remote end and it acts as a means of compensation for variations in ambient temperature, since with changes in ambient the liquid in both capillaries expands or contracts with respect to the capillaries and both bellows therefore move together. For changes in oil temperature only the bellows connected to the bulb will move. Movement of both sets of bellows has no effect on the mechanism of the instrument while movement only of the bellows connected to the bulb causes the rotation of a temperature indicating pointer and a rotating disc which carries up to four mercury switches. The pointer can be set to give a local visual indication of oil temperature and the mercury switches can be individually set to change over at predetermined temperature settings. The mercury switches can thus provide oil temperature alarm and trip signals and also a means of sending a start signal to pumps

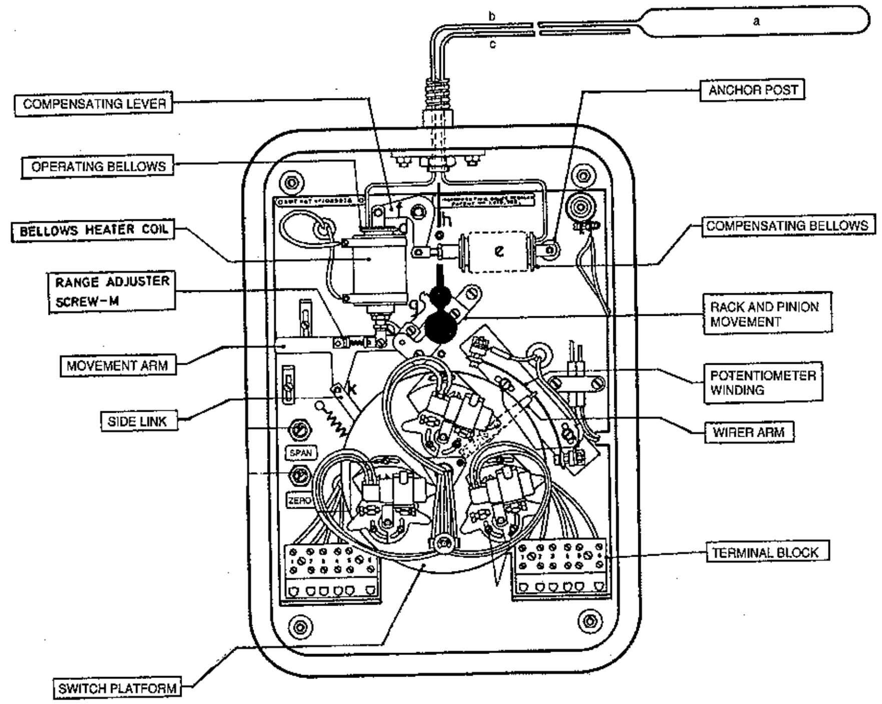


Figure 4.110 Transformers temperature controller (Accurate Controls Ltd)

and/or fans. The pointer is also connected to a potentiometer which can be used to provide remote indication of temperature. If it is required to have an indication of winding temperature the sensing bulb can be located in the hottest oil but surrounded by a heater coil supplied from a current transformer in either HV or LV winding leads. The heater coil is then designed to produce a temperature rise above hottest oil equivalent to the temperature rise of the HV or LV hot spot above the hottest oil. This is known as a *thermal image* device. The heater coil is provided with an adjustable shunt so that the precise thermal image can be set by shunting a portion of the CT output current. Of course, the setting of this heater coil current requires that the designer is able to make an accurate estimate of the hot-spot rise, and, as indicated in Section 5 of this chapter, this might not always be the case. If the transformer is subjected to a temperature rise test in the works, it is usual practice to carry out a final setting of the winding temperature indicators after the individual winding temperature rises have been calculated. On larger transformers one each will be provided for HV and LV windings.

4.9 PROCESSING AND DRY-OUT

The paper insulation and pressboard material, which make up a significant proportion by volume of transformer windings, have the capacity to absorb large amounts of moisture from the atmosphere. The presence of this moisture brings about a reduction in the dielectric strength of the material and also an increase in its volume. The increase in volume is such that, on a large transformer, until the windings have been given an initial dry-out, it is impossible to reduce their length sufficiently to fit them on to the leg of the core and to fit the top yoke in place.

The final drying out is commenced either when the core and windings are placed in an autoclave or when they are fitted into their tank, all main connections made, and the tank placed in an oven and connected to the drying system. The tapping switch may be fitted at this stage, or later, depending on the ability of the tapping switch components to withstand the drying process.

Traditional methods of drying out involve heating the windings and insulation to between 85 and 120°C, by circulating heated dry air and finally applying a vacuum to complete the removal of water vapour and air from the interstices of the paper before admitting transformer oil to cover the windings. For a small transformer operating at up to, say, 11 kV, this heating could be carried out by placing the complete unit in a steam or gas-heated oven. For a large transformer the process could take several days, or even weeks, so that nowadays the preference is to use a *vapour-phase* heating system in which a liquid, such as white spirit, is heated and admitted to the transformer tank under low pressure as vapour. This condenses on the core and windings, and as it does so it releases its latent heat of vaporisation, thus causing the tank internals to be rapidly heated. It is necessary to ensure that the insulation does not exceed a temperature of about 130°C to prevent ageing damage: when

this temperature is reached, the white spirit and water vapour is pumped off. Finally, a vacuum equivalent of between 0.2 and 0.5 mbar absolute pressure is applied to the tank to complete the removal of all air and vapours. During this phase, it is necessary to supply further heat to provide the latent heat of vaporisation; this is usually done by heating coils in an autoclave, or by circulating hot air around the tank within the dry-out oven.

The vapour phase dry-out process is similar to systems used previously, the only difference being in the use of the vapour to reduce the heating time. It is not a certain method of achieving a drier transformer and, in fact, it is possible that the drying of large masses of insulation might be less efficient since, being limited by the rate of diffusion of water through the material, it is a process which cannot be speeded up. This is an area where further research might be beneficial. Particular problem areas are laminated pressboard end support structures and laminated wood used in the same location, where moisture will tend to migrate along the laminations rather than cross through the interlaminar layers of adhesive. Designers need to give special consideration to such structures and can often improve the dry-out process by arranging to have holes drilled in places where these will assist the release of moisture without weakening the structure. Another aspect of this system of drying out which requires special attention is that of the compatibility of the transformer components with the heat transfer medium. For example, prior to the use of the vapour phase process, some nylon materials were used for transformer internals, notably in a type of self-locking nut. This nylon is attacked by hot white spirit, so it was necessary to find an alternative.

Even in the case of small transformers, where dry-out will probably be carried out using a heated oven, there is still a need for careful attention in certain difficult areas. One of these is for multilayer high-voltage windings using round conductors. This type of winding usually has a layer of paper insulation between conductor layers. The moisture trapped within this interlayer insulation will have to travel up to half the length of the layer in order to be released to the atmosphere. This can take many hours, even days, at 130°C.

Monitoring insulation dryness during processing usually involves measurement of some parameter which is directly dependent on moisture content. Insulation resistance or power factor would meet this requirement. Since there are no absolute values for these applicable to all transformers, it is usual to plot readings graphically and dry-out is taken to be completed when a levelling out of power factor and a sharp rise in insulation resistance is observed. *Figure 4.111* shows typical insulation resistance and power factor curves obtained during a dry-out. Vacuum is applied when the initial reduction in the rate of change of these parameters is noted: the ability to achieve and maintain the required vacuum, coupled with a reduction and levelling out of the quantity of water removed and supported by the indication given by monitoring of the above parameters, will confirm that the required dryness is being reached. For a vapour phase drying system, since it could be dangerous to monitor electrical parameters, drying termination is identified by monitoring

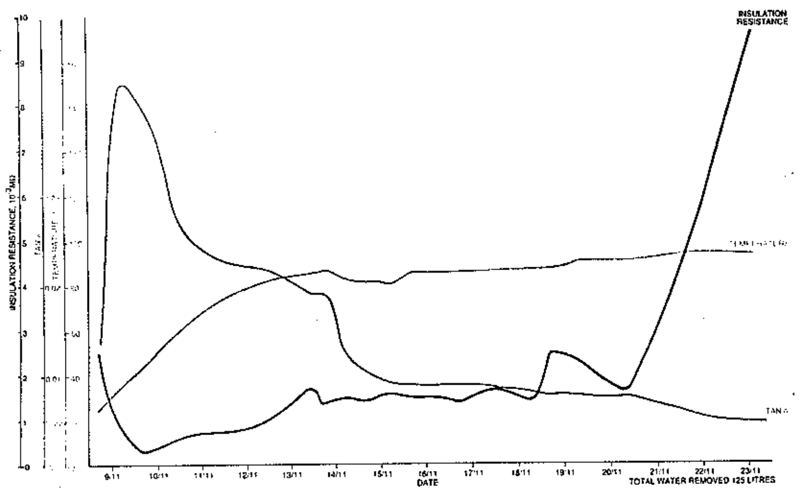


Figure 4.111 Insulation resistance and power factor curves during dry-out

water condensate in the vacuum pumping system. At this point oil filling is begun with dry, filtered degassed oil at a temperature of about 75°C being slowly admitted to the tank and at such a rate as to allow the vacuum already applied to be maintained.

Drying out of insulation is accompanied by significant shrinkage, so it is usual practice for a large transformer to be de-tanked immediately following initial oil impregnation to allow for retightening of all windings, as well as cleats and clamps on all leads and insulation materials. This operation is carried out as quickly as possible in order to reduce the time for which windings are exposed to the atmosphere. However, once they have been impregnated with oil, their tendency to absorb moisture is considerably reduced so that, provided the transformer is not out of its tank for more than about 24 hours, it is not necessary to repeat the dry-out process. On returning the core and windings to the tank, the manufacturer will probably have a rule which says that vacuum should be reapplied for a time equal to that for which they were uncovered, before refilling with hot, filtered, degassed oil.

Before commencement of final works tests, the transformer is then usually left to stand for several days to allow the oil to permeate the insulation fully and any remaining air bubbles to become absorbed by the oil.

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4.2 Shroff, D.H. and Stannett, A.W. (1984) 'A review of paper ageing in power transformers'. *Proc. IEE*, **132**, 312–319.

