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Evaluation of Advanced Technologies for Power Transformers

Final Report — Part I November 1976
Through March 1979

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Published June 1980

Prepared for:

U.S. Department of Energy
Assistant Secretary for Resource Applications
Office of Electric Energy Systems
Under Contract No. EC-77-C-01-2134

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Prepared by:

General Electric Company
Schenectady, N.Y.

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Prepared for:

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Assistant Secretary for Resource Applications
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Washington, DC 20461

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ABSTRACT

The high insulating strength of certain gases, such as sulfur hexafluoride, when used at high pressure, suggests that there may be advantages to compressed gases as the insulating fluid in power transformers. However, simply exchanging the oil for compressed gas in an otherwise conventional transformer design will not yield a significant overall advantage.

Compressed gases present the engineer with properties which are quite different from mineral oil. If gases are to be used as the major insulating fluid in power transformers, then virtually all aspects of the insulation and cooling of the apparatus must be reconsidered, affording an opportunity to introduce new design concepts, new materials, and new construction techniques. In this program, the feasibility of using the following principal design concepts has been explored:

- Sheet conductors for the windings
- A system of sealed, self-contained, annular cooling ducts containing circulating cooling fluid to cool the windings
- Polymer film for turn-to-turn insulation
- Compressed gas insulation

Experimental and analytical studies, described in this report, indicate that the sheet-wound, compressed-gas-insulated design should result in power transformers of significantly smaller size and weight when compared with oil-filled units of equivalent rating. These advanced technologies offer the opportunity for the design of more efficient power transformers.

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FOREWORD

This Final Report - Part 1 describes the work performed between November 17, 1976 and March 15, 1979, for the U.S. Department of Energy, Washington, DC, and the New York State Energy Research and Development Authority under Department of Energy Contract No. EC-77-C-01-2134. Work to be performed under a fifteen month extension of this contract will be reported as a separate report: Final Report - Part 2. In addition, some of the relevant research performed as an internally funded program by the General Electric Company prior to this contract is included. The work was performed and the report was prepared by the Electrical Systems and Technology Laboratory at the Research and Development Center of the General Electric Company, Schenectady, New York; during the present contract, part of the experimental program was carried out at the General Electric High Voltage Laboratory, Large Transformer Technical Resources Operation, Pittsfield, Massachusetts. The program is under the technical cognizance of Mr. J.P. Vora, Project Manager -- Division of Electric Energy Systems, Department of Energy. Useful discussions with Mr. Vora are gratefully acknowledged.

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Throughout this work, J.J. Cammarere at the General Electric Research and Development Center was in charge of the shop work for, and played an active role in, the construction of model coils for dielectric, thermal, mechanical, and transient analysis tests. The new coil winding apparatus used in this work was designed, constructed and made operative by members of the Laboratory Equipment Development Operation at the General Electric Research and Development Center; the principal contributors were J.O. Fielding, J.J. Cammarere, K.M. Douglas, and A.J. Simone. G. Pagnotta at the Research and Development Center was a leading participant in the fabrication of the cooling ducts used in the thermal test coils.

The contributors acknowledge many helpful discussions with their colleagues in Schenectady and Pittsfield.

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Section 1
EXECUTIVE SUMMARY

Section 1

EXECUTIVE SUMMARY

1.1 Introduction

Present and future designs for power transmission lines and substations are evolving in order to meet new requirements, which are

- Higher power transfer capabilities per circuit, usually accompanied by the need for higher transmission voltage.
- Much improved compactness in substations and rights-of-way, to meet the limitations of space in urban service and to provide amenable installations both in urban and rural locations. Nonflammable coolant and insulant are required in many cases.

Under these circumstances, it is inevitable that compressed gases should be considered as a replacement for the atmosphere (air) as the insulating medium for power transmission. The use of sulfur hexafluoride (SF₆), for example, at a pressure of five atmospheres (absolute) permits a reduction of insulating distances to as little as one-tenth those required in air.

For 765 kV and below, some of the apparatus necessary for a compressed-gas-insulated transmission substation has recently been developed and is now available in the industrial market. The power transformer, the largest single piece of equipment in the station, is an outstanding exception -- no compressed-gas-insulated power transformer is offered in the commercial market today. For voltages above 765 kV, development work is required for all components of gas-insulated substations. The U.S. Department of Energy is one organization addressing the development needs for gas-insulated equipment above 765 kV, specifically those at the 1200 kV level.

Compressed gas appears to have possible advantages over mineral oil, the insulating fluid used in conventional power transformers. However, simply exchanging the oil for compressed gas in an otherwise conventional transformer design will not yield a significant overall advantage. Compressed gases present the engineer with physical properties which are quite different from mineral oil. If gases are to be used as the major insulating fluid in power transformers, then virtually all aspects of the apparatus insulation and cooling must be reconsidered, affording the opportunity to introduce new design concepts, new materials, and new construction techniques.

In this program, the following principal design concepts for a gas-insulated power transformer have been explored:

- Sheet conductors for the windings
- A system of sealed, self-contained, annular cooling ducts containing circulating cooling fluid to cool the windings
- Polymer film for turn-to-turn insulation
- Compressed gas insulation (which fills the free space in the film-insulated windings and also insulates all the major gaps).

Some additional comments on these concepts are appropriate.

In the area of electrical insulation, the proposed concepts present two novel features: compressed-gas-immersed polymer film as the turn-to-turn and reactance gap insulation for the sheet-wound coil; insulating coolant pipes and liquids for the cooling system.

With reference to the second feature, the insulating pipes carry cooling fluid from the ducts at high potential to a manifold at ground potential. Hence, both the coolant pipes and fluid must be able to sustain the high potential differences along the length of the pipes.

In the area of coil design, both inner and outer windings are constructed of sheet conductor, the sheet width extending over the full axial height of the winding. Built into the windings at appropriate intervals are cooling ducts in the form of thin, cylindrical annuli. The ducts are constructed of metal, and each could comprise one turn of the winding; they are at the potential of the adjacent conductor.

The conceptual transformer design derived from these concepts calls for a major departure from conventional design and manufacturing practice in terms of the insulation, cooling, and mechanical design. Only the magnetic circuit and core design would be similar; however, alternate core designs would be possible.

However, for such major changes to be worthwhile, significant advantages must accompany them. Although much work remains to be done before a large power transformer of this type can be built and the potential advantages verified, it is possible, nevertheless, to identify some of the potential advantages which could be expected from such a design. These include

- Compact power transformers of reduced size and weight -- possibly 1/2 the size and weight of a conventional transformer of similar rating
- Insulating and cooling fluids which are not combustible and are chemically inert
- Noise levels external to the containing vessel which should be inherently lower than with oil-filled transformers because of diminished acoustical coupling from the core to the outside air
- A transformer design which is compatible with compressed-gas-insulated compact substation and transmission cable designs.

Finally, there is a general advantage stemming from reduced size and weight: It should be possible to build large power transformers of significantly lower losses and lower impedance, with a corresponding greater design flexibility.

During the past eight years, the General Electric Company has undertaken a program to investigate the above technologies. The past two years of this program have been funded by the U.S. Department of Energy (DOE) and the New York State Energy and Development Authority (NYSERDA) under DOE Contract No. EC-77-C-01-2134. The results of a portion of the internally-funded program combined with the work conducted under the sponsorship of DOE and NYSERDA on the present DOE contract are described in this report. The objective of the DOE and NYSERDA program and the major accomplishments are summarized below.

1.2 Program Objective

The objective of the Department of Energy program has been to further develop and refine the following technologies for incorporation into the design of power transformers:

- sheet-wound coils
- compressed gas and polymer film insulation
- sealed, forced-liquid cooling.

1.3 Major Accomplishments

Coil Insulation

The coil insulation structure has been investigated using simulated turn-to-turn samples of various polymer film* thicknesses and total areas; the limits of the insulation system were determined by the 60 Hz threshold voltage for partial discharges (corona). Partial discharge inception stresses greater than 58 kV(rms)/mm have been achieved in turn-to-turn samples consisting of 0.05 mm polymer film (area $\sim 16 \text{ m}^2$) impregnated with SF₆ gas at a pressure of 6 atm abs.

Complete model coils have also been tested under ac and impulse voltages at an SF₆ gas pressure of 6 atm abs. For ac, two large windings (conductor area $\sim 3300 \text{ m}^2$) were tested using 60 Hz induced voltage up to 265 kV (peak) with no detectable partial discharges. For impulse voltages, a 164-turn single winding (conductor area $\sim 168 \text{ m}^2$) was tested up to 200 kV, at which point creepage flashover over the exterior of the coil occurred at a stress of $\sim 40 \text{ kV/cm}$. An additional test coil has been designed to test the full impulse capability of the turn-to-turn insulation; the results will be reported in Part 2 of the Final Report.

The above 60 Hz partial discharge inception stress (= 58 kV(rms)/mm) is to be compared to an assumed 60 Hz operating stress of 13 kV(rms)/mm for a large conceptual EHV power transformer described in this report. The impulse strength capability of a sheet winding will be measured in Part 2.

Cooling System Insulation

In a special test fixture, in compressed SF₆ gas at room temperature, an insulating coolant pipe of 20 cm length and containing quiescent fluorocarbon coolant liquid has withstood impulse voltages of 1200 kV, corresponding to an average gradient of 60 kV/cm along its length; this stress compares favorably with an assumed design value of 20 kV/cm (impulse) for the conceptual large EHV power transformer design evaluated in this report. Measurements at 60 Hz have also been made on shorter (1.5 cm) sections of coolant pipe with fluid in

*The polymer film used throughout these investigations has been the polyethylene terephthalate (polyester) film, Mylar®. (®Registered trademark of E.I. du Pont de Nemours and Co.)

forced circulation up to a voltage of 177 kV(rms) corresponding to a stress along the pipe of 118 kV(rms)/cm; the maximum ac test stress for the coolant pipe is 12.6 peak kV/cm.

Electromagnetic Analysis

A study of the magnetic aspects of the design has included the important question of current distribution in sheet windings. Analytical techniques of general applicability have been developed in this work. A verification of the current distribution, as predicted by the analysis, has been obtained in temperature measurements on a model force-cooled transformer coil.

Consideration has been given for the optimum configuration of a core to utilize the very compact coil construction offered by these new technologies: Such a configuration may be a five-legged or "cruciform" core construction; during the present program such a core has been designed and constructed.

Thermal Analysis

Study of the thermal aspects of the design has been confined to the windings and the coil structure. The temperature distribution in sheet-wound coils was studied analytically and experimentally. The temperatures measured in an experimental transformer are in good agreement with those calculated by our analyses.

Design of the cooling system as a whole has been pursued to the point of yielding some preliminary design quantities for full-sized power transformers. A simple, forced-cooled system for a model transformer has been built and used for this work.

Mechanical Analysis

Two different concepts for mechanical support of the entire transformer coil, including cooling ducts, have been employed in this work:

- End-support by means of annular insulating plates, which were supported from an inner structural cylinder.
- Self-supporting coils, achieved through the use of tension applied during winding. Inner and outer structural cylinders (glass-reinforced epoxy was used in the present study) hold the winding in compression.

Except for the first three coils, model coils for this work were prestressed and self-supporting.

Preliminary calculations have been made of the axial and radial stresses which would be experienced by full-sized, sheet-wound power transformer windings under short-circuit conditions. A detailed mechanical analysis has not yet been performed. The concept of the self-supporting coil requires additional engineering development before a thorough mechanical analysis will be possible.

Axial stability of a sheet winding has received preliminary assessment. It is clear that the axial stability (i.e., the relative displacement of the inner and outer windings) of a sheet winding qualitatively and quantitatively differs from that of a wire winding. In sheet windings, stability depends on the detailed geometry of the displacement.

A successful preliminary experimental study of the performance of a sheet wound coil subjected to short circuit forces has been made on a coil constructed with a 25 turn inner winding and 1670 turn outer winding. Currents of 20,000 A (rms) (43,000 A on the first offset peak) were attained with no perceptible mechanical damage or distortion to the coil.

Manufacturing Technology

The preliminary development of some of the basic manufacturing techniques for sheet-wound coils has been one of the major achievements of this work. Based on the experience gained in building the first six model coils, a new winding machine was designed and built. This machine incorporates a number of innovations to meet fabrication difficulties specific to sheet windings.

The excellent dielectric performance of some of the coils constructed, particularly those composed of a dozen dielectric test samples, leads to the conclusion that the proposed techniques for eliminating particles during winding can be applied to larger coils.