The remainder of this chapter is devoted to illustrations of typical transformers from the smallest to the largest size (see *Figures 4.11* to *4.141*). These are shown with different types of tanks and with different terminal arrangements, and are typical of modern practice in the design of power transformers.

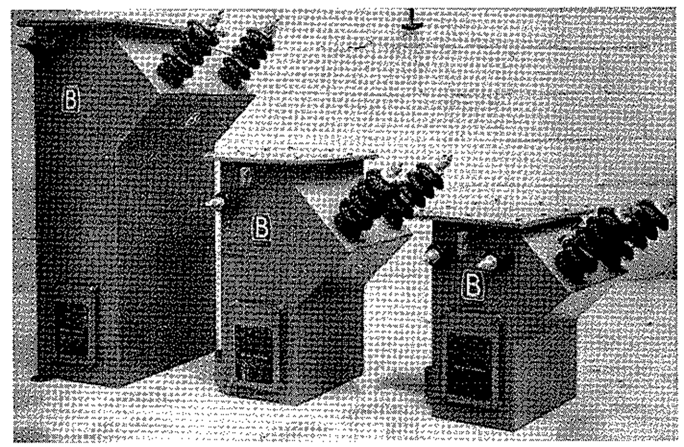


Figure 4.112 Single-phase 11 kV, 50 Hz, pole-mounted transformers. Rated 16–50 kVA (Allenwest Brentford Ltd)

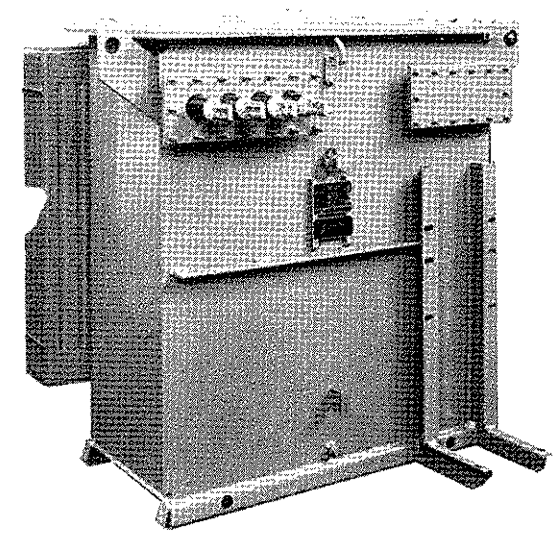


Figure 4.113 Three-phase 500 kVA, 11 kV, 50 Hz substation transformer showing the provision made for mounting LV fusegear on the left and an HV ring main unit on the right (ABB Power T&D Ltd)

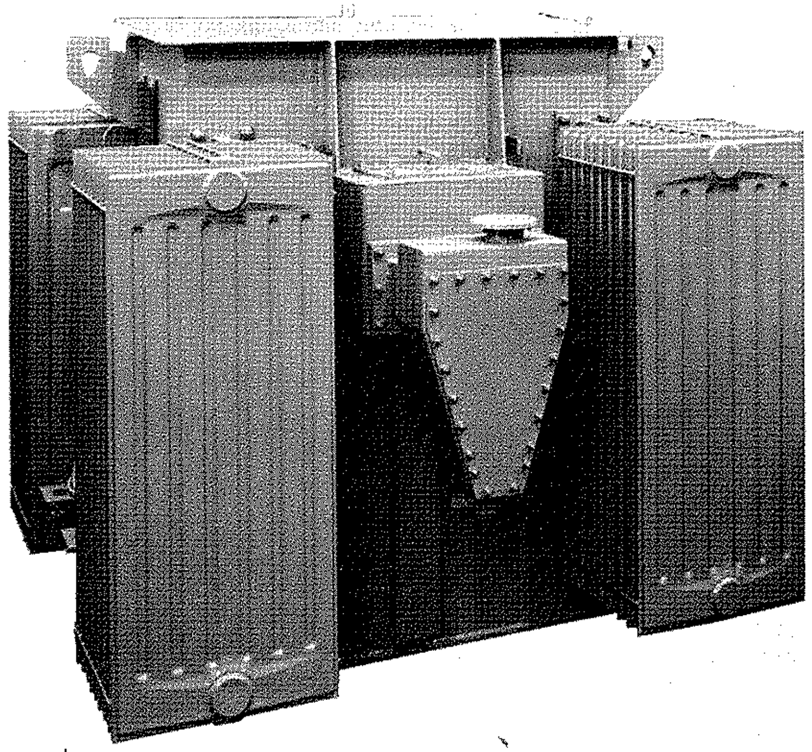


Figure 4.114 Three-phase 750 kVA, 11 000/395 V, 50 Hz sealed-type transformer with welded cover; viewed from HV side. The HV cable box is attached to a disconnecting chamber (ABB Power T&D Ltd)

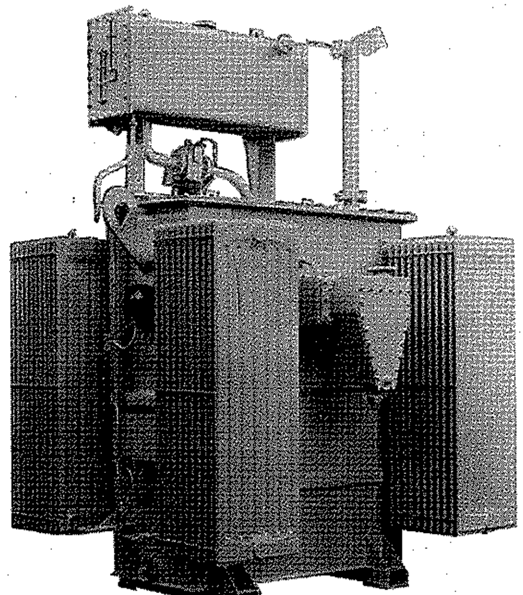


Figure 4.115 Three-phase 750 kVA, 11/3.3 kV transformer fitted with conservator, Buchholz relay and explosion vent. Tappings over a range −2.5% to +7.5% are brought out to an off-circuit selector switch (ABB Power T&D Ltd)

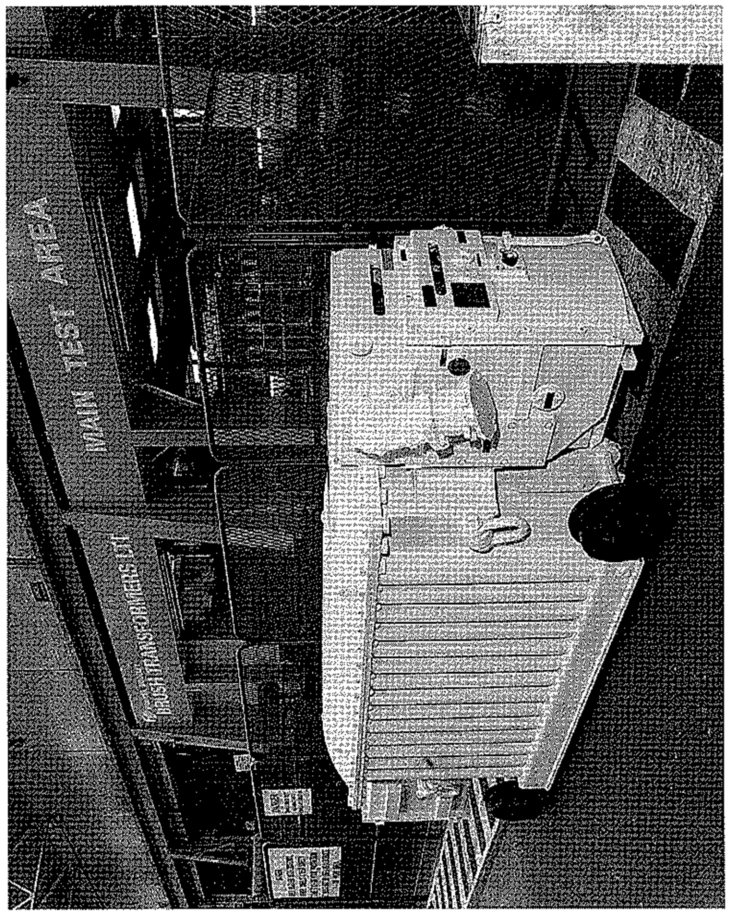


Figure 4.116 Three-phase dry-type mining transformer 3300/1130-565 V, 50 Hz. High-voltage SF_6 switchgear is mounted on the near end of the tank with LV chamber containing earth-leakage equipment at far end (Brush Transformers Ltd)

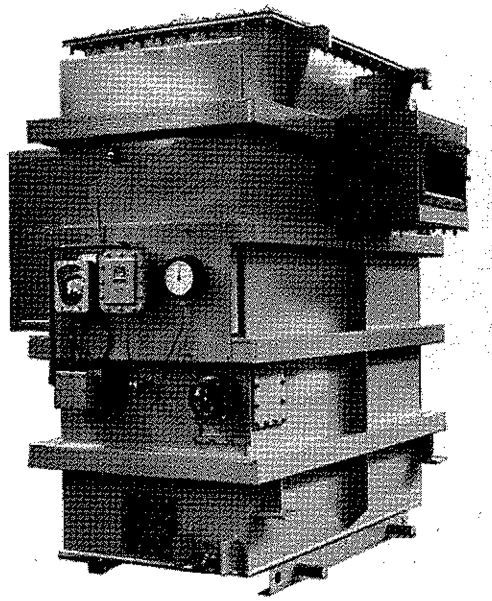


Figure 4.117 Three-phase 11 kV, 50 Hz dry-type nitrogen-fitted sealed transformer (Allenwest Brentford Ltd)