Construction of sealed, metallic cooling ducts, suitable for application to these coils proved to be a challenging task. Techniques have been developed which make it possible to build model forced-cooled coils.

1.4 Conclusions

The results of the work reported lead to the conclusion that it is technically feasible to build sheet-wound, compressed-gas-insulated, forced-liquid-cooled power transformers. Conceptual designs have been developed which offer the following significant advantages over conventional oil-filled designs of similar rating:

- lower losses
- lower weight
- smaller size
- nonflammability
- compatibility with compressed-gas-insulated substations and transmission lines.

Further advantages not studied in this program may be inherently lower noise transmitted outside the containing vessel, coil designs which are adaptable to automatic manufacturing processes, and opportunity to introduce standard design.

Results obtained to date, coupled with the above potential technical advantages, lead to the recommendation that further development of the sheet-wound compressed-gas-insulated power transformer should be vigorously pursued to a full-scale demonstration model.

1.5 Final Report - Part 2

A fifteen-month extension of this program will be reported in Part 2 of the Final Report. Work will be undertaken in the following areas:

- 60 Hz and impulse breakdown measurements on small (six-inch size) sheet-wound coils built on a new coil winding apparatus to be constructed by the General Electric Company. A comparison will be made of the breakdown performance at elevated temperatures of coils wound with polyester film against coils wound with polyamide film.
- Analysis of the forces associated with short circuit currents in sheet windings. Design, construction and testing of sheet-wound model coils.
- Application of axisymmetric finite element magnetic field analysis to sheet-wound transformer designs; analysis of losses, forces, and thermal performance.
- Analytical studies of transformer designs based on the proposed concepts.
- Preliminary study of the possible manufacturing technology requirements associated with sheet-wound gas-insulated transformers.

Section 2
INTRODUCTION

Section 2

INTRODUCTION

For application at EHV (765 kV) levels and below, some of the apparatus necessary for a compressed-gas-insulated transmission substation has now been developed to the state of being available in the industrial market. Specifically, the buswork, circuit breakers, disconnect switches, surge diverters, and current and voltage measuring equipment are available for use in compressed-gas-insulated substations. The power transformer, the largest single piece of equipment in the station, is an outstanding exception: no compressed-gas insulated power transformer is offered in the commercial market today.

The several pieces of compact substation apparatus named above can be made much smaller by enclosing them, individually or collectively, and filling the enclosure with compressed gas. Such enclosed and pressurized apparatus is usually more expensive than the open-air apparatus which it replaces, but, in a growing number of instances, the added cost is found to be justified by the saving in space which results. Similar considerations arise in examining whether power transformers can advantageously use compressed gas insulation.

Gases (air in particular) are electrical insulators, a fact which has been appreciated from the earliest days of electrical science. Likewise, it has long been known that the insulating strength of gases was a function of pressure, and that different gases had different intrinsic insulating strengths.

Sulfur hexafluoride (SF_6), so much in favor as an insulating gas today, has an insulating strength which can be characterized as 2.5 times that of air. Gases are known with higher strength than this, but SF_6 has the additional attractive properties of being

- Usable as a gas at high pressures (5 to 10 atm)
- Nontoxic
- Chemically inert
- Noncombustible.

Compressed gas appears to have definite advantages as a potential replacement for mineral oil, the insulating fluid used in conventional power transformers. SF_6 gas, at pressures above 2 to 2.5 atm abs, is an insulant superior to transformer oil. For example, at 7 atm in a uniform field at ac voltages, the insulating strength of SF_6 gas is four times that of transformer oil.

In this program, the following principal design concepts have been explored:

- Sheet conductors for the windings
- A system of sealed, self-contained, annular cooling ducts containing circulating cooling fluid to cool the windings

- Polymer film* for turn-to-turn insulation[†]
- Compressed gas insulation (which fills the free space in the film-insulated windings and also insulates all the major gaps).

It should be noted that these four concepts are neither arbitrary nor independent, but form a consistent whole. Thus, for example,

- The use of compressed gas -- a poor fluid for transferring heat -- suggests the use of a different fluid for cooling. This, in turn, leads to the possibility of completely separating the insulating and cooling fluids.
- Depending on the design and specific application, sheet windings generally provide a higher winding space factor than that attainable with wire windings when winding currents exceed certain levels. Furthermore, the simple, cylindrical geometry of sheet windings makes possible a comparatively simple cooling duct design.
- The use of sheet windings makes possible the use of polymer film as turn-to-turn insulation.
- Compressed gas insulation is compatible with a wide variety of polymers which would not be suitable for use with oil.

A schematic representation showing how these elements might fit together to provide a power transformer concept is presented in Figure 2-1. Both the inner and outer windings are constructed of sheet conductor, the sheet width extending over the full axial height of the coil. The cooling ducts are thin, cylindrical annuli wound into the coil at intervals, as required. The ducts are constructed of metal, and each could form one turn of the winding. These ducts are at the potential of the adjacent conductor.

Insulating pipes which carry the cooling fluid connect the ducts to a manifold at ground potential. In application to a large power transformer, these insulating pipes have to be capable of supporting high voltages from the end where they connect to the cooling duct at high potential to the end where they connect to the manifold at ground potential. It is clear that not only the pipe, but the cooling fluid itself, must be an insulator.

The resulting transformer concept calls for a major departure from conventional design and manufacturing practice in insulation, cooling, and mechanical aspects. Only the magnet circuit and core design will be similar.

*The polymer film used throughout these investigations has been the polyethylene terephthalate (polyester) film, Mylar. Future studies need to address whether Mylar or some other polymer film will provide optimum performance under the combined electrical, mechanical, and thermal requirements of this application.

[†]Gas-impregnated polymer tapes have been investigated previously for applications in high voltage electrical equipment by Skipper and Gibbons¹ and by Kind and Brand.²

Figure 2-1. Schematic, cross section drawing of a sheet-wound coil, with sealed, forced-liquid cooling

Significant advantages must be forthcoming for such changes to be worthwhile. Much work remains to be done before a large power transformer of this type can be built, and the potential advantages verified and given quantitative expression. Nevertheless, it is possible to identify some of the potential advantages which could reasonably be expected from such a design. These include

- Compact power transformers of reduced size and weight -- possible 1/2 the size and weight of a conventional transformer of similar rating
- Insulating and cooling fluids which are not combustible and are chemically inert
- Noise levels external to the containing vessel which should be inherently lower than with oil-filled transformers because of diminished acoustical coupling to the outside air
- A transformer design which is compatible with new compressed-gas-insulated substation and cable designs.

Finally, there is a general advantage stemming from reduced size and weight. It should be possible, for example, to build and deliver large power transformers of significantly lower losses and lower impedance. All in all, a greater flexibility in design should result.

The general philosophy of development of this sheet-wound, compressed-gas-insulated transformer has been determined by the fact that the four component concepts, viewed individually, are not novel; and are in general industrial use in a variety of equipment. Consequently, there are available considerable engineering data regarding all four component concepts in a variety of applications.

Experience teaches that the novel application of a well-known practice is rarely trivial. There are usually many important details to be worked out and, not infrequently, innovation is necessary to make the application fruitful. Nevertheless, the principal challenge is to accomplish a successful synthesis, as demonstrated in a working model.

The development program should be organized to provide

- Development of the initial concept of the new transformer in sufficient detail to permit application of existing engineering data to estimate a feasible, full-scale design.
- Development of fabrication techniques to a point at which a successful working model can be built
- Comparison of model performance with background engineering data.

After progressing through these steps, one may return to the first step and produce an estimated, full-scale design which should be significantly closer to the final design.

The model in Figure 2-2 has been built and tested during the present program. Tests on experimental coils using this model (described later) have demonstrated the general feasibility of the sheet-wound, compressed-gas-insulated transformer.

Figure 2-2. A laboratory model of a sheet-wound, compressed-gas-insulated transformer

Section 3

ELECTRICAL AND INSULATION DESIGN

Section 3

ELECTRICAL AND INSULATION DESIGN

3.1 Compressed Gas Insulation for a Sheet-Wound Transformer

The insulating strength of compressed gas is most simply and most fundamentally characterized by the value, E_s , of potential gradient at which an electrical instability becomes incipient.⁵ A gas will retain its insulating property under electrical stress so long as the energy of an electron released within the gas, or at the cathode electrode, can be kept below the critical value beyond which cumulative ionization commences (Townsend theory). The mechanism by which the energy of an electron (released in gas) is kept below critical value is the inelastic collision. In addition, some gases, such as SF_6 , will attach an electron to form a stable negative ion.

For any given gas, a theoretical value for E_s can be calculated by using the probabilities of electron-ion collisions.³ In theory, E_s is proportional to pressure, p . For SF_6 , the most important insulating gas presently in use, $E_s/p = 90\text{kV/atm-cm}$.⁴

In practical application, the electric field in the insulating gas depends upon electrode geometry, geometry of intervening dielectric structures (if any), and accumulated space charge (if any). Despite the complexities of an actual application, the maximum insulating gradient in some practical cases is found to be the theoretical value, E_s .

The two fundamental insulation challenges common to all transformers are

- (1) insulation of the voltage between adjacent turns within the winding, either the induced ac voltage or the voltages arising from incoming transients, and
- (2) insulation of the total coil voltage from supporting and surrounding structures. In the proposed design, the task of insulating large total voltages over the end surfaces of the coil (the upper and lower margins of the coil in Figure 2-1), and of insulating large total voltages along the pipes which carry the coolant liquid from a manifold, at ground potential, to ducts within the coil, must be addressed.

3.2 Coil Insulation Structure

3.2.1 General Description

The structure of a sheet-wound coil, having sheet conductor of width equal to the axial height of the coil and having interwound film insulation, is more or less self-evident. Figure 3-1 illustrates this schematically by showing a small upper edge segment of such a coil in cross section. (Figure 2-1 gives a more complete schematic view.)

In principle, there are alternatives to the technique of interwound film insulation: For example, the coil, after being wound with some means provided for separating adjacent turns of the conductor, could be impregnated with a liquid which is then subsequently processed, or "cured", to form a solid insulation.

Figure 3-1. Schematic detail of a sheet winding, showing the situation at the edge of the conductor

The two major problems facing the "potted," or solid impregnated, coil are

- (1) The winding must be free of any voids, since these would not, in general, become filled with gas and therefore would be possible sites for breakdown, even at low potential gradients.
- (2) The impregnated and "cured" coil must have sufficient elasticity, as well as strength, to endure temperature changes and temperature gradients.

These two problems, for which no solutions seem presently available for large conducting sheets, led to the decision to pursue the alternative of dry-wound insulating film.

It should be emphasized that throughout this work the insulating and conducting sheets are wound "dry"; with no adhesives within the winding. Two sheets of polymer film are wound between adjacent sheets of conductor in order to minimize the consequences of imperfections in individual insulating films.

3.2.2 The Theory of Gas/Polymer Film Insulation

From Figure 3-1, it is clear that, in a microscopic sense, the insulation between turns consists of a composite of polymer film and gas. For the discussion which follows, the turn-to-turn gap will be represented by the simplified one-dimensional model of Figure 3-2, in which the polymer film (normally, two

Figure 3-2. Hypothetical model for discussion of gas voids in turn-to-turn insulation

sheets in series) has a thickness, t , and dielectric constant, k , and the gas-filled void is a layer of thickness, δ .

Breakdown is conceived to take place when localized gas breakdowns (so-called partial discharges) initiate in any given gas gap, and with time lead to rupture of the polymer films.

Within the winding, the turn-to-turn gap is in essence a solid dielectric. However, the gap also contains a very thin layer of gas.* The total turn-to-turn gap, g , approximately equals t when $\delta \ll t$.

Near the edge of the conductor, the effective gas rapidly changes from one in which $\delta \ll t$ (the simple one-dimensional case of Figure 3-2) to one in which $\delta \approx t$, and then changes again to become a possible breakdown path in which $\delta > t$. Four such possible breakdown paths are suggested in Figure 3-1.

In addition to the variability in possible breakdown paths, there is also the possibility that gas composition and pressure may be a variable. For example, it might be possible that the composition and pressure of the gas in a void deep within the winding is not the same as those of the gas surrounding the winding. The composition could be different if the air at such locations cannot be removed completely by the application of vacuum. Specifically, the gas pressure applicable to Path #3 (Figure 3-1) may not be the same as that applicable to Paths #1, #2, and #4.

Path #4 is composed of a relatively long distance along the polymer film surface or in the gas adjacent to the surface.

It is possible to describe the threshold voltage for partial discharges between turns in a manner which encompasses the variables discussed above. This description should be generally applicable to the composite gas/solid insulating gap since it will be derived from basic data on the insulating properties of the gas. (The discussion is based on that of Devins and Halleck.)⁵

If the gas void (Figure 3-2) breaks down (e.g., by a burst of corona) when a voltage V_δ is impressed across it, then the corresponding voltage, V_g , across the electrodes will be

$$V_g = V_\delta \left[\frac{\beta}{\delta} + 1 \right] \quad (3-1)$$

*The presence of such a thin gas layer within the winding will be referred to as "impregnation" of the winding by the gas; in this sense, impregnation differs from impregnation of a polymer film by a dielectric liquid in a capacitor, where a swelling of the polymer (film) matrix occurs.

where $\beta \equiv t/k$. V_g is the measured voltage for discharge inception, and k is the dielectric constant of the polymer film.

The gas void is assumed to contain gas pressure p . V_g will be a function of the species of gas used, of p , and of β . V_g can be calculated from the Paschen Curve for SF_6 , which is a plot of the breakdown voltage (metal electrodes, uniform field) as a function of the product of gap and pressure (Figure 3-3).*

Over a useful, wide range of values of product $p\delta$, the Paschen Curve can be approximated by a straight line, and, therefore, one may write

$$V_g = A + Bp\delta, \quad (3-2)$$

where A and B are constants. Combining this with Equation 3-1, one may now write

$$V_g = B_p \delta + A + Bp\beta + \frac{A\beta}{\delta} \quad (3-3)$$

There is a value of gas gap, δ , for which V_g has a minimum value. This is evident from Equation 3-3. Taking the minimum of V_g in Equation 3-3, with respect to δ , one finds the minimum to occur for

$$\delta = \sqrt{\frac{A\beta}{Bp}} \quad (3-4)$$

Consequently, the minimum value of V_g is given by

$$V_g(\min) = B\beta p + 2\sqrt{AB(\beta p)} + A. \quad (3-5)$$

$$A = 500 \text{ V}$$

$$A = 9500 \frac{\text{V}}{\text{mm-atm}}$$

Thus, for example, for a gap .05 mm thick filled with polymer film (dielectric constant = 3.2) and exposed to a pressure of 6 atm abs of SF_6 ,

$$V_g(\min) = 2725 \text{ volts}$$

If one assumes that in the variety of paths available for breakdown (e.g., those shown in Figure 3-1) between turns, there will surely be a critical gas gap δ , (Equation 3-4), then $V_g(\min)$ (Equation 3-5) is the breakdown voltage. What has been shown, therefore, is that the basic data on the insulating properties of a gas, as expressed in the Paschen Curve, lead to the expression (Equation 3-5) for the breakdown voltage in a polymer film/gas insulating gap.

*It is assumed that the breakdown voltage, V_g , for gas between a metal conductor and a polymer surface will be the same as the breakdown voltage, U_d , of gas between two plane metal electrodes.

Figure 3-3. "Paschen's Curve": breakdown voltage as a function of the product of gas pressure and gap length for breakdown in sulfur hexafluoride between uniform field metal electrodes⁶

3.2.3 Preliminary 60 Hz Partial Discharge Experiments with Compressed-Gas Insulated Sheet Windings

The gas-impregnated, polymer film insulating gap is a key element in the insulating structure of the proposed concept. It must be possible to construct windings which will perform as predicted under electrical stress and, in particular, it must be possible to impregnate such a winding with compressed gas.

Accordingly, the first experiment addressed the question of polymer film gas insulation. A winding was built which incorporated two simulated turn-to-turn gaps, across which the full voltage of the secondary portion of the winding could be applied. Some of the principal dimensions of this coil, shown schematically in Figure 3-4, were the following:

Conductor dimensions:	0.035 cm x 56 cm
Turn insulation:	two layers of polymer film 0.0254 cm x 61 cm
Number of turns in primary:	12
Number of turns in secondary:	160

The sample turn-to-turn gaps consisted of four layers and eight layers, respectively, of polymer film 0.0254 mm thick; providing a total thickness of 0.102 and 0.203 mm, respectively. The mean diameter of the sample turns wound over the outer circumference of the coil was 68.6 cm. Therefore, the surface area of one electrode of the test sample was 1.2m^2 .

The pressure vessel was closed, then evacuated to a pressure of 0.1 Torr, or less, for at least ten hours before the introduction of the gas insulation. Figure 3-5 shows the experimental arrangement for measuring the onset of partial discharges in the two test samples.

Figure 3-4. Schematic drawing of experimental coil for testing 0.1 mm and 0.2 mm gaps

Figure 3-5. Circuit-schematic drawing of experimental setup for test of the coil in Figure 3-4

Typical results obtained in this series of experiments are shown in Figure 3-6. The partial discharge inception stress, in peak kV/mm, is plotted as a function of pressure. In different experiments, two different gases, nitrogen (N_2) and SF_6 were used. The partial discharges were detected by using an Electric Research Association corona detector.

Figure 3-6. Experimental results: threshold gradient for partial discharges in polymer film gaps, impregnated with SF_6 gas or N_2 gas

Sensitivity depends on sample capacitance and size of the "blocking" capacitor (Figure 3-5). In these studies, sensitivity was on the order of 10 pC.

Some observations from Figure 3-6 are

- There is a relatively linear increase of the partial discharge inception stress, E_T , over a wide pressure range
- E_T in SF_6 gas is approximately 2.5 times the value of E_t in nitrogen
- Higher gradients can be supported across a thinner sample

The data presented cannot be taken as proof that the winding is completely impregnated. It can be said, however, that impregnation appears to be complete in the region where partial discharges were first observed. Whether this occurs within the winding or at the edges of the conductor cannot be determined from these measurements.

Some comment should be made on the difficulties of computing the "impregnation time," that is, the time required for gas pressure in interior parts of the winding to achieve equilibrium with the gas pressure surrounding the winding.

There appear to be two mechanisms by which the impregnation can occur: the diffusion of SF_6 molecules through the bulk of the polymer film, and the flow of SF_6 gas into the interstitial space between layers of the tightly wound polymer film and aluminum sheet. The first mechanism can be immediately discarded as having no practical effect, since the diffusion coefficient for SF_6 in the present polymer film is less than 10^{-8} cm²/s, at room temperature.

The second mechanism is, no doubt, the operative one; but a quantitative determination depends critically on the dimensions of the interstitial gap. It has not proven practical to determine this in the obvious way, viz., by carefully measuring the diameter of the completed coil to obtain a measurement of the "overbuild." In practice, "zero," or even "negative", overbuilds are measured; this is the consequence of the tension used in winding - which leads to some distortion of both the conductor and the polymer film - and the dimensional tolerances of the materials involved.

Overbuild could, perhaps, be measured by carefully weighing the completed coil and comparing that weight with the coil dimensions and the precise quantity of materials used.

Figure 3-7 plots the results from Figure 3-6 together with theoretical curves. Also shown are data from work by Skipper and Gibbons¹ on partial discharge inception in SF_6 -impregnated polyethylene films of 0.07 mm thickness.

At 6 atm of SF_6 gas, the experimental and theoretical values of partial discharge inception stresses compare as follows:

Gap (mm)	Experimental (KV/mm)	Theoretical (kV/mm)
0.1	62	41.7
0.2	45	33.6

Figure 3-7. Threshold gradient for partial discharges in polymer film gaps impregnated with SF_6 gas compared with theoretical behavior

Some light on the reason for this difference can be obtained by computing the thickness δ of the void size corresponding to the theoretical breakdown voltage. Using Equation 3-4, at 6 atm pressure, the following values of δ are obtained:

Gap Length, t (mm)	Void Thickness, δ (mm)
0.1	.0165
0.2	.0234

If we assume that voids of this size are unlikely to exist within the winding (i.e., that breakdown Path #3 in Figure 3-1 is unlikely), the data suggest that breakdown occurs at the edges of the aluminum foil, along paths such as 1, 2 and 4 in Figure 3-1. If this occurs, the effective insulating gap would be larger than the assumed thickness, t . At the same time, the breakdown will be occurring at a location where the field in the gas is nonuniform, as depicted in Figure 3-8, which shows electrostatic equipotential plots along the edge of a coil. The electric field is enhanced at the corner of the conductor edge and diminishes through the mid-gap portion of the discharge path. Hence, the real situation is probably a considerable departure from the simple, one dimensional model of Figure 3-2.

It is conjectured that the difference between theory and experiment shown in Figure 3-7 results because the length of the actual breakdown path is significantly greater than the turn-to-turn spacing between conductors.

Irrespective of this difference between theory and experiment, the attained stress levels at 6 atm pressure at which partial discharge inception occurs (roughly 60 kV/mm using the 0.1 mm thick insulation) provided considerable optimism for the insulating capability of the compressed gas-impregnated polymer

Figure 3-8. Electrostatic equipotential plot along the edge of a coil

film system. As described below, more detailed experiments confirmed this optimism and resulted in the proposed design stress for conceptual designs of large power transformers outlined in Section 7.

Following these preliminary measurements of partial discharge inception behavior on small area single turns of quite thick insulation pads, a detailed investigation was carried out on a series of model windings with larger area and with insulation thicknesses typical of those that might be used in a full-sized transformer based on these concepts: two sheets of 0.0125 mm polymer film and two sheets of 0.0233 mm polymer film. Both partial discharge inception and breakdown strength at 60 Hz applied voltages were measured.

3.2.4 Partial Discharge Inception and Breakdown Measurements on Model Sheet Windings - Multiple Samples Wound as Dielectric Test Coil

The purpose of these tests was to evaluate the dielectric strength of the proposed turn-to-turn insulation (polymer film impregnated with compressed SF_6 gas) using a test coil built on the new winding apparatus (Figure 7-4) with improved winding techniques, including stringent measures to remove particles.

The test coil consisted of two sets of "capacitor type" sample wound concentrically onto a fiberglass-reinforced epoxy cylinder, approximately 50.8 cm O.D. and 1.9 cm wall. Twelve test samples were constructed:

- Six samples (#s 1-6) constructed with 5 turns of
0.076 mm thick, 61 cm wide aluminum sheet
+ 2 layers of 0.0125 mm thick, 62.5 cm wide polymer film.
- Six samples (#s 7-12) constructed with 5 turns of
0.076 mm thick, 61 cm wide aluminum sheet
+ 2 layers of 0.0233 mm thick, 62.5 cm wide polymer film

The leads were 5.1 cm wide, 0.076 mm thick aluminum, attached at the start of each conductor. Additional layers of 0.05 mm polymer film were employed over the leads at the start of the test coil and between conductors at the finish. Protective pads, each comprised of eight layers of 0.127 mm polymer film, were wound between successive capacitor samples.

Figure 3-9. Schematic drawing of dielectric test coil

In both sets of six samples, 5 turns were wound for each sample: the approximate capacitance of samples #1-6 was 12 μF , that of samples #7-12 was 6 μF . A schematic drawing of the coil is shown in Figure 3-9.

Each of the samples was constructed by winding a six layer sandwich: aluminum sheet (the high voltage electrode), two layers of polymer film, aluminum sheet (the ground electrode), then finally two more layers of polymer film. Provisions were made for removing particles from each of the four polymer films being wound into the coil. As each sheet of film was unwound from its supply roll and fed to the fiberglass cylinder, it first passed close to a static elimination source (either a polonium α -source or a corona charge generator) and then close to jets of ionizing air issuing from perforated tubes located parallel to the film. The static elimination source neutralizes the charges on the polymer surface to which particles are adhering, and the jets of ionized air blow the particles off the film. A 12.7-cm-diameter pressure kiss roll exerting several hundred pounds force was employed for the construction of all samples. Other details of the winding techniques used are described in Section 7 (Figures 7-4 through 7-8).

For each of the twelve test samples wound onto this coil, measurements were made of the 60 Hz partial discharge inception voltage and breakdown voltage. A preliminary evaluation of "conditioning" that can occur during partial discharge measurements was made.

Test Procedure

The test coil, which is illustrated in Figure 3-10, was installed in the pressure vessel using the three-legged core for support. The installed coil is shown in Figure 3-11. Specially-shaped connectors, seen in Figure 3-12, were

Figure 3-10. Dielectric test coil following construction

Figure 3-11. Dielectric test coil mounted on core in the pressure vessel

Figure 3-12. Shielded (corona-free) connections to aluminum sheet leads

constructed in order to provide corona-free connections between the sheet aluminum taps from the capacitor samples and the 3.2 mm tubing leads used to make contact with the pressure vessel bushings. Connections to six capacitor samples were made simultaneously through these bushings so that the pressure vessel needed to be opened only three times in testing all twelve samples.