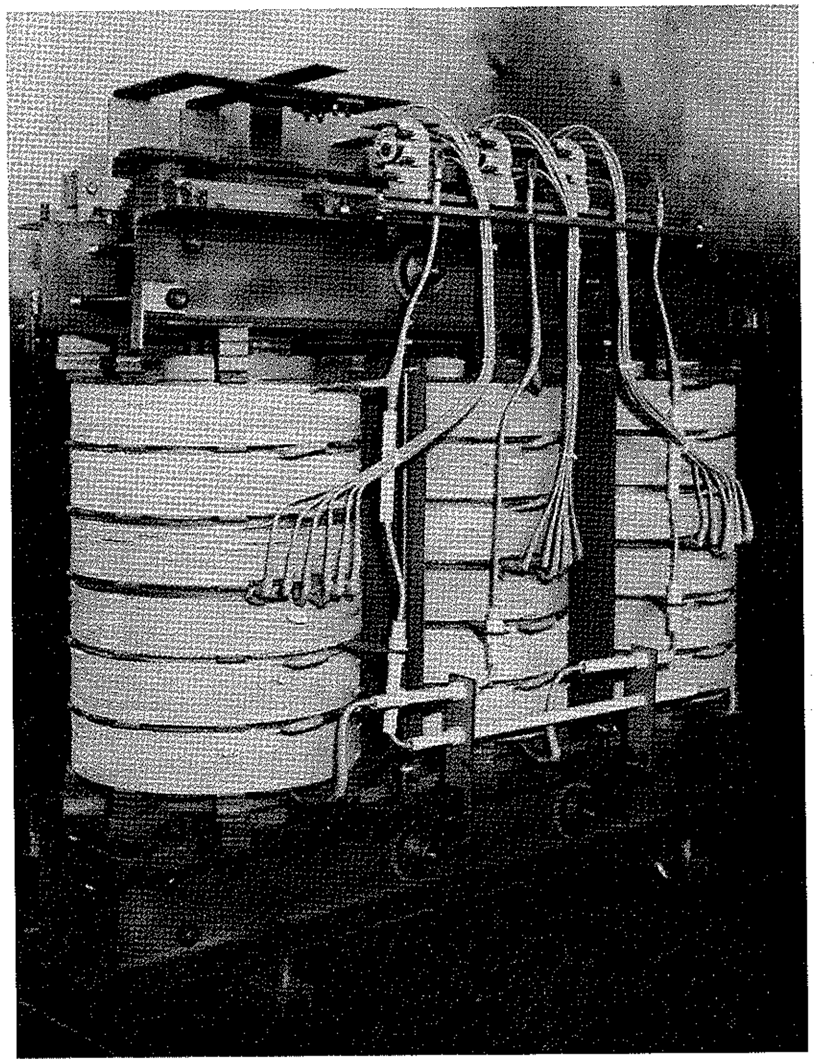


Figure 4.118 Three-phase 1750 kVA, 13800/480 V, 50 Hz core and windings. HV tappings brought to an off-circuit tap selector (Bonar Long Ltd)

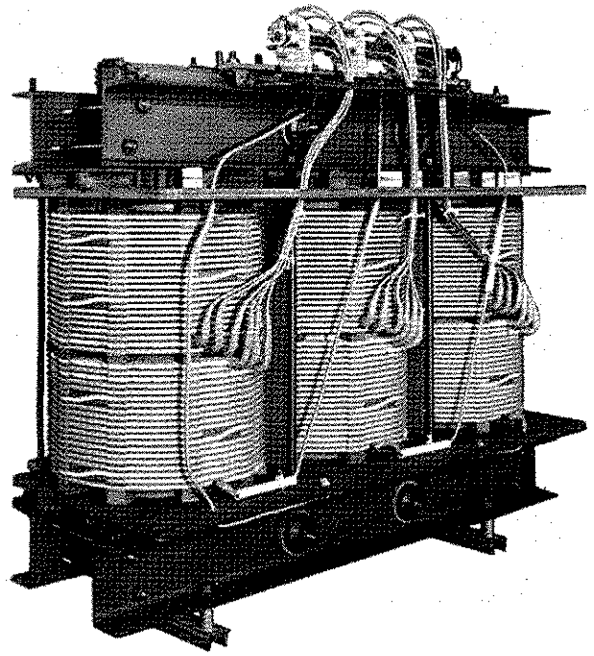


Figure 4.119 Three-phase 1500 kVA, 13.8/3.3 kV, 50 Hz core and windings. HV tappings at ±2.5% and ±5% taken from the HV disc type windings (Bonar Long Ltd)

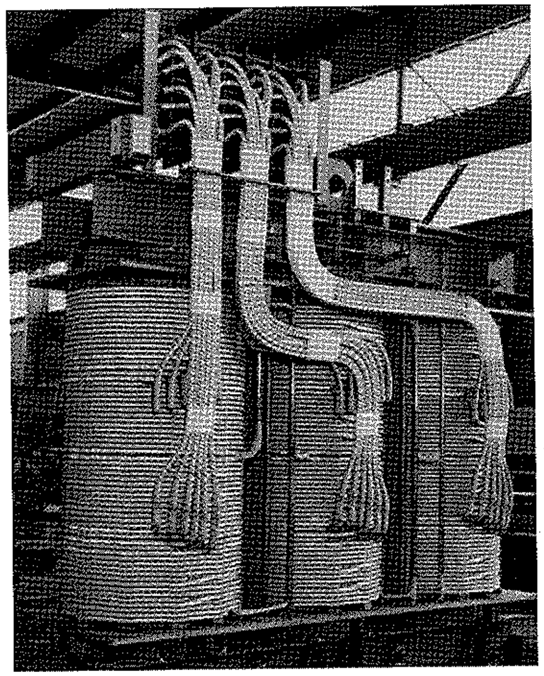


Figure 4.120 Three-phase 6 MVA, 600/3450 V, 50 Hz core and windings with HV tappings brought to an off-circuit selector. The HV disc winding is arranged in two parallel halves to reduce axial forces (ABB Power T&D Ltd)

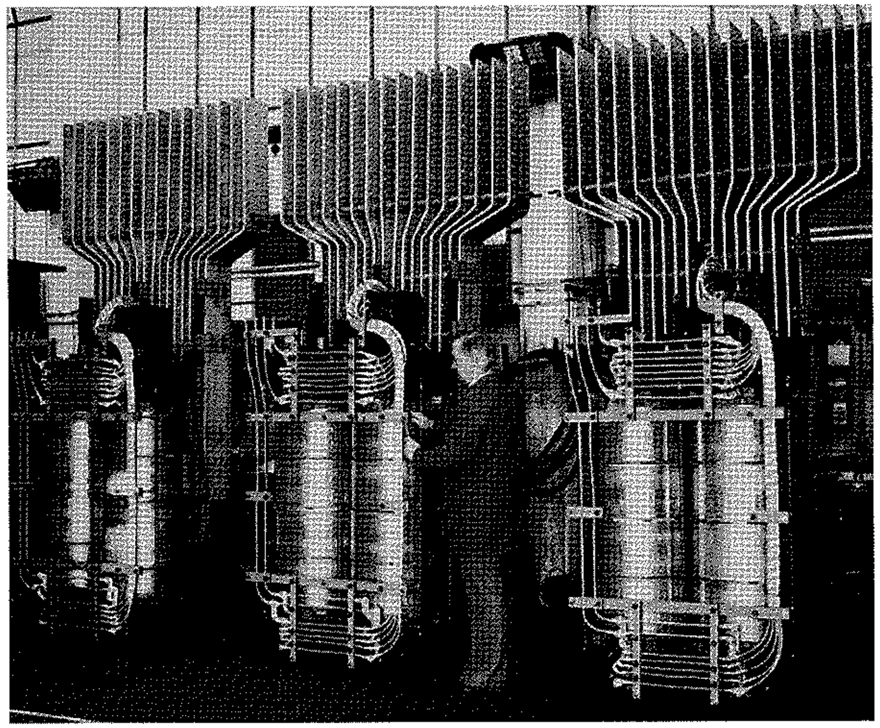


Figure 4.121 Core windings of three single-phase units each rated at 10 000 A and desiged for rectifier testing duty (Allenwest Brentford Ltd)

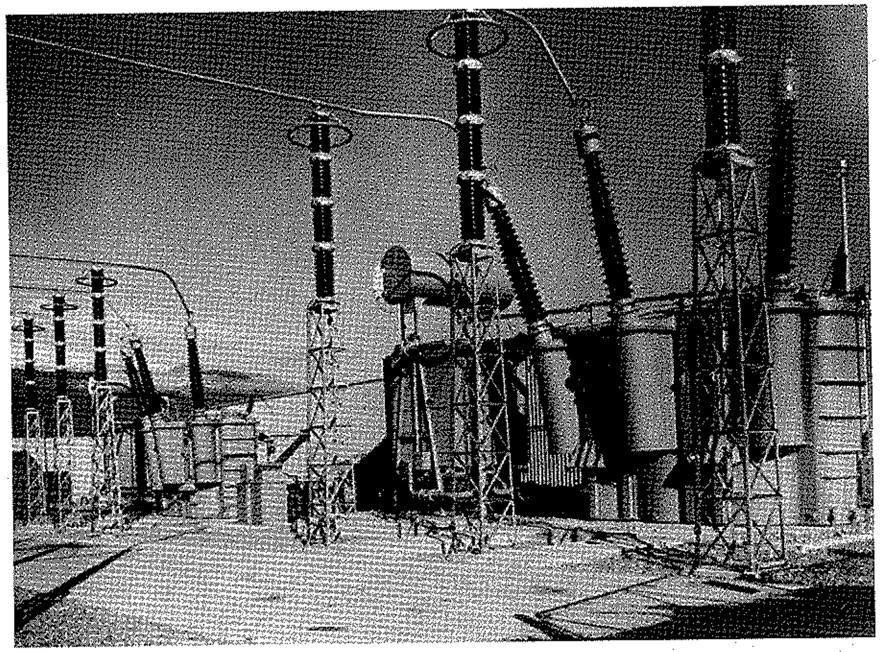


Figure 4.122 Two 90 MVA, 385/18.7 kV units in service at CERN (The European Organisation for Nuclear Research). The units provide power for what is claimed to be the world's largest nuclear particle accelerator; a 400 GeV proton synchrotron. The units have to withstand three million pulses per year at a peak load of 148 MW, 50% above their nominal rating (Hawker Siddeley Power Transformers Ltd)