After the test coil was mounted to the test vessel and the six bushing connections were made, the vessel was sealed and evacuated to a pressure of less than 100 μ m for at least 24 hours. The time required to reach this pressure level varied considerably in subsequent test vessel evacuations, and seemed to depend on both the initial state of the test coil and the pumping apparatus and procedure. The 24-hour period under vacuum was necessary to insure that impurities and moisture had been removed from the capacitor samples before back-filling with SF_6 .

After the period of evacuation, the vessel was filled with SF_6 to a pressure of 6 atm abs and, in all cases, the samples were allowed to impregnate in this condition for at least 48 hours prior to testing.

The test circuit shown in Figure 3-13 was developed for making partial discharge and breakdown measurements. The elements of the circuit included a large capacity transformer which was required because of the high capacitance of the samples. The tuning reactor was placed on the low voltage side of the transformer to act as a resonant element in order to limit the current-carrying duty of the control circuitry. The test circuit contained two parallel arms, one with the dielectric test sample and the other with a balancing capacitor. A differential measurement of partial discharges was used in order to obtain as high a sensitivity as possible for determining the corona inception level. Additional elements of the circuitry included protective resistors and detecting impedances. The final oscilloscope display sensitivity that was achieved - 50 pC/cm - appears rather low compared to partial discharge measurements made on typical electrical equipment. However, this sensitivity is considered to be quite good, considering the high capacitance of the samples, and was deemed to be adequate for a reasonable interpretation of the discharge phenomena. For example, using this display sensitivity, the minimum detectable discharge was about 30 pC.

Figure 3-13. Test circuit used for partial discharge and breakdown measurements on dielectric test coil

Results

Three test series were performed on the capacitor samples. These were as follows:

Test Series #1. Corona inception measurements were made on the outer six (6 μ F; #s 7-12) capacitor samples up to and slightly beyond inception but not to breakdown.

Test Series #2. Corona inception measurements were made on the inner six (12 μ F; #s 1-6) capacitor samples, followed by breakdown tests on these samples.

Test Series #3. Discharge inception measurements were remade on the outer six samples, followed by breakdown measurements on these samples.

The results for both discharge inception and breakdown measurements are given in Table 3-1. In Table 3-1, only final values for discharge inception are presented, since it was generally found that discharge inception voltages increased when the measurements were repeated. For example, on sample #12, partial discharges were observed at 1650 V during test series #1, but later, in series #3, were not detected until 3000 V. This type of "conditioning" may result from additional SF₆ impregnation or from a "burning out" of discharge sites. These effects require additional study. It should be noted that the voltage was usually increased in 100 V steps, and was held at each level for

Table 3-1
PARTIAL DISCHARGE INCEPTION AND BREAKDOWN MEASUREMENTS*
ON DIELECTRIC TEST COIL

Sample #	Partial Discharge Inception Voltage volts peak (volts rms)	Partial Discharge Inception Stress kV peak/mm (kV rms/mm)	Breakdown Voltage volts peak (volts rms)	Breakdown Stress kV peak/mm (kV rms/mm)
2 x 0.0125 mm thick polymer film				
1	--	--	1410 (1000)	55.7 (39.4)
2	2120 (1500)	83.4 (59.0)	2260 (1600)	89.0 (63.0)
3	--	--	1555 (1100)	61.2 (43.3)
4	--	--	<212 (<150)	<8.3 (<5.9)
5	--	--	566 (400)	22.2 (15.7)
6	2120 (1500)	83.4 (59.0)	2260 (1600)	89.1 (63.0)
2 x 0.0233 mm thick polymer film				
7	3960 (2800)	84.6 (59.8)	5020 (3550)	107.0 (76.0)
8	3820 (2700)	82.0 (57.9)	5510 (3900)	118.0 (83.5)
9	3960 (2800)	84.6 (59.8)	4380 (3100)	93.7 (66.3)
10	4525 (3200)	97.1 (68.5)	2260 (1600)	48.5 (34.3)
11	4240 (3000)	90.8 (64.2)	5660 (4000)	121.0 (85.6)
12	4240 (3000)	90.8 (64.2)	4670 (3300)	100.0 (70.7)

*The partial discharge inception and breakdown stresses are given in peak values since it is the peak voltage that initiates discharges, particularly in a gas dielectric. However, it is normal design practice for the transformer industry to refer to rms values, hence the equivalent rms values are given in brackets.

at least one minute. Discharge extinction levels ranged as low as 80% of the inception levels. A histogram of the breakdown stresses is given in Figure 3-14 where the 60 Hz breakdown stress for all 12 samples is given in rms kV/mm.

Two conclusions can be drawn from Figure 3-14. First, the thin (2 x 0.0125 mm) samples show a relatively wide dispersion in breakdown stress and a lower mean stress than the thicker (2 x 0.0233 mm) samples. Second, with the exception of one test, all the thicker samples have a breakdown stress in excess of 93 kV peak/mm. It is interesting that sample #10, which had a low breakdown stress, actually had an initial corona inception level which was far in excess of its breakdown stress, and consequently it appears that the breakdown was influenced by the breakdown of an adjacent sample. This is further evident in Figure 3-15 where the breakdown stresses and discharge inception stresses are plotted on the same histogram. In that figure, the data for all the 2 x 0.0233 mm samples and the data for two of the 2 x 0.0125 mm samples with highest discharge inception and breakdown have been replotted. Note that the sample which had a low breakdown stress had a very high initial inception stress in previous testing.

Figure 3-14. Histogram of 60 Hz breakdown stresses measured for the twelve test samples of dielectric test coil

Figure 3-15. Histogram of the 60 Hz partial discharge inception and breakdown stresses for selected samples from dielectric test coil. Discharge inception and breakdown values on given samples are connected.

The results shown in Figure 3-14 are considered to be very encouraging for the proposed application of this (SF₆-impregnated polymer film) insulation system to a sheet-wound transformer. Two reasons for this optimism follow. First, the 60 Hz breakdown stress is at least 5 times higher than the assumed operating stress of 18.4 kV/mm, used as the basis of a preliminary design of a 500 kV, 500 MVA generator step-up transformer (see Section 8). Second, even if the impulse ratio is as low as 1.0, the results in Figure 3-14 indicate that the minimum impulse breakdown stress of the 2 x 0.0233 mm insulation system would be on the order of 93 peak kV/mm. This is to be compared with a BIL of approximately 67 kV/mm which has been assumed in the preliminary design estimates for a conceptual 500 MVA, 500 kV generator step-up transformer (Section 8).

Although the number of samples tested above is insufficient to suggest a statistically acceptable design stress, the data lead to considerable optimism about the maximum stresses which can be achieved in the proposed turn-to-turn insulation system. However, it is acknowledged that other factors, such as the area under test and the conductor edge length, could reduce the attainable stress levels in SF₆-impregnated polymer film insulation significantly from those reported in Figures 3-14 and 3-15. The influence of such factors requires considerable future study in order to obtain appropriate design information.

After the coil was removed from the pressure vessel, post mortem analyses were performed on the individual test samples. These revealed that failure sites were removed from the conductor edge and were randomly distributed. This suggests that the breakdowns were generally caused by particles randomly introduced during winding. The breakdown site for sample #10 was located directly opposite that of the adjacent sample, #9. This suggests that the breakdown value of sample #10, which was lower than the discharge inception voltage, was probably reduced by breakdown in the adjacent sample.

When the discharge inception data obtained with this coil are plotted with those from Figure 3-7, the data are shown in Figure 3-16. Again, it is found that the experimental data lie above the theoretical data. To account for this, it is again conjectured that the discharges are occurring in the SF₆ gas at the aluminum foil edges. As noted above, however, postmortems revealed that in all instances breakdown occurred away from the edges. Two possibilities then present themselves: 1) discharges first occur at the foil edges but do not in themselves cause breakdown (presumably they would do so if given time) which occurs at particles; 2) the discharges occur at particles within the winding and, even though adjacent gas gaps may be very small, these discharges are triggered by the locally very high electric field. Additional studies to examine and identify the origin of the discharge sites in gas-impregnated, polymer-film-insulated sheet windings are recommended. Such studies should examine other factors such as:

- the effect of foil edges (rolled vs machined)
- the effect of deliberately introduced particles of known size and shape (both within the winding and at the edges of the foil)
- the important question of whether the center of a winding becomes "impregnated" with the insulating gas.

Conclusions from Discharge Inception and Breakdown Measurements on Model Sheet Windings

- At 6 atm SF₆ gas pressure, the 60 Hz discharge inception and breakdown stresses achieved in sheet-wound model windings are well in excess of proposed design levels
- In future work, extensive data will be needed on the effect upon discharge inception and breakdown stresses of parameters such as the area under test, conductor edge length, conductor edge finish, and chance contaminant particles.

Figure 3-16. Revised Figure 3-7 showing data for 0.046 mm turn-to-turn insulation

3.3 Electrical Insulation for the Cooling System

This transformer concept employs sealed cooling ducts, in the form of thin, cylindrical annuli, wound into the coil at intervals (Figure 2-1). These ducts are made of metal (both copper and stainless steel have been used in model coils) and are connected electrically to an adjacent turn of the winding. Within the duct itself, the coolant liquid moves in an essentially field-free space. Insulation of the duct within the winding is accomplished exactly as if the duct were a turn of the winding conductor.

With a sealed, self-contained cooling system employing ducts within the coil, it is inevitable that a pipe carrying the coolant must pass from a grounded portion of the cooling system to the high voltage coil. The manner in which this could be accomplished is shown schematically in Figure 2-1, and as actually accomplished in model coils in Figures 3-17 and 3-18.

The pipes, as well as the fluid moving within them, must be high-voltage insulators. Three insulating media - solid, liquid, and gas - are present in the high voltage gap; the surfaces at which liquid insulant meets solid, and where solid insulant meets compressed gas, are particularly critical in insulation design. In the proposed design, the situation requires particular care because the potential gradient is more or less parallel to these critical boundary surfaces. A further difficulty arises as a consequence of the dynamic state of the insulating liquid: It is being circulated at velocities which are typically 30-50 cm/s, and it is at elevated temperature. Therefore, formation of gas voids, or vapor pockets, within the liquid in the insulating pipes must be counted as a likely possibility.

Figure 3-17. Insulating coolant pipes for a 100 kV model transformer

Figure 3-18. Top end of a force-cooled coil, showing insulating coolant pipes

In addressing this problem, use is made of the fact that the insulating pipes need not be located within the core window, which might have been suggested by the schematic drawing of Figure 2-1, but may pass upward (or downward) from the coil outside the core yokes. Relatively large total insulating distances, with long lengths of insulating pipe, are therefore possible, as evident in Figures 3-17 and 3-18. The key to the successful design of these pipes is to make the insulating segments in such a way that gradient intensification at the ends is as small as possible. Thus, the greatest possible advantage is derived from the available insulating length. One means toward this end is the use of shield structures (Figures 3-17 and 3-18) which fit over the plumbing hardware at the ends of the pipes.

A thorough, experimental study of the performance of insulating coolant pipes suitable for this application is yet to be done. However, two preliminary experimental demonstrations of feasibility were successfully accomplished.

The choice of pipe material was governed by the ready availability of a commercial Teflon[®] piping, complete with standard, high-pressure fittings in a variety of bores, lengths, and wall thicknesses. The first tests were made at voltages up to 250 kV (ac peak) in the experimental transformer. Two lengths of insulating pipe ran from ground to a cylindrical structure at the high voltage coil (Figures 3-19 and 3-20). The upper and lower pipe segments each emerged from a circular shield at ground potential and then passed to the cylindrical shield at high voltage. Within the high voltage shield the pipes were connected so that a closed path was provided, through which the coolant liquid, R-113,* could be pumped during the high voltage test. The coolant was at room temperature (~25°C) during the course of these tests.

The upper pipe segment was the "test sample;" and the uppermost grounded shield was mounted to a moveable structure, which had a worm gear drive from an electric motor (Figure 3-21). By means of this device, the insulating gap from the upper grounded shield to the cylindrical high voltage shield could be varied at will during high voltage testing with the pressure vessel closed and filled with gas.

With this experimental arrangement, and with the voltage available (260 kV), it was not possible to produce a surface breakdown along the insulating pipe. With the pressure of SF₆ gas reduced to 4 atm, breakdown occurred at 250 kV (peak) (177 kV (rms) $\approx$ 118 kV(rms)/cm gradient) across a 1.5 cm gap between the two metallic shields. This, however, was a breakdown in the insulating gas - not along the pipe. (At this close spacing the shield electrodes dominated the situation.)

[®]Registered trademark of E.I. du Pont de Nemours and Co.

*R-113 is the generic designation used for the refrigerant trichlorotrifluoroethane.

Figure 3-19. Testing of insulating coolant pipe in the experimental transformer

Figure 3-20. Cross section of the arrangement for high-voltage testing of coolant pipe

Figure 3-21. Device for changing the insulating gap in the arrangement of Figures 3-19 and 3-20

Apparatus for detecting microdischarges was connected to the grounded end of the test segment. However, no discharges were observed, even at the highest stresses which could be applied to the pipe and the circulating liquid; viz., 118 kV (rms)/cm for a 1.5 cm gap.

The second test sequence aimed at higher total voltages and employed a similar test sample (Figure 3-22). For this purpose, facilities which permitted testing at ac voltages up to 600 kV(rms) and at impulse voltages up to 1200 kV were used. In this work the coolant pipe was filled with R-113, and measurements were made with the coolant at room temperature. The coolant was not circulated.

Three samples of the configuration shown in Figure 3-22 were tested. The results can be summarized as follows:

Gap Between Shields (See Fig. 3-22) (cm)	Pressure of SF ₆ (atm abs)	Breakdown Voltage (kV)	Calculated Average Breakdown Stress (kV/cm)
30	5	777 (peak ac)	28
20	5	>1200 (Impulse)*	>60*
20	4	900 (Impulse)	45

*Breakdown voltage could not be reached with the available equipment.

Figure 3-22. Fixture for the second series of tests of insulating coolant pipe

These results demonstrate the general feasibility of the proposed concept of circulating a coolant fluid through ducts, at high voltage, wound into a coil. To establish quantitative design information, much more extensive test data are required. However, before an extensive program to test pipe insulating strength is carried out, alternative designs of both pipes and alternative materials must be considered.

Two areas need to be addressed in future work on insulating coolant pipe design: 1) the long time aging under combined electrical and mechanical stresses; 2) the possible detrimental effects caused by charge generation in the coolant liquid as it circulates through the insulating coolant pipes.^{7,8}

Meanwhile, it seems reasonable to expect that these insulating coolant pipes can be designed to meet the electrical stress which would be encountered in a full-size power transformer.

3.4 Compressed Gas Insulation for Major, High-Voltage Gaps

Full transformer voltage must be insulated from portions of the coil to the grounded core and pressure vessel. The proposed design therefore, includes high voltage, compressed gas gaps between the coil and the core yoke, and end limbs. Usually, the determining electrical stress in designing these insulating gaps is the stress experienced in commercial test.

Compressed gas is now in such general use in certain types of high voltage equipment that adequate design data for simple geometries are available for both ac and impulse voltages. Permissible design stresses in compressed-gas-insulated gaps depend upon the gas, the geometry of the gap, and the electrodes.

The variables involved are the following:

1. The gas
 - (a) The species of gas or gas mixture
 - (b) The gas pressure

2. The geometry

- (a) Length of gap
- (b) Highest potential gradient
- (c) Area of electrode

3. Electrodes

- (a) Material
- (b) Surface conditions -- finish, possible particle contamination

Figure 3-23 shows some examples of insulating performance achieved in SF_6 . By taking into account the effect of electrode area and the necessity of providing a safety factor between expected withstand and expected breakdown, a "maximum safe electrical stress" can be established for the coil-to-core insulating gap. For example, at 8 atm of SF_6 , this gradient is taken to be 20 kV/mm (peak ac) in this work. As seen from Figure 3-23, this is well below the breakdown value, either theoretically or experimentally.

In some power transformer applications, high voltages must be insulated across the reactance gap between windings. In the model transformer coils built so far in this work, the reactance gap is a tightly wound layer of polymer film exposed to the ambient compressed gas. In other words, the structure of the reactance gap is the same as that of the turn-to-turn gap; and its insulating performance can be considered as fundamentally the same performance as discussed in Section 3.2.

Figure 3-23. Maximum insulating potential gradients in SF_6 gas

In common with all other insulating media, the permissible electric stress in polymer film/gas gaps declines with increasing gap. This is the essential distinction between turn-to-turn gap and the reactance gap: the total voltage across the reactance gap is usually many times that across a turn-to-turn gap. Consequently the reactance gap is greater, and the permissible potential gradient smaller. This theoretical dependence of permissible electric stress on gap is illustrated in Figure 3-24.

3.4.1 Particles in Compressed Gas

Under certain conditions, free particles present in a compressed gas-insulated, high-voltage gap can seriously diminish the insulating strength of the gap.

When particles are at rest on an electrode surface, they can become the loci of electric fields much greater than the average field in the gap, and, as a result, give rise to corona (electrical discharges). As the applied voltage is increased, the particles may become levitated by the electric field and move toward the opposite electrode, when a high field will be generated and a discharge to the electrode can occur. In turn, this discharge can trigger a breakdown of the complete gas gap. This deleterious effect of particles, leading to low level corona and breakdown at lower electric fields than in their absence, has been the subject of much attention, particularly over the past ten years.^{12,13}

Figure 3-24. Threshold gradient for partial discharges in polymer film/SF₆ gaps as a function of gap length

Practical experience has shown that metallic particles are much more harmful than dielectric particles. Under some circumstances, however, dielectric particles may be coated with fine metallic dust which effectively renders the particle conducting. A high voltage gap in which a solid insulating structure is also present has been found to be a situation highly vulnerable to the harmful effects of particles since the particles adhere electrostatically to the insulating surface and become the initiating sites for corona and breakdown. even in the presence of particles. These circumstances include:

1. Apparatus in which a substantial portion of the gas volume is a region of low potential gradient.
2. Electrode geometries which do not inherently tend to trap particles in a region of high electric stress, e.g., sphere-to-sphere gaps.

The fabrication of large pieces of high voltage equipment, under essentially particle-free conditions, is likely to prove expensive and difficult. Even if it were attainable, there remains the problem of possible generation of "particles" in service. For apparatus in a completely sealed pressure vessel, internal sources of contaminating particles may be present: Any moving parts are, of course, possible sources of spurious particles; even if there are no moving parts, vibration, cycling in temperature, and circulation of the insulating gas can cause particles to appear in the insulating gap if they are already present in the system. For these reasons, in recent years, much attention has been directed to devices which will trap particles.¹⁴⁻¹⁹

Turning to the proposed gas-insulated transformer, the highly stressed spaces are those between the outer cylindrical surface of the coil and the end-limbs and those between the upper and lower edges of the coil shield and the upper and lower yokes, respectively. These interelectrode spaces are a small part of the total gas volume; in terms of items (1) and (2), this is an inherently favorable situation. However, potential problem areas are foreseen as being the spaces between the coil shield and the upper cooling duct headers which might be natural particle traps. Particles trapped in this region would be a potential danger if they became lodged between the polymer sheets. Turn-to-turn failures might then occur. Attention may need to be directed to the possible use of insulating barriers (with or without a "fly-trap" particle trap) to prevent the harmful effects of particles in these regions.

The following general observations can be made for the compressed-gas-insulated transformer:

- If the surfaces exposed to high potential gradient are kept clean of visible particles ($>20\text{ }\mu\text{m}$), the maximum insulating strength used in our conceptual designs (see Section 8), about 80 kV/atm-cm in SF_6 , can be achieved. Based on the work in the present program, this degree of cleanliness is considered to be feasible.
- The introduction of particle traps into or adjacent to the highly stressed regions between the high-voltage shields of the coil and the core shields (as shown in Figure 3-25 for a high-voltage test coil) is considered unnecessary because such high stress regions

Figure 3-25. Test coil showing high-voltage shield and core shields

are surrounded by large volumes at very low potential gradients. Furthermore, placing trapping devices in the highly stressed electrodes would complicate the structure of these electrodes and require additional space in the core window.

- Circulation of the compressed gas can bring particles from field-free regions into the highly stressed gaps. If forced circulation of gas is utilized for cooling of the core, this problem could become serious. However, in a large transformer, the gas will circulate due to inevitable temperature differences among parts. Some means for filtering the gas, moving under either natural convection or forced flow, may therefore be necessary.

3.5 Transient Overvoltages in Sheet Windings

The importance of transient overvoltages to power transformer design is demonstrated by the fact that the insulation design of a winding is often determined by the electrical stresses which develop under transient overvoltage.

A lumped-parameter model of a winding is shown in an equivalent circuit in Figure 3-26. The capacitances, C_{si} , are made up, primarily, of the turn-to-turn capacitance. The capacitances, C_{gi} , are the stray capacitances to ground.

Figure 3-26. Schematic diagram of the equivalent circuit of a transformer winding

The model can be greatly simplified with very fast transient waves (in the limit, a voltage "step") if one seeks only the initial response. Then, for times which are short compared to the period of natural oscillations of the winding, no current will have developed in the winding itself and, consequently, the inductances may be considered as infinite impedances. With these simplifications, the equivalent circuit is as shown in Figure 3-27.

Figure 3-27. Simplified schematic of a winding, applicable to the initial distribution of voltage

As a further simplification, let each C_s be identical to the rest (which is often the case in practice) and let each C_g be identical to the rest (which is not as often the case). Then it is readily shown²⁶ that the initial response to a voltage step of magnitude E_0 is a voltage distribution $e(x)$:

$$e(x) = E_0 \frac{\sinh(\alpha x / \ell)}{\sinh \alpha}$$

where

x = linear distance through the winding

ℓ = total length of the winding

$$\alpha = (C_g / C_s)^{1/2}$$

It is evident that the voltage gradient, de/dx , will be greatest at the line end of the winding, where it has the value

$$E_{\max} = \alpha \frac{E_0}{l} \coth \alpha.$$

If $\alpha > 3$, this is approximately

$$E_{\max} = \alpha E_0 / l.$$

This last expression displays the essential point of this discussion; the maximum gradient in the voltage distribution is proportional to $\alpha = (C_g/C_s)^{1/2}$. This quantity, for a given winding, has come to be taken as a measure of the possible severity of response to transients. Calculations have been made of C_g and C_s for the high-voltage winding of the conceptual 500 MVA, 500 kV(Y)/20kV single phase power transformer described in Section 7. The winding is comprised of five sections of sheet winding, each of average capacitance = 35,000 pF, divided by four cooling ducts for each of which the capacitance from header to ground is 450 pF. The value of α therefore is ~0.1. We therefore expect very little stress enhancement in this conceptual high-voltage sheet winding when subjected to an applied transient voltage.

One cannot totally rely on the above treatment, however, because the actual dynamic behavior, after currents have developed in the winding, can, in some cases, give rise to more severe gradients than were present in the "initial voltage distribution." A preliminary assessment of this situation has been made in a transient analysis study of a model sheet winding.

3.5.1 Testing of Transient Analysis Coil for Sheet Windings

As supported above for a conceptual 500 MVA, 500 kV single phase power transformer, because of the unusually large turn-to-turn capacitance and low stray capacitance compared to typical wire windings, sheet windings will have inherently very small values of the parameter " α ". This implies that the initial voltage distribution due to an applied impulse voltage will be nearly uniform. However, in general the applied impulse voltage will result in electrical oscillations within the winding, a situation that is excluded from the simplified analysis based on Figure 3-27; such oscillation could then result in significant departures from a uniform voltage distribution. It is therefore important to conduct experiments to measure the actual voltage distribution, in particular to measure the waveform of the voltage at different points within a winding. The dynamic aspects of the transient voltage are studied in practice by applying a recurring low voltage surge to a coil structure and observing its response. These waves are typically shaped to match waveforms which have been identified as typical of observed lightning and switching overvoltages. To this end, a coil was constructed during the present program, and comprehensive full- and chopped-wave measurements were carried out for a range of lead connections and for simulated cooling duct header-to-ground capacitances.

A model coil designed for transient voltage measurements (Figure 3-28) had the following specifications:

Figure 3-28. Test coil used for transient voltage measurements at the completion of construction. Labels are shown attached to voltage probes.