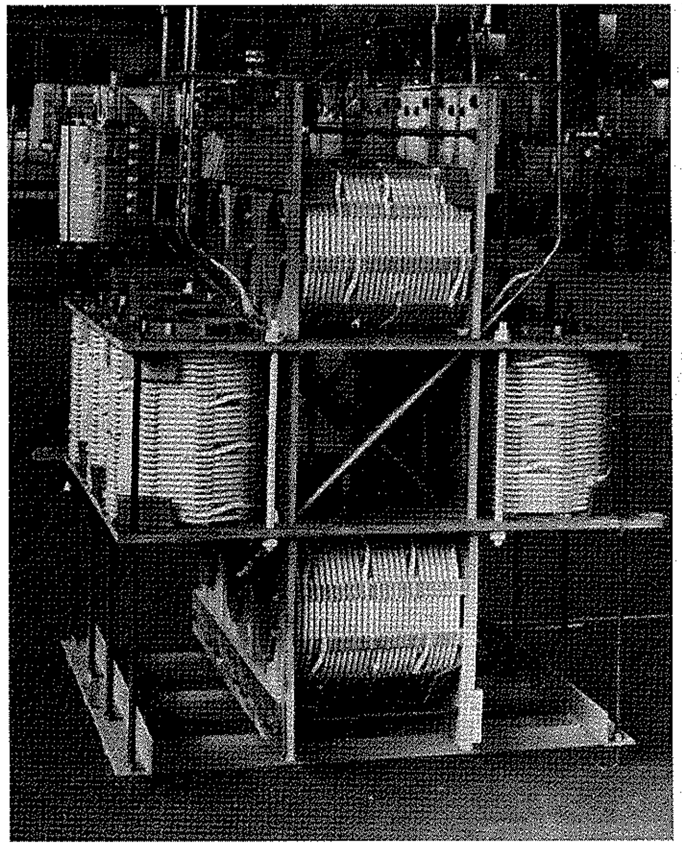


Figure 4.123 Frame and windings of a three-phase air-cored reactor, 20 MVA, 11/6.6 kV, 4% × 50 Hz, shown out of its tank (ABB Power T&D Ltd)

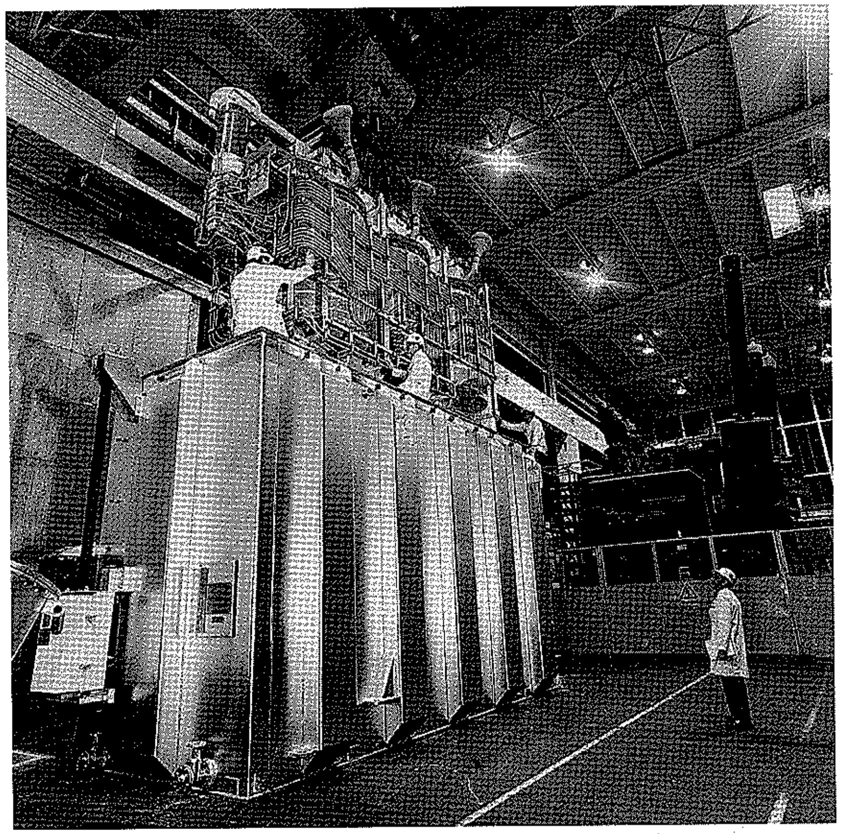


Figure 4.124 Lowering the core and windings of a 148 MVA 275 kV 50 Hz, three-phase generator transformer into its tank (ABB Power T&D Ltd)

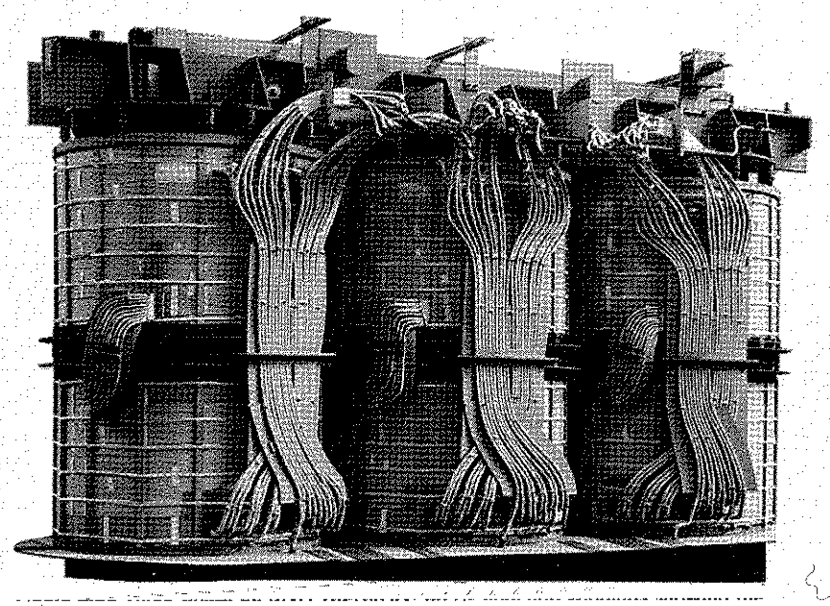


Figure 4.125 Three-phase 60 MVA, 132/33 kV, 50 Hz core and windings showing the outer tapping winding and the tapping leads assembly (ABB Power T&D Ltd)

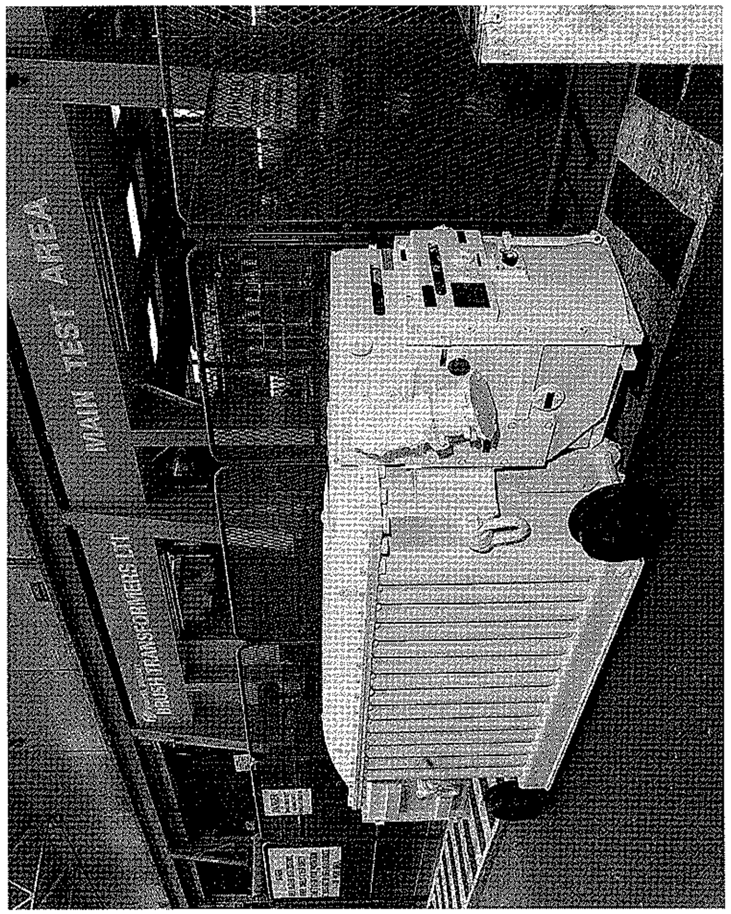


Figure 4.116 Three-phase dry-type mining transformer 3300/1130-565 V, 50 Hz. High-voltage SF_6 switchgear is mounted on the near end of the tank with LV chamber containing earth-leakage equipment at far end (Brush Transformers Ltd)

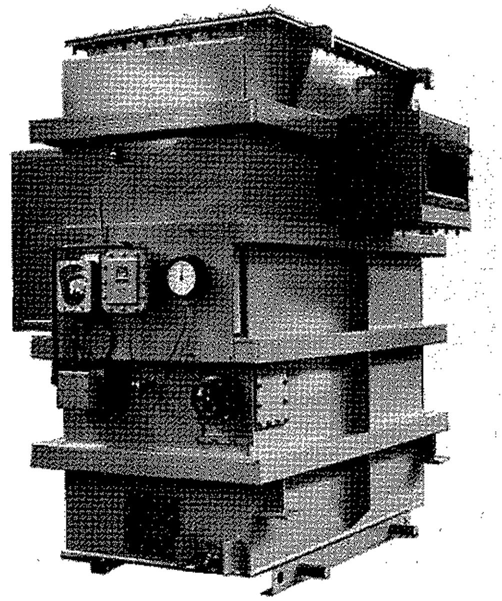


Figure 4.117 Three-phase 11 kV, 50 Hz dry-type nitrogen-fitted sealed transformer (Allenwest Brentford Ltd)