Winding form (Fiberglass-reinforced epoxy cylinder)

O.D.	0.55 m
Length	0.755 m
Wall thickness	2.54 cm

Inner winding (Low voltage winding)

Turns	100
Conductor	Aluminum sheet, 61 cm x 0.025 mm
Turn insulation	Two sheets of polymer film, 62 cm x 0.025 mm

Reactance gap

Total gap	1 mm
Construction	40 turns of polymer film, 62 cm x 0.025 mm

Outer winding (High voltage winding)

Turns	1600
Conductor	Aluminum sheet, 61 cm x 0.025 mm
Turn insulation	Two sheets of polymer film, 62 cm x 0.025 mm

Probes were wound into this coil at 100-turn intervals; in the vicinity of leads and simulated cooling duct headers, the probes were located a few turns apart to achieve greater sensitivity in detecting possible localized voltage enhancements at winding discontinuities. These probes were strips of

thin aluminum sheet 63 cm x 0.75 cm x 0.025 mm. They were laid parallel to the winding axis with their ends protruding from the winding. To these probes measuring equipment could be connected, and to some probes, capacitors were attached, connecting to ground, to simulate the stray capacitance-to-ground of cooling headers.

Measurements on the sheet winding are complicated by the fact that the number of turns per unit length, in the radial direction, is very high. Consequently, there can be strong inductive coupling between the measuring leads and the winding. This difficulty was overcome by suitably shielding the measurement leads.

No Capacitances Attached to Coil

The simplest situation is that in which the inner end of the outer winding is grounded, the inner winding is open circuited, and the test voltages are applied at the outer end of the outer winding. Waveforms of the winding voltage were measured to obtain the dynamic effects. Figure 3-29a shows the applied voltage at the outer winding terminal and Figure 3-29b shows the voltage at the midpoint of the outer winding.

Waveforms such as those of Figure 3-29 (a and b) record the winding response to impulse voltages in all its complexity. A common means of summarizing waveforms from many places in a winding and presenting a simplified picture in a single graph is shown in Figure 3-30a. In this graph, the highest voltage (above ground) seen on each oscillogram is plotted against the distance (number of turns) from one terminal of the winding. (Note: This is not the initial voltage distribution.) Following the usual practice, the ordinate scale is normalized to 100 for the peak of the applied impulse. A similar graph for the same coil and lead connections, but measured from line to turn, is shown in Figure 3-30b. In both cases (Figures 3-30 a and b), we note that the voltage peaks are distributed in a nearly uniform manner as would be predicted from Figure 3-27 for $C_s \gg C_g$.

If the inner winding is short-circuited, the voltage oscillations in the outer winding became much more pronounced. Figure 3-31 shows the voltage waveform at the midpoint of the winding in this case. (The oscillations evident in Figures 3-29b and 3-31 appear to have a periodicity which agrees fairly well with the calculated period $(41 \mu s)^*$ of the primary resonance of the outer winding.)

The distribution of peak potential differences generated from turn-to-ground waveforms such as Figure 3-31 is shown in Figure 3-32a. As with Figures 3-30 a and b, the voltage distribution is seen to be linear. However, when the high voltage line-to-turn potential is plotted (Figure 3-32b), there

*Calculated assuming that the coil forms a transmission line:

Transit time for a disturbance from line to ground and return

$$\begin{aligned}
 &= 2 \times \frac{\text{Length of conductor}}{\text{Velocity of propagation in the coil}} \\
 &= 2 \times \frac{3450 \text{ m}}{3 \times 10^8 \text{ m/sec}} \times (3.2)^{1/2} = 2 \times 20.57 \mu s \approx 41 \mu s
 \end{aligned}$$

(using $\epsilon = 3.2$ for the dielectric constant of the polymer film).

Figure 3-29. Voltage waveforms for the following case: no added capacitance, inner winding open circuited.
 (a) Applied voltage at winding terminal (1600 turns to ground)
 (b) Voltage at midpoint of winding (800 turns to ground)

is a pronounced nonlinearity in the voltage distribution in the winding. (Again, it should be noted that this is not the initial voltage distribution.) This difference is caused by the fact that the peaks of the turn-to-ground wave do not occur at the same time as the peak of the line-to-turn (turn 1600) wave.

We note that by short circuiting the low voltage winding, the resulting high mutual coupling effect results in a nonlinear potential distribution in the winding.

Addition of Capacitances to Simulate Cooling Duct Headers

If capacitances to ground are added to the high voltage winding, it is possible to simulate the cooling ducts that would be used in a larger power transformer. To simulate the capacitance-to-ground of the headers of the cooling ducts for conceptual 500 MVA, 500 kV single phase power transformer,

Figure 3-30a. Distribution of peak potential differences within the winding for the following case: No added capacitance, inner winding open circuited. Measurements from turn-to-ground.

Figure 3-30b. Distribution of peak potential differences within the winding for the following case: No added capacitance, inner winding open circuited. Measurements from high voltage line-to-turn.

Figure 3-31. Voltage waveform at the midpoint of the winding (800 turns-to-ground) for the following case: No added capacitance, inner winding short circuited

it was calculated that capacitors of 16 pF were needed for a coil of our size. Capacitors of this size were attached to the winding at the 1/4, 1/2, and 3/4 points in the winding. A capacitance of 33 pF to ground was also added to the outer turn to simulate the bushing capacitance of the same conceptual power transformer; to simulate the lead inductance, 5 m and 20 m long leads were attached to the ground and line ends, respectively.

Figure 3-33 (a and b) shows the resulting voltage waveforms for measurements from the midpoint of the winding (800 turns) to ground. In Figure 3-33a, the low voltage winding was short circuited; in Figure 3-33b, a resistance of 940 Ω in series was added to the low voltage winding - this was done to simulate another test condition sometimes used in testing power transformer coils. The two waveforms show very little difference from those of Figures 3-31 and 3-28b, respectively. The waveforms measured at each other location in the coil were also closely similar to those obtained in the absence of added capacitances; hence the resulting voltage distributions should be similar to those of Figures 3-30 (a and b) and 3-32 (a and b), i.e., dependent only on the way in which the low voltage winding leads are terminated. In Figure 3-34, this similarity is shown for the measurements obtained from line to turn (cf. Figure 3-32b). These results indicate that, for capacitances scaled-down from those of the proposed cooling duct headers of a conceptual EHV power transformer, to match the capacitance of our coil, there is little effect of header capacitances on the voltage distribution within the winding.

As a matter of interest, large capacitances (1800 pF) were added in place of the 16 pF capacitances at the same 1/4, 1/2, and 3/4 point of the winding. These capacitances were roughly 100 times larger than the present header design would give for a coil of our size. The addition of such capacitances caused a significant nonlinear distribution of the voltage in the winding. This result illustrates the need to avoid high capacitances to ground for the cooling duct structures which might be used with these proposed new designs.

The effect of the addition/removal of the 6 m long lead attached to the winding was also studied. The added inductance due to the lead changed the

Figure 3-32a. Distribution of peak potential differences within the winding for the following case: No added capacitance, inner winding short circuited. Measurements from turn to ground.

Figure 3-32b. Distribution of peak potential differences within the winding for the following case: No added capacitance, inner winding short circuited. Measurement from high voltage line to turn.

(a)

(b)

Figure 3-33. Voltage waveforms at midpoint of winding (800 turns to ground) for the following case: 16 pF capacitances added at 1/4, 1/2, and 3/4 point of the winding; 33 pF capacitance added from turn 1600 to ground

(a) Inner winding short circuited

(b) Resistance of 940 Ω to ground added

Figure 3-34. Potential distribution within the winding for the following case: 16 pF capacitances added to the 1/4, 1/2, and 3/4 points of the winding; 33 pF capacitance added from turn 1600 to ground; 5 m and 20 m leads added to the ground and line ends of the outer winding. Measurements from high-voltage line to turn.

observed oscillations slightly: One of the results was that the peak voltages appearing across the winding were on the order of a few percent higher than the peak of the test voltage applied at the ends of the leads.

Other Studies Conducted on Transient Analysis Test Coil

Both full-wave and chopped-wave measurements were made during this investigation. For these windings, under the test conditions evaluated, the line-to-turn stress distribution was generally worse (i.e., more nonuniform) when a full wave was applied to the high voltage terminal than when a chopped wave was applied. An example of this is shown in Figure 3-32b. Whether this is generally the case for all sheet windings cannot be said at this time. Additional experimental and analytical work on the response of sheet windings to full, chopped, and other testing waveforms will be needed in the future.

It was also of considerable interest to examine whether the potential difference measured between different positions in the winding, particularly across a few turns of the winding, might be significantly greater in magnitude than the potential difference between the peaks of the waveforms at two such positions, measured from the high-voltage line to each turn separately or from each turn to ground. For example, this can occur if the peak of a waveform measured from a particular turn to ground does not occur at the same time as a turn-to-ground measurement several turns away. If this situation prevails, the local electric stresses could be much higher than computed from figures such as Figures 3-30 (a and b).

To examine the above possibility, measurements were made of the turn-to-turn potential difference, i.e., the difference in waveform between probes. An example of the "turn-to-turn" data obtained during the present study is shown in Table 3-2 for the test configuration of Figure 3-35. (Note: This is the same test configuration used to evaluate the effect of simulated headers (Figure 3-34).) Both full- and chopped-wave results are included in Table 3-2. The percent voltage is the maximum potential difference observed (at any time) between two turns, expressed as a percentage of the maximum applied voltage. The stress enhancement factor (= ratio of percent voltage to percent winding) is a measure of the deviation from a linear stress distribution in the winding. In Figure 3-36, stress enhancement data from Table 3-2 has been plotted as a function of position in the winding. We note that the maximum departure from a linear stress distribution occurs at the outer turns of the high voltage winding. However, the maximum stress enhancement is only slightly greater than two (i.e., twice the uniform stress distribution).

Table 3-2

TURN-TO-TURN POTENTIAL MEASUREMENTS ON TRANSIENT ANALYSIS TEST COIL;
TEST CONFIGURATION SHOWN IN FIGURE 3-34

Test Measurement	Percent Winding	Percent Voltage		Stress Enhancement Factor	
		Full Wave	Chopped Wave	Full Wave	Chopped Wave
1600-1595	0.3125	~0.67	~0.57 a.c.	2.14	1.84
1600-1585	0.9375	2.07	1.73 a.c.	2.21	1.84
1585-1570	0.9375	2.07	1.7 a.c.	2.21	1.81
1230-1215	0.9375	1.7	1.1	1.81	1.17
1215-1200	0.9375	1.67	1.1	1.78	1.17
1205-1200	0.3125	~0.5	~0.4	1.6	1.28
1200-1195	0.3125	*	*	*	*
1200-1185	0.9375	1.7	1.1	1.8	1.17
1185-1170	0.9375	1.63	1.1	1.73	1.17
810-800	0.625	0.97	0.77	1.55	1.23
800-790	0.625	0.93	0.77	1.5	1.23
30-15	0.9375	~0.67	~1.26 c.s.	0.71	1.34
15-0	0.9375	~0.63	*	0.67	*

a.c. - peak is after chop

c.s. - chopped voltage peak

*Values below measurement sensitivity

Comparing the data of Figure 3-36 with that shown in Figure 3-34, in the latter case examining the gradient of the distribution of the peak potential difference for both full and chopped waves, we note roughly the same behavior throughout the winding for the full-wave case (i.e., a stress enhancement of just over two at the outer turns, decreasing steadily through the winding to

Figure 3-35. Schematic representation of the test configuration for turn-to-turn potential difference measurements, showing the location of the turns

Figure 3-36. Plot of stress enhancement as a function of position in the winding corresponding to the turn-to-turn measurements shown in Table 3-2

a value just over 0.5 at the inner turns), but a somewhat larger stress enhancement obtained from turn-to-turn than from line-to-turn measurements for chopped waves. In future studies of the response of sheet winding to transient voltages, turn-to-turn, line-to-turn, and turn-to-ground measurements should always be made.

Discussion

This preliminary study of transient voltages in sheet windings has been directed to the voltage responses in a two-winding coil. Future investigations of the transient behavior of sheet windings need to address different winding structures (configurations) representing different transformer designs. Attention to the effect of different lead structures and the presence of taps should be included in such investigations.

It was originally anticipated that it would be possible to perform transient analysis measurements on any coil that was constructed in the present program, particularly those intended for testing under transient voltages. To do this, it was hoped that probes could be inserted into the winding following high voltage testing of the winding; it was necessary that the probes make continuous contact with the aluminum sheet without puncturing the polymer film and thereby causing a shorted turn with consequent errors in measurement. A variety of probes was tested for this purpose: steel and copper wires of various diameters (0.13 to 0.38 mm), metallic ribbon (6 mm wide, 0.125 mm thick) with a sharpened edge, and different sized needles connected to shielded cables. However, in all cases, problems resulting from the tightly-wound structure and small space (between polymer films) in which to insert the probes -- resulting in damage to the film, poor electrical contact, etc. -- made it an

impractical task. It was therefore concluded that the only way in which to perform transient analysis tests was by using a coil dedicated to this end. The test coil described above was constructed for this purpose.