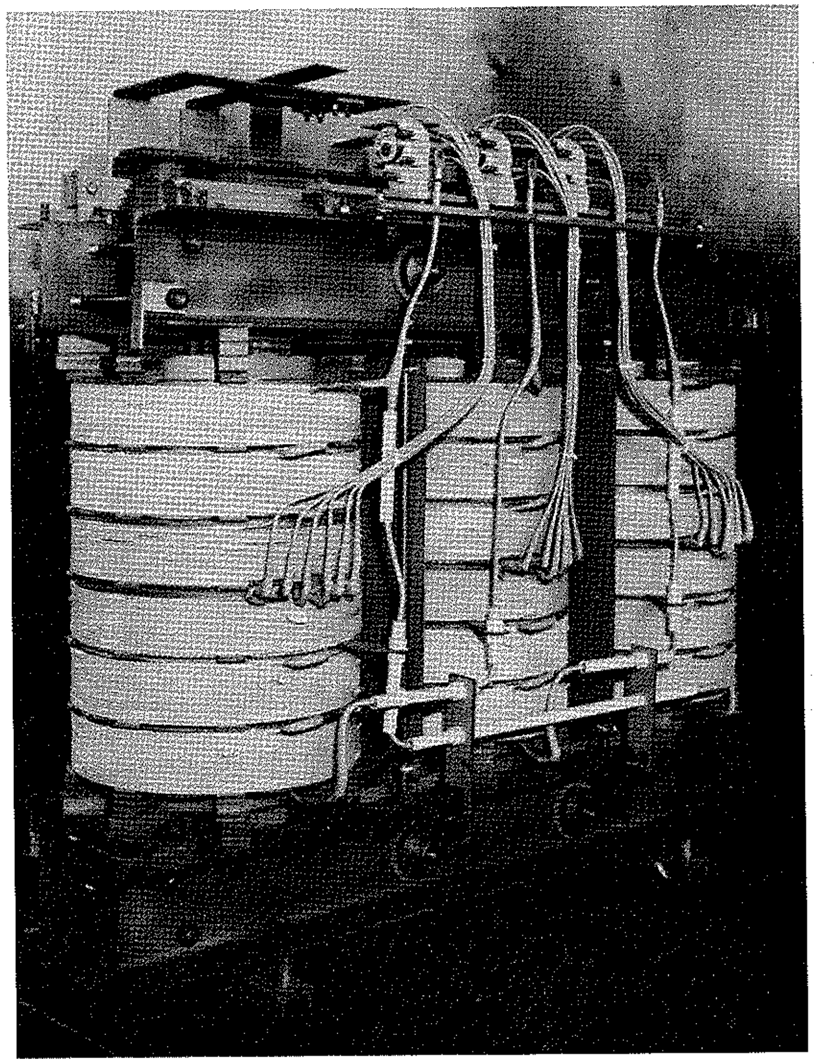


Figure 4.118 Three-phase 1750 kVA, 13800/480 V, 50 Hz core and windings. HV tappings brought to an off-circuit tap selector (Bonar Long Ltd)

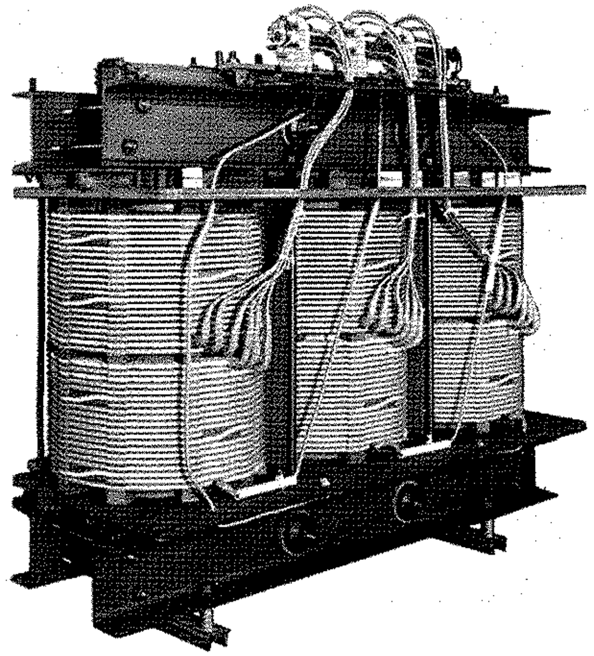


Figure 4.119 Three-phase 1500 kVA, 13.8/3.3 kV, 50 Hz core and windings. HV tappings at ±2.5% and ±5% taken from the HV disc type windings (Bonar Long Ltd)

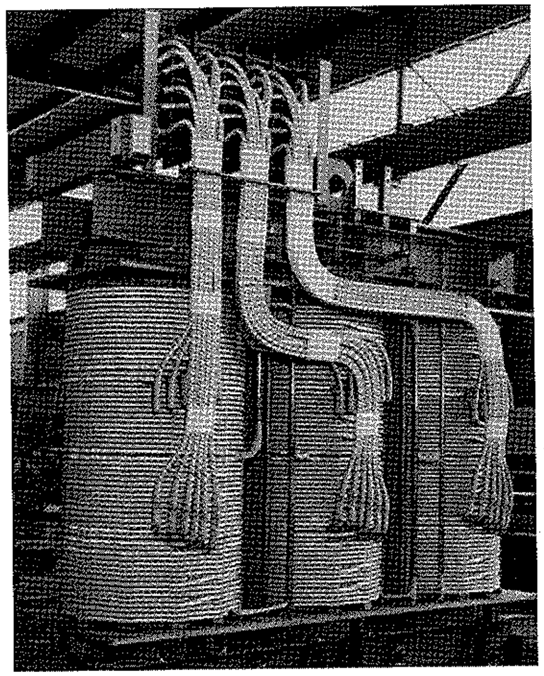


Figure 4.120 Three-phase 6 MVA, 600/3450 V, 50 Hz core and windings with HV tappings brought to an off-circuit selector. The HV disc winding is arranged in two parallel halves to reduce axial forces (ABB Power T&D Ltd)

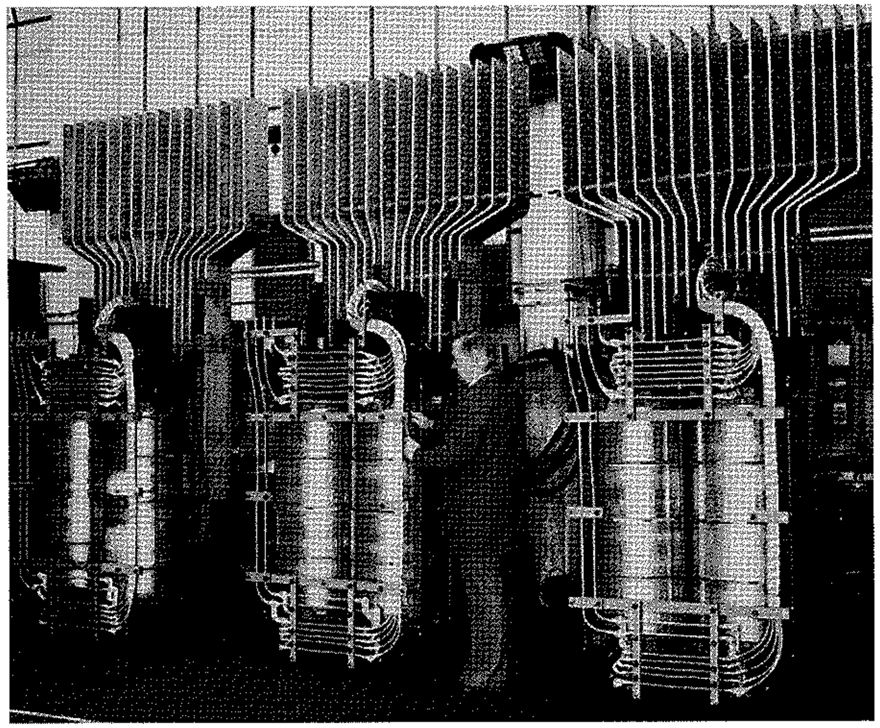


Figure 4.121 Core windings of three single-phase units each rated at 10 000 A and desiged for rectifier testing duty (Allenwest Brentford Ltd)

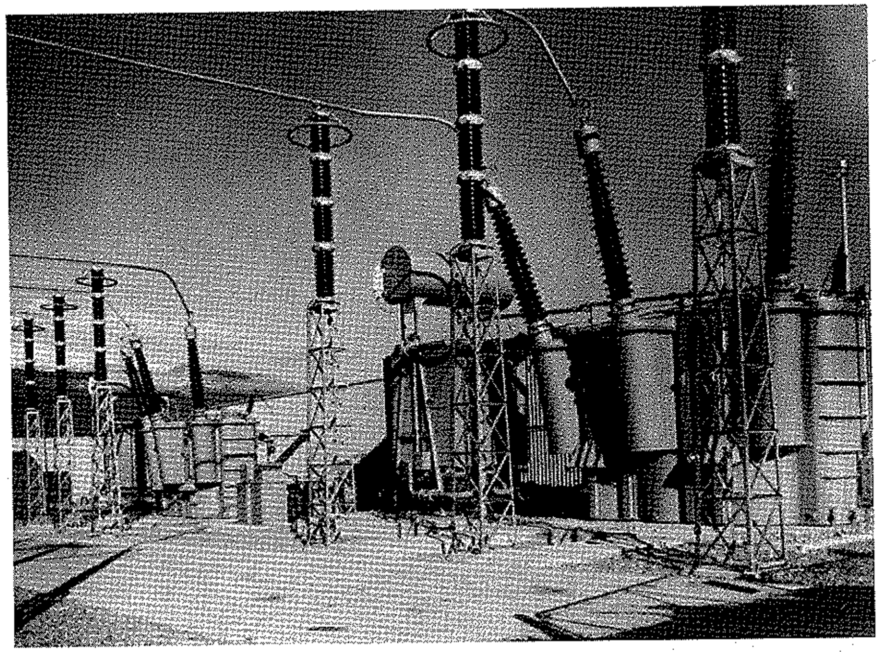


Figure 4.122 Two 90 MVA, 385/18.7 kV units in service at CERN (The European Organisation for Nuclear Research). The units provide power for what is claimed to be the world's largest nuclear particle accelerator; a 400 GeV proton synchrotron. The units have to withstand three million pulses per year at a peak load of 148 MW, 50% above their nominal rating (Hawker Siddeley Power Transformers Ltd)