Conclusions from Transient Analysis Tests on Model Coil

- It has been demonstrated that successful transient analysis tests on sheet-wound coils can be made using coils constructed with narrow aluminum sheet probes running the full axial length of the coil; low level voltage waveforms can then be measured; measurements can be made on probes located only a few turns apart.
- The basic sheet-wound coil examined in the present study exhibited a nearly linear stress distribution when subjected to transient voltages. A major finding was that the termination of the low voltage winding (i.e., whether open circuited, short circuited, or terminated in a resistor) had a strong effect on the stress distribution. The generality of this latter statement to other sheet windings needs to be determined.
- The presence of cooling duct headers or other structures (e.g., shields) that give rise to stray parallel capacitances to ground leads to more nonuniformity in stress distribution.
- The present study indicates that the stress distribution arising from transient voltages applied to sheet windings will be more favorable (i.e., more uniform) than in conventional wire windings. However,
 - additional analytical and experimental investigations of nonuniform sheet winding configurations are needed
 - it is generally anticipated that the situation will be less favorable as discontinuities (leads, taps, etc.) are introduced to the winding.

3.5.2 Testing of Impulse Test Coil

An experimental test coil was designed specifically for impulse tests. It was a single, sheet aluminum winding with high-voltage and ground shields each designed to minimize the chances for corona or breakdown in the SF_6 gas outside the coil. The turn-to-turn insulation was two sheets of polymer film in series, impregnated with SF_6 gas at 6 atm abs. Impulse voltages of prescribed waveshape were applied through a high-voltage bushing rated at 1300 kV BIL.

The impulse test coil at the completion of construction is shown in Figure 3-37. A schematic drawing of the construction is given in Figure 3-38. The major design features and details of construction are outlined below.

Major Design Features

- High-voltage ring shields and ground ring shields used an insulated gap in them to prevent shorted turns. The gap was set between hemispherical sections on each end of the ring.

Figure 3-37. Impulse test coil following construction

- Similarly, the aluminum sheet ground shield and high-voltage shield had insulated lap joints.
- The ground shield was wound directly onto the fiberglass cylinder and ran from edge to edge of the cylinder.
- Heavy taps ran axially the full winding width to provide contacts to the first and last turns.
- There was a high-voltage shield directly over the winding followed by fiberglass tape and another high-voltage shield. Only the outer high-voltage shield was electrically connected to the high-voltage ring shields; this avoided submitting the fiberglass-epoxy tape to electrical stress.
- A 1.27-cm-diameter threaded rod for connection to the high-voltage bus was screwed into a 4.44 cm x 0.63 cm x 50.8 cm aluminum plate just beneath the layer of fiberglass tape. This provided a strong point of attachment for the high voltage bus as well as a heavy tap for distribution of current along the coil axially. This plate was attached to the underlying high-voltage shield and last turn but insulated from the ring shields. It was attached through 0.38 mm aluminum straps to the outer high-voltage shield in the center only.
- The clamps holding the ends of the grounded ring shields at the ends of the cylinder were insulating clamps and faced outward axially so they did not modify the electric field between these ground ring shields and the high-voltage ring shields.
- The clamps holding the high-voltage ring shields together were also insulating clamps. They faced inward axially to avoid the main electric field. Over the two clamps holding the high voltage ring shields was

Figure 3-38. Schematic drawing of an axial cross section of the impulse test coil

fastened an O-shaped ring shield to reduce the field to the tank wall from the sharp corners of the clamps. This O-shaped ring shield also had an insulated gap in it; it was attached electrically to the ring shields at only one point. It was attached mechanically by an insulated connection at one other point.

- The coil was supported on an insulating support table directly in the pressure tank. There were no obstructions between the outside surfaces of the coil and the tank walls; there was no core. The ground connection was a 2.54 cm wide copper braid leading to an insulated bushing through the tank wall; the copper braid was used for current measurements during test.

Details of Construction

Winding Cylinder

The cylinder was fiberglass epoxy; it was 50.9 cm ID and 55.7 OD. It was 77.5 cm in the axial dimension. The cylinder was accurately machined over the whole external surface and to a distance of 5.08 cm from the ends on the internal surface.

Ground Shield

The ground shield was made from two pieces of 0.3 mm thick aluminum wound around the cylinder and butted at the center of the cylinder axially. There was insulation between the ends of the shield circumferentially, consisting of a 7.6 cm wide, 0.1 mm thick sheet of polymer film running axially between the ends of the shields. If we call this location on the circumference of the shield IGG (standing for insulated gap in the ground shield), then at 180° around the circumference of the cylinder an extra tap was provided underneath the ground shield. This tap was a 15.24 cm wide, 0.3 mm thick aluminum sheet and ran from edge to edge of the cylinder axially. It was cemented with conducting epoxy to the overlying ground shield. We refer to this circumferential location as GT (for ground tap).

Ground Ring Shields

These were 1.9-cm-diameter aluminum tubing with a 3.2 mm wall thickness. They were specially fabricated, using a machine modified for this purpose, to conform exactly to the diameter of the cylinder with its overlying ground shield. The ends of the rings were threaded inside to accept brass fittings of the exact outside diameter of the tubing. Each of these fittings was machined at its projecting end into the form of a hemisphere. The technique was to accurately measure the distance between the threaded ends of the ring shields and to fabricate the inserts so that there remained about a 1.27 mm gap between the opposing surfaces of the hemispheres when in place. Polymer tape was also used on the surface of each hemisphere to ensure the insulation of the gap. The very small cracks where the fittings screwed into the tubing were filled with conducting epoxy and polished. The insulated gap in the rings was at position IGG.

Ring Shield Clamps

The ring shields had L-shaped aluminum pieces bolted to them on the outside surfaces of the tubing. Holes in the projecting sections of the L-shaped pieces accepted a special tightener with interior threading on the end which was insulated from the "L" by means of a polymer bushing. The other end of the tightener was a regular bolt.

Winding

To the ground shield at position GT was connected the inner end of the 55.9 cm wide, 0.076 mm thick aluminum winding. It was cemented with conducting epoxy resin to the ground shield for a distance of 7.6 cm circumferentially and the full axial width of the sheet.

The turn insulation consisted of two sheets of 62.23 cm wide, 0.0233 mm thick polymer film.

Since the polymer film projected 3.17 cm beyond the edge of the aluminum on each side, it was also necessary to provide 2.54 cm, 0.076 mm polymer film strips to support the projecting polymer film. In previous coils, it was felt unnecessary to have the polymer film project this far, but it was decided to do so in this case to reduce the likelihood of a gas breakdown between the high voltage ring shield and the ground shield.

It was necessary to accurately machine the aluminum sheet while wound on the supply roll to provide aluminum edges without defects. This was judged to be a critical step in the construction of the winding.

The winding consisted of 164 turns of the 0.076 mm aluminum sheet and the two layers of 0.0233 mm polymer film. The winding was ended at a position circumferentially directly above IGG.

Inner High Voltage Shield

At this point, the center of the high voltage grading shield, made of 55.9 cm wide, 0.076 mm thick aluminum sheet, was cemented in place using conducting epoxy. The joint was 7.6 cm in the circumferential direction. The two ends of the shield were carried around the winding to a point directly over GT using 2 layers of 0.0233 mm polymer film as insulation between the shield and the underlying winding. The ends were overlapped circumferentially but insulated from each other with 2 layers of 0.0233 mm polymer film and taped in place.

At position IGG, the 4.44 cm x 0.63 cm x 50.8 cm aluminum bar was cemented in place over the high voltage shield using conducting epoxy. In the center of this bar was provided a hole drilled and tapped to accept a 1.27-cm-diameter stud with 7.87 threads/cm. Also attached by conducting epoxy to the center of this bar were two tap straps 3.8 cm wide x 0.3 mm thick which were to come out through the fiberglass tape (applied next) alongside the bolt and were used to make electrical connection to the high-voltage shield.

High-Voltage Ring Shields

The high-voltage ring shields were constructed just like the ground ring shields and were clamped around the winding with the center of the tubing directly over the edge of the aluminum but insulated from it with a 2.54 cm wide, 0.076 mm thick polymer tape. The gaps in the rings were at position GT. Clamps were also at that point. These rings were also insulated with polymer tape from the aluminum bar. The clamps were like the clamps in the ground ring shields but facing inward.

Outer Girdle

The outer girdle consisted of 7 turns of 2.54 cm wide, 0.38 mm thick fiberglass tape impregnated with epoxy resin. It was wrapped over the aluminum bar in such a way that the bar was held mechanically by the tape. The two tap straps cemented to the center of the bar passed through the tape centrally and were folded over the tape toward the two ends of the windings.

Outer High Voltage Shield

This consisted of a piece of 0.30 mm thick aluminum just wide enough to wrap around the winding between the two high-voltage ring shields. The insulating gap in this outer shield was just above the gap in the inner high-voltage shield at position GT and beside the gap in the ring shields. The outer high-voltage shield was connected electrically by cementing with conducting epoxy to the underlying straps coming up from the aluminum bar. It was also cemented with conducting epoxy to the inner sides of the high-voltage ring shields being careful to keep the foil and cement as far in (radially) as possible on the tubes.

Mounting Stud

After the fiberglass girdle and outer high voltage shield were applied, the threaded hole in the underlying aluminum bar was cut out and the stud inserted.

Ground Connection

The ground connection was made at position GT by means of 6 bolts from inside the inner cylinder. These bolts were tapped into the ground shield rings and clamped the rings down against the ground shield and tap strap. Inside the cylinder, the bolts clamped a piece of 2.54 cm wide copper braid against the cylinder wall. Connection between the braid and the winding was through the bolts and ring shield and tap strap.

The ground ring shields were also contacted electrically to the ground foil layer by means of a conducting epoxy filler along the outside of the ring shield between the shield and the underlying foil.

Test Procedure

The coil was positioned on an insulating support table in the pressure vessel. Connection to the ground lead on the inside of the fiberglass cylinder was made prior to replacing the top of the pressure vessel. The high-voltage lead was then connected to the mounting stud on the exterior surface of the

high-voltage shield; following this, the high-voltage bushing was added and the high-voltage lead connected to it. The final arrangement is shown schematically in Figure 3-39.

After the pressure vessel was sealed, it was evacuated to a pressure less than 100 μm for over 24 hours. The vessel was then filled with SF_6 gas at 6 atm abs and the coil was allowed to impregnate in this condition for over 48 hours prior to testing.

The circuit shown in Figure 3-40 was used for conducting the tests. The generator was set up with 5 sections in parallel in each of 3 sections in series, giving an overall capacitance of 0.18 μF . The line inductance was estimated at 80 μH . In series with the sample was a resistance of 180 Ω , a value reached after examination of the waveform produced by trying several resistors; the sample had an inductance of 7.9 mH.

As shown in Figure 3-41a, the voltage waveform produced by this combination of circuit elements had a rise time of 1.32 μs and a decay time to one-half voltage of 23.5 μs . All the tests had this same waveform. In Figure 3-41a, the sweep time was 5 $\mu\text{s}/\text{cm}$. In Figure 3-41b, the same voltage wave is shown with a sweep time of 20 $\mu\text{s}/\text{cm}$; this shows the voltage dropping through zero in about 42 μs and going to reverse polarity before finally becoming zero. Figure 3-42 shows the current waveform with a sweep time of 50 $\mu\text{s}/\text{cm}$. This shows the current peak being reached at 50 μs , just after the voltage wave had reached zero. Thereafter, the current drops, reverses for a short period, and finally approaches zero.

Figure 3-39. Schematic view of the lead structure for testing the impulse test coil

Figure 3-40. Test circuit used for the impulse test coil

Results

During calibration of the equipment and determination of the proper R_S , six negative polarity and three positive polarity impulses at 70 kV were applied successfully without failure.

After this, the peak voltage was raised to the 100 kV level. Three withstands of positive polarity at 102 kV and three negative at 107 kV were recorded at this level.

The voltage level was increased to 150-160 kV; four positive impulses at 157 kV and six negative at 158 kV were applied without incident.