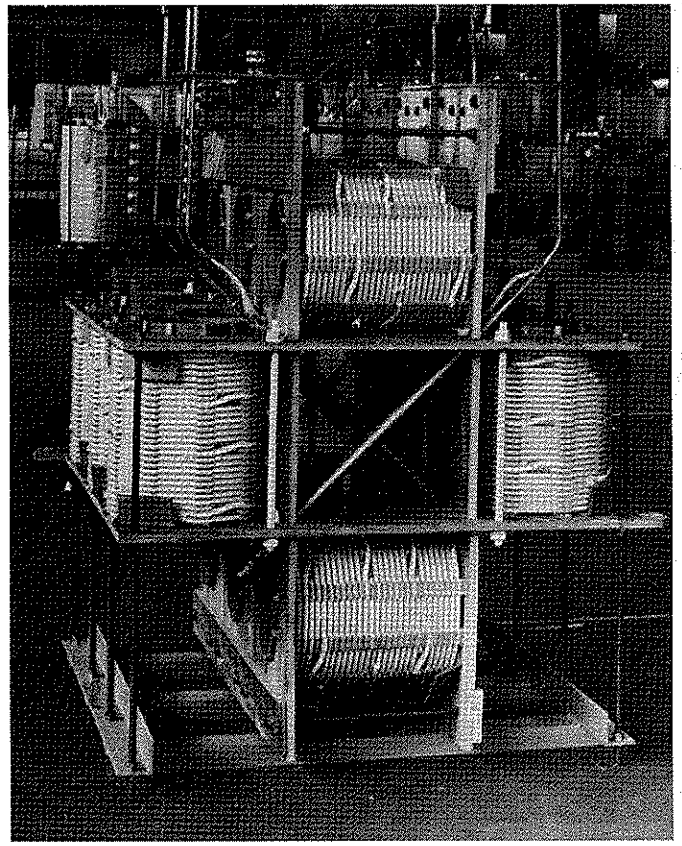


Figure 4.123 Frame and windings of a three-phase air-cored reactor, 20 MVA, 11/6.6 kV, 4% × 50 Hz, shown out of its tank (ABB Power T&D Ltd)

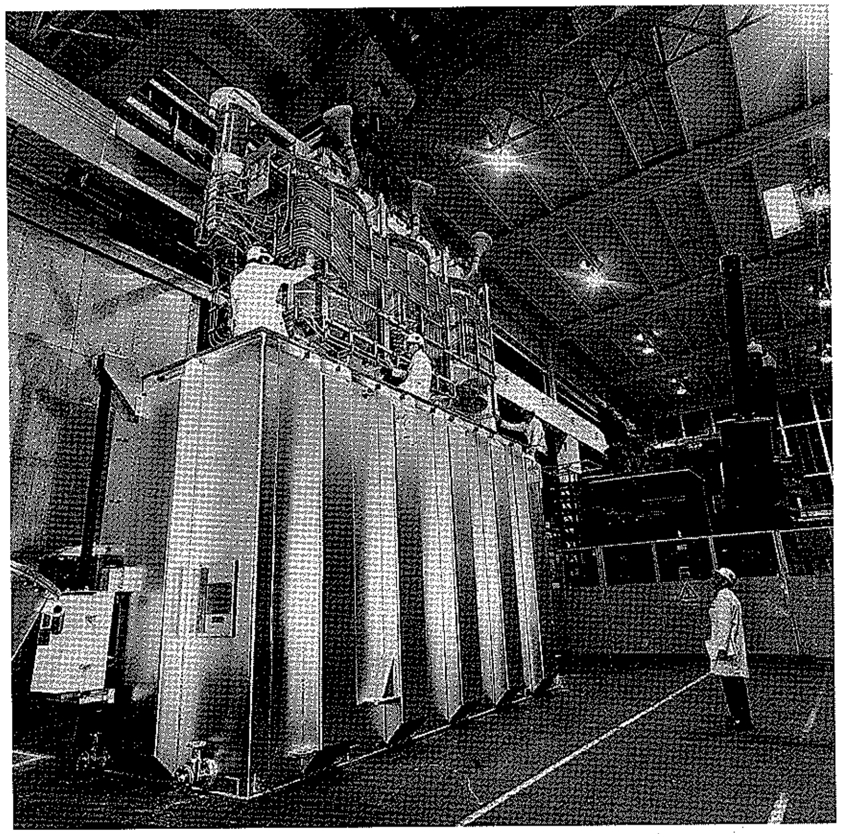


Figure 4.124 Lowering the core and windings of a 148 MVA 275 kV 50 Hz, three-phase generator transformer into its tank (ABB Power T&D Ltd)

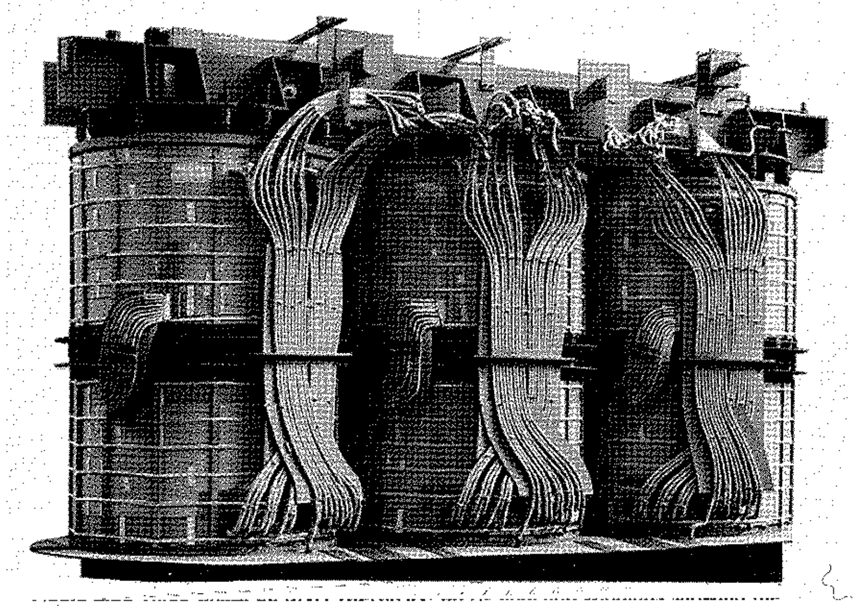


Figure 4.125 Three-phase 60 MVA, 132/33 kV, 50 Hz core and windings showing the outer tapping winding and the tapping leads assembly (ABB Power T&D Ltd)

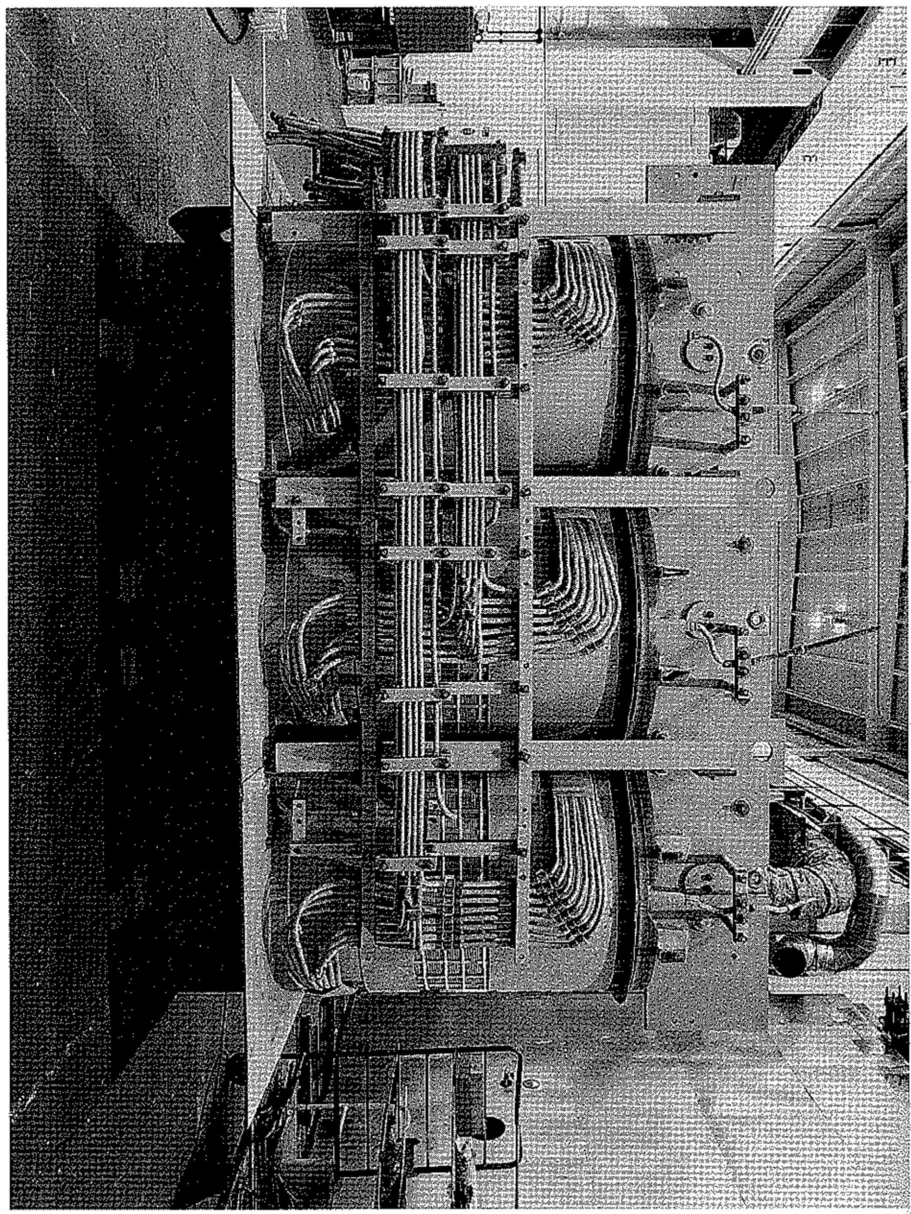
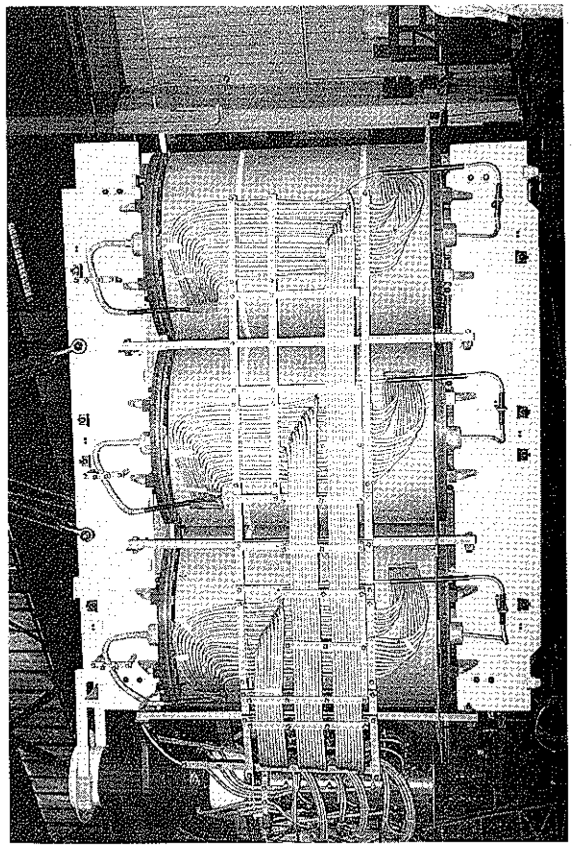
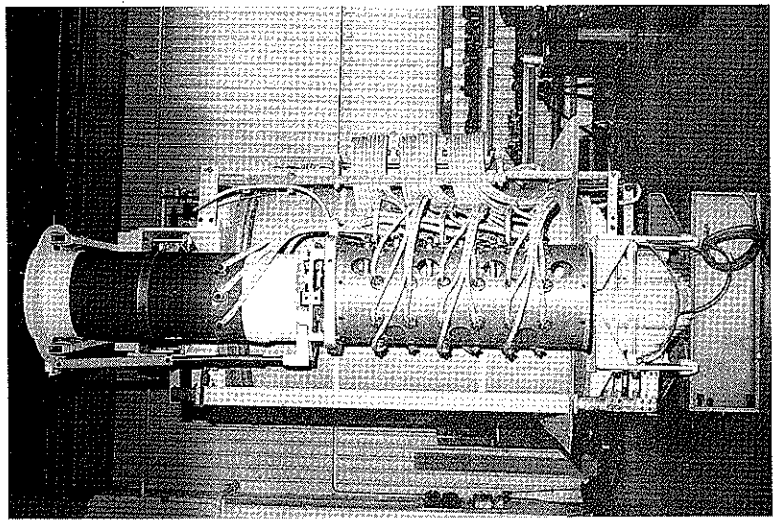


Figure 4.126 Core and windings of 46 MVA, 72.8/11.5 kV, 50 Hz, three-phase transformer with tappings brought out for connection to on-load tapchanger (ABB Power T&D Ltd)



(a)



(b)

Figure 4.127 Two views of the core & windings of a three-phase 90 MVA, 132/33 kV, 50 Hz transformer connected star-delta and fitted with a +10% to −20% tappings on 18 steps of 1.67% at the neutral end of the HV winding. (ABB Power T&D Ltd)

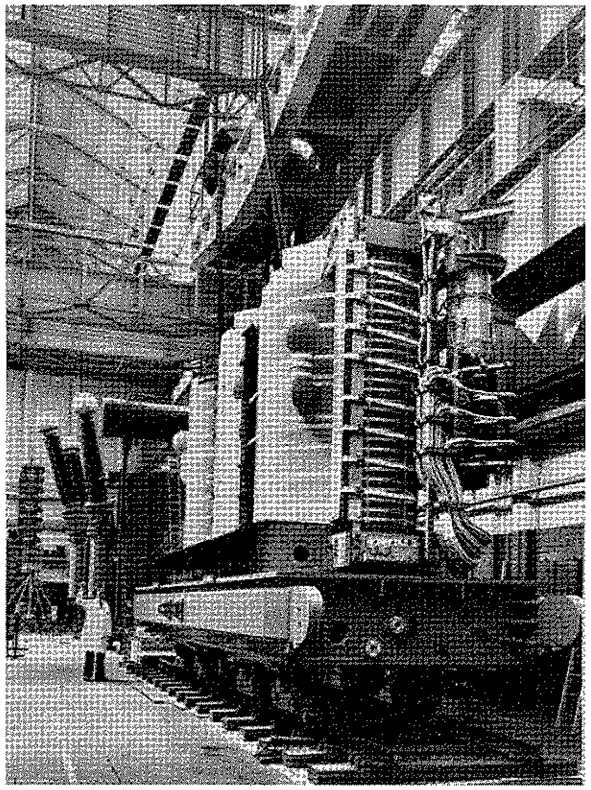


Figure 4.128 Core and windings of a 250 MVA, 400/121 kV power transformer manufactured for the Czechoslovakian Supply Authorities being fitted to its special Schnabel tank base (Hawker Siddeley Power Transformers Ltd)

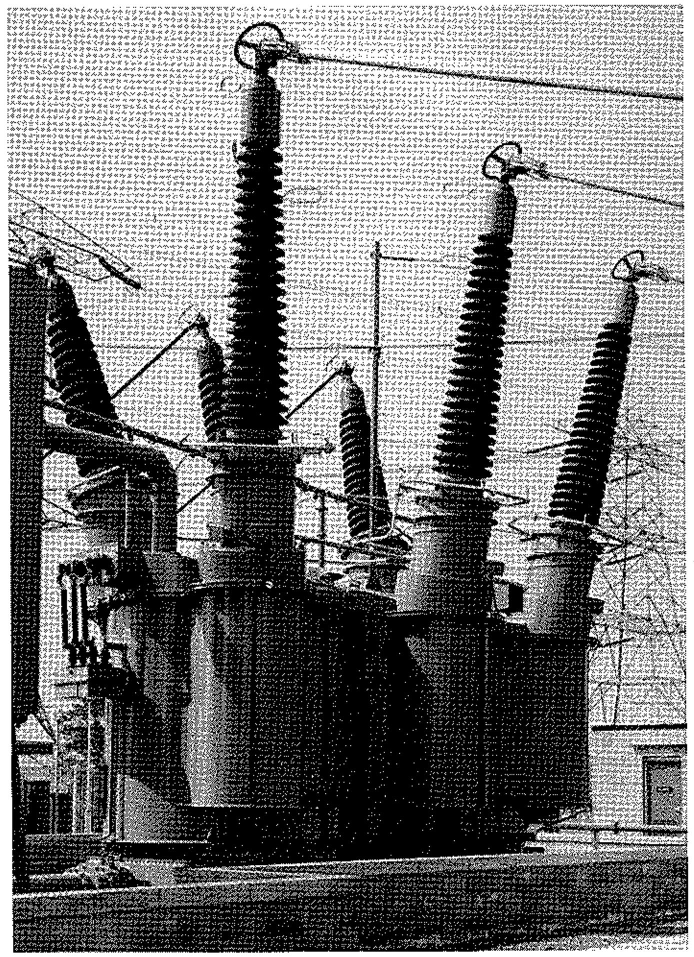


Figure 4.129 A 500 MVA transformer linking the National Grid Companies' 400 kV and 275 kV Supergrid Systems (Hawker Siddeley Power Transformers Ltd)

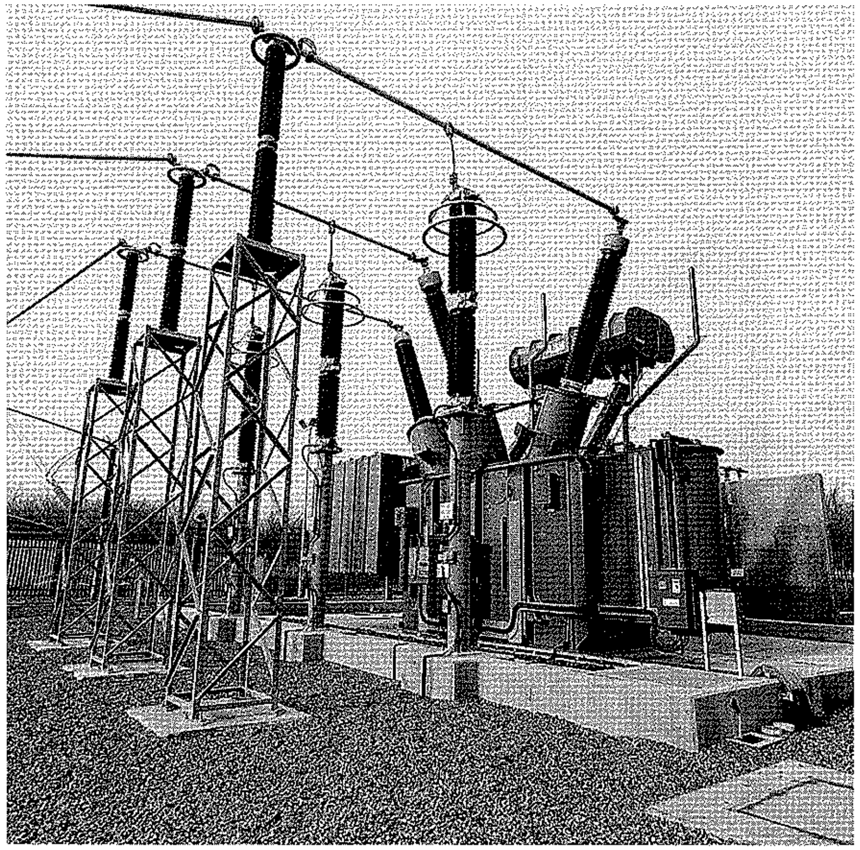


Figure 4.130 Site installation of a 40 MVA, 275 kV, 50 Hz, three-phase, step-down transformer on the UK grid system (ABB Power T&D Ltd)

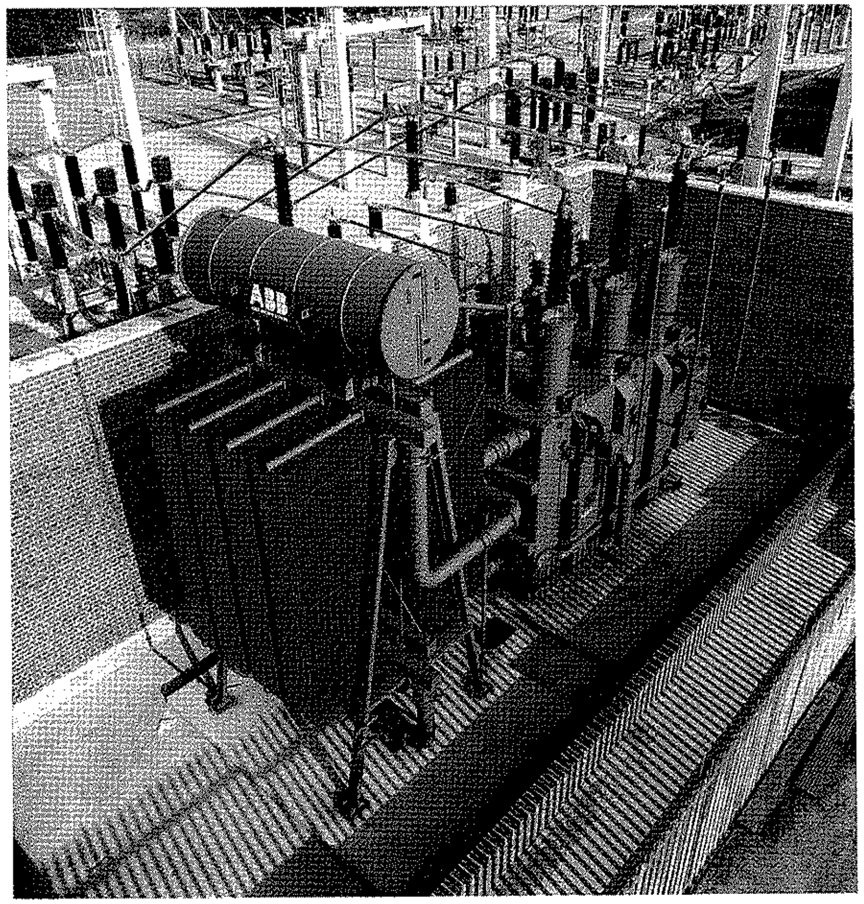


Figure 4.131 Site installation of a 90 MVA, 132/33 kV, 50 Hz, three-phase transformer showing separate cooler bank (ABB Power T&D Ltd)

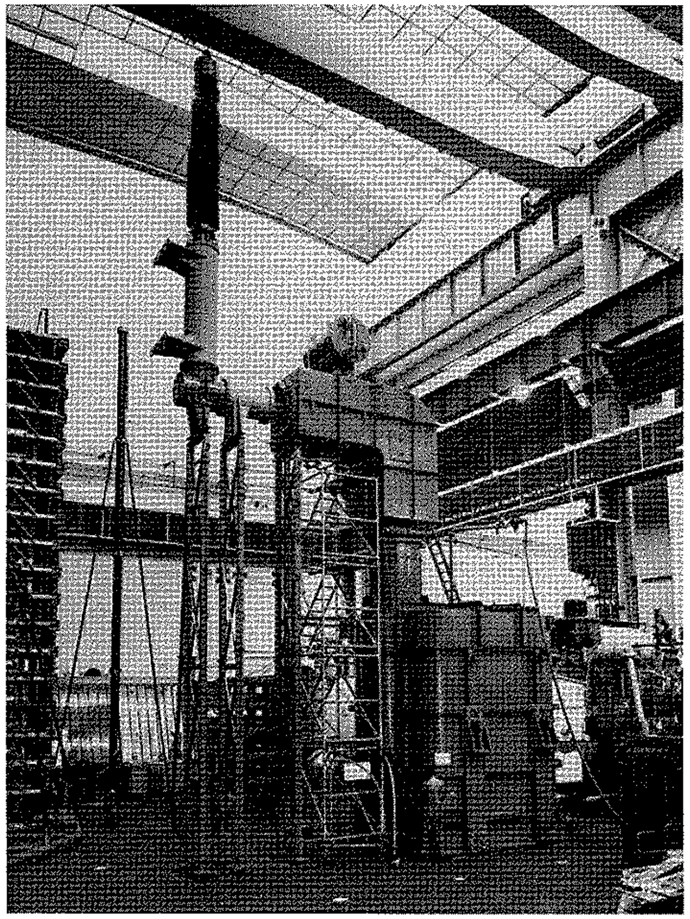


Figure 4.132 Single-phase 267 MVA, 23.5/249 kV, 50 Hz generator transformer type ODAF. Three such units form an 800 MVA, 23.5/432 kV bank. The interposing SF_6 chamber, fitted for test purposes, can be seen on the HV side (Peebles Transformers)

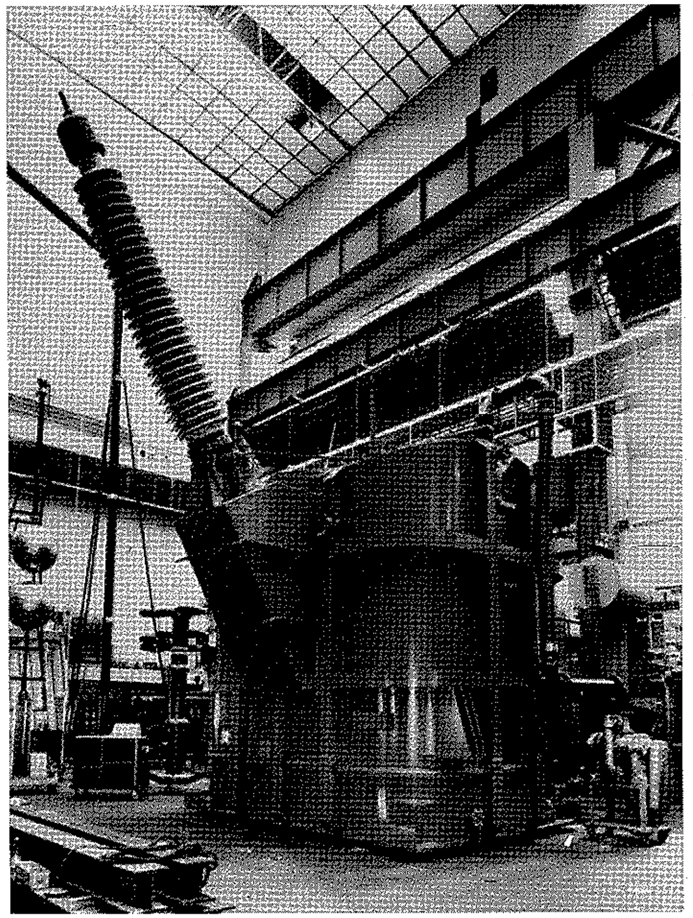


Figure 4.133 Single-phase 239 MVA, 21.5/231 kV, 50 Hz generator transformer. Three such units form a 717 MVA, 21.5/400 kV three-phase bank (Peebles Transformers)

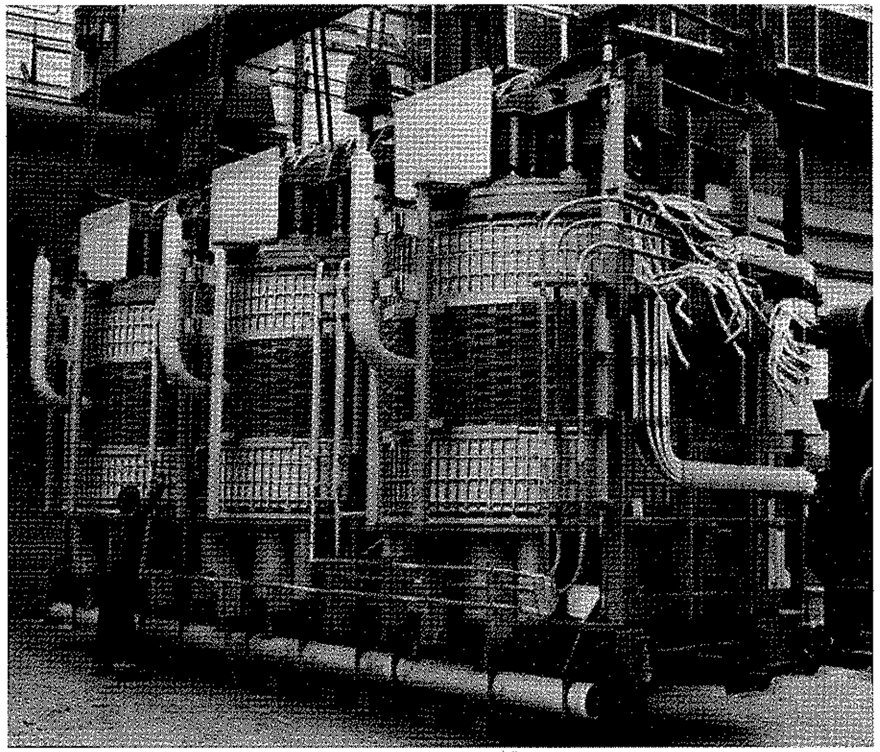


Figure 4.134 Core and windings of a 340 MVA, 18/420 kV, 50 Hz three-phase transformer, type ODWF (Peebles Transformers)

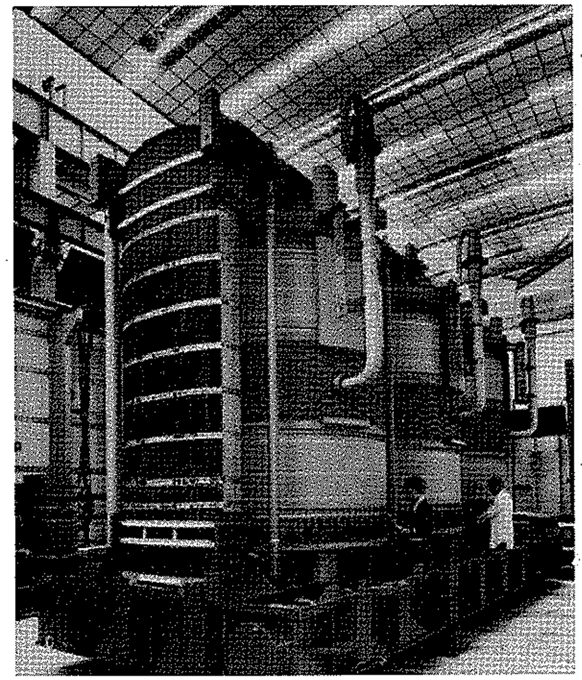


Figure 4.135 Core and windings of a 776 MVA, 23.5/285 kV three-phase generator transformer, type ODWF (Peebles Transformers)