The voltage was raised to the 200 kV level and the first positive polarity impulse caused a failure accompanied by a bright light visible at one of the viewing ports, indicating that breakdown had occurred in the gas space. Visual examination of the test coil through the sight glass revealed a carbonization mark on the polymer film outside the high-voltage ring shield.

The voltage was dropped to 102 kV and a positive withstand was achieved at that level. Two more positive withstands were recorded at 151 and 157 kV.

The voltage was again raised to the 200 kV level and a withstand at 199 kV, positive, was recorded. This is considered to be very strong evidence that the first breakdown was in the gas space. Another impulse at the 200 kV positive level again resulted in a breakdown accompanied by bright visible light. After this failure, the voltage was again lowered to the 150 kV level and a withstand at 155 kV positive was shown. This is considered to be additional evidence that the failures at 200 kV were in the gas space and that the coil is still undamaged.

Upon removal, two carbonized tracks on the polymer film extending under the UPPER high-voltage ring shield were found. There was evidence in the form of tiny depressions on the aluminum sheet ground shield, just outside the edge of the polymer film, that the other end of the failure was on the sheet ground shield. The two failure sites are shown in Figures 3-43 and 3-44.

(a)

(b)

Figure 3-41. Impulse voltage waveform: (a) sweep time = 5 $\mu\text{s}/\text{div}$;
(b) sweep time = 20 $\mu\text{s}/\text{div}$

Figure 3-42. Current waveform associated with impulse wave shown in Figure 3-41

Figure 3-43. One of the two failure sites on impulse test coil

Figure 3-44. Other of the two failure sites on impulse test coil

Discussion

The impulse test shows that the winding itself had withstood an average stress of 26.1 kV/mm on the polymer film without failure. Although the test coil was not subjected to a transient analysis test, on the basis of the results obtained on the transient analysis test coil described in the previous section, it is considered that the stress distribution throughout the winding would have

been nearly uniform (26.1 kV/mm) under the imposed impulse voltages. Since the breakdown occurred in the gas space external to the coil, there is good reason to believe that higher test voltages can be achieved on a winding of this construction.

The failure due to breakdown from the high voltage shield, over the polymer film surface, to ground - a surface creepage failure - indicates that more refinement is needed in shield design. In actual prototype transformers, the stress at the shield at the gas phase would be much lower due to the greater spacing between high voltage and ground caused by the greater conductor thickness in the winding.

Further tests on the sheet winding construction are needed to determine the impulse strength of the SF₆ gas impregnated polymer film insulation structure. Future tests should also address creepage performance in SF₆ gas over the polymer surface.

Conclusions

- A 164-turn sheet-wound coil has withstood impulse voltage (1.32 x 23.5 μ s) stresses of 26.1 kV/mm without failure.
- Further impulse tests are needed to determine the impulse capability of the SF₆-gas-impregnated polymer film insulation structure. Attention needs to be directed to the creepage performance over the polymer film/SF₆ gas surface.

Section 4
MAGNETIC DESIGN

Section 4

MAGNETIC DESIGN

4.1 Reactance Flux and Eddy Currents in Windings

4.1.1 Calculation of the Reactance of Sheet-Wound Transformer Coils

A simple formula for transformer reactance is readily derived from fundamentals; this formula yields a value of reactance which is quite accurate in the case of sheet-wound coils.

The derivation, as developed below, requires the following assumptions:

1. The magnetic field intensity, H , varies radially across the coil in the manner shown in Figure 4-1.
2. The region outside of the coil assembly and its included reactance gap has infinite permeability.
3. There is no fringing of magnetic field, therefore H has only a vertical (z) component throughout the coil and $H_z = H$ is independent of z .

Figure 4-1. Schematic drawing of a coil showing how the magnetic field is assumed to vary in calculating the reactance of sheet windings

The problem is treated in two dimensions. This approximation to the actual situation becomes more accurate as the radial builds, d_i , d_g , and d_o , become small compared to the radii. In view of these assumptions, the magnetic intensities H_i , H_g , and H_o , in the inner winding, gap, and outer windings, respectively, are given by:

$$H_i = \frac{NI}{h} \frac{x}{d_i} \quad (0 \leq x \leq d_i)$$

$$H_g = \frac{NI}{h} \quad (d_i \leq x \leq d_i + d_g)$$

$$H_o = \frac{NI}{h} \left[\frac{d_i + d_g + d_o - x}{d_o} \right] \quad (d_i + d_g \leq x \leq d_i + d_g + d_o)$$

where NI = ampere turns of either winding.

The energy, U , stored in an inductor is given by the expression

$$U = 1/2 LI^2, \quad (4-1)$$

where I is the current and L is the inductance, hence, the inductance can be computed from the energy stored per unit current. The energy density, U , in the field being

$$U = 1/2 \vec{B} \cdot \vec{H} \quad (4-2)$$

For the inner winding, outer winding, and reactance gap, the stored energies are given below.

Inner Winding:

$$U_i = 1/2 \int_0^{d_i} \mu_o H_i^2 d\tau = \frac{\mu_o N^2 I^2}{2h^2 d_i^2} \int_0^{d_i} 2\pi R_i h x^2 dx \quad (4-3)$$

$$U_i = \frac{\pi \mu_o N^2 I^2}{h} \frac{R_i d_i}{3} \quad (4-4)$$

Outer Winding:

$$U_o = \frac{\mu_o N^2 I^2}{2h^2 d_o^2} \int_{d_i+d_g}^{d_i+d_g+d_o} 2\pi R_o \left[\frac{d_i+d_g+d_o-x}{d_o} \right]^2 dx = \frac{\pi \mu_o N^2 I^2}{h} \frac{R_o d_o}{3} \quad (4-5)$$

Reactance Gap (Field intensity constant):

$$U_g = \frac{\pi \mu_o N^2 I^2}{h} R_g \text{ hdg,} \quad (4-6)$$

Hence, the total stored energy is

$$U = \frac{\pi \mu_o N^2 I^2}{h} \left[\frac{R_i d_i}{3} + R_g d_g + \frac{R_o d_o}{3} \right] \quad (4-7)$$

Combining Equation 4-1 and 4-3, the inductance can be obtained:

$$L = \frac{2\pi \mu_o N^2}{h} \left[\frac{R_i d_i}{3} + R_g d_g + \frac{R_o d_o}{3} \right] \quad (4-8)$$

Equation 4-8 is a more accurate expression for a sheet-wound transformer than it is for a wire-wound transformer because, for the former, assumption #3 is better fulfilled. Based on assumption #2, the formula will be more accurate when the windings more nearly fill the core window. This will tend to be the case for large power transformers.

Transformer inductance, L , is not so commonly used as is the transformer reactance, ωL , expressed in percent of "per unit."

Let Z = the reactance in percent. Then

$$Z = \frac{IX}{V} \times 100$$

where I is the nominal current through the winding and V the associated nominal voltage. It is convenient also to use the fact the $V = Ne$, where e is the voltage per turn. Equation 4-8 then yields

$$Z = 4.961 \times 10^{-3} \frac{IVF}{e^2 h} \left[\frac{R_i d_i}{3} + R_g d_g + \frac{R_o d_o}{3} \right] \quad (4-9)$$

Since the transforming capacity, P , is $P = IV$, Equation 4-9 can be written

$$Z = 4.961 \frac{PF}{e^2 h} \left[\frac{R_i d_i}{3} + R_g d_g + \frac{R_o d_o}{3} \right] \quad (4-10)$$

where P is the capacity in kVA.

For sheet-wound transformers, the principal source of error in this formula is the reduction of the reactance caused by the space between the top (and bottom) of the coils and the core. In conventional power transformers,

one way of introducing a correction for the fringing of the magnetic field which occurs within the windings, including the space between the top (and bottom) of the winding and the core, is to replace the actual winding height, h , by an "effective height," h_{eff} ; empirically it has been found that h_{eff} has the form shown in the following expression

$$h_{\text{eff}} = h_{\text{act}} + 0.8 d$$

where $d = 1/3$ (radial build of inner winding) + gap between the windings (metal to metal) + $1/3$ (radial build of the outer winding).

We believe that a similar empirical relationship for effective coil height can be developed for sheet windings to improve the above reactance formula to an accuracy of one percent or better.

4.1.2 Summary Description of the Eddy Current Problem

Eddy currents arise whenever conducting materials are exposed to magnetic fields which change with time. Since the conductors have finite resistance, the induced currents cause heating within the conductor.

The larger the surface area over which the magnetic flux can act, the greater will be the induced current and the greater the energy lost in producing heat. If at least one dimension of the conductor can be made small enough, then the losses due to eddy currents can be kept below a chosen value.

In conventional transformer practice, it is precisely the eddy current problem which limits the dimensions of the wires to be used in the windings. A wire whose cross-sectional dimensions exceed 2 or 3 mm x 5 or 10 mm in the radial and axial directions of the coil, respectively, is an example of the upper limit of acceptable wire size. Consequently, it is sometimes necessary to use a very great number of wires in parallel to achieve the desired conductor cross section. For example, in an illustrative 500 MVA generator transformer design, the low-voltage winding required a conductor made up of 300 individual wires, each separately insulated for each electrical turn of the coil. This compares with two conducting sheets in parallel required by a sheet winding.

There are some interesting and significant differences between the manifestations of eddy currents in sheet-windings and those in wire windings.

These differences arise from the different dispositions of conducting material in the two cases, and the resultant eddy current generation by the reactance flux. Figure 4-2 is a schematic drawing of the different reactance flux patterns in the sheet-wound and the wire-wound cases.

The concept of a "skin depth" for induced currents is generally useful, and is helpful in discussing Figure 4-2. When a changing magnetic field induces a current in a conductor, the induced current, in turn, produces another magnetic field which always opposes the original field (Lenz's Law). It follows then, that induced (eddy) currents act to exclude magnetic flux from the interior of a conductor. The skin depth provides a quantitative measure of this exclusion in that the oscillating magnetic flux is able to penetrate the conductor approximately one skin depth.

Figure 4-2. Patterns of reactance flux in a transformer coil:
(a) wire-wound coil (b) sheet-wound coil

If z is the depth into a conductor from a surface exposed to an oscillating magnetic field, then the eddy current density, J , can be shown to be²⁹

$$J(z) = J_0 e^{-z/\delta} \cos(\omega t - z/\delta), \quad (4-11)$$

where

$J(z)$ = density of induced current at a depth z within the conductor

J_0 = density of induced current at $z = 0$

ω = angular frequency of the magnetic field

δ = the skin depth = $\sqrt{\frac{2\rho}{\omega\mu}}$

ρ = specific electrical resistance

μ = magnetic permeability of the conductor

For example, in aluminum at 125 °C, for a field oscillating at 60 Hz,
 $\delta = 12.74$ mm.

The derivation of Equation 4-11 above also yields a similar expression for the magnetic flux density $B(z)$ within the conductor. This also decreases exponentially by the factor $e^{-z/\delta}$.

In Figure 4-2a, if the wires in the winding are fine enough to make eddy currents negligible, then, by the same token, there will be no significant exclusion of magnetic flux from the winding. In Figure 4-2b, on the other hand, the sheet conductors extend over the entire axial dimension of the coil and therefore place no impediment to the full development of eddy currents over the sheet. Meantime, in the radial direction, the conductor dimensions are usually very small, with negligible eddy current development.

Thus, the sheet-wound design permits eddy currents to develop without impediment over the axial dimension of the sheet, and excludes radial components of magnetic field from the coils; at the same time, it prevents the development of eddy currents in the radial direction, and does not impede the axial component of magnetic field passing through the windings.

The current is uniformly distributed over the conducting sheet, except for approximately one skin depth from either edge. It is over these regions at the margins of the sheet that the net effect of eddy currents becomes manifest. Apart from a few unusual cases, these induced eddy currents increase the current density at the conductor edges, i.e., they are in the same direction as the load current.

It is an inherent advantage of sheet windings that they provide a large conductor cross section while presenting only a thin edge to the main (axial) component of reactance flux. In the case of wire windings, comparable conductor cross section requires the use of a multitude of strands which are, in addition, transposed at intervals to control circulating currents.

4.1.3 Computation of Eddy Currents

An important matter for the proposed sheet-wound design is the magnitude of the eddy currents in the windings and their significance for the efficiency and thermal design of the transformer. The mathematical analysis, verified through measurements in a model transformer, constitutes one of the important accomplishments of this work.

The computational techniques developed in the course of this work evolved through three stages, each new stage leading to greater accuracy at some expense in added complexity.

The first stage in computation utilized the model shown in Figure 4-3 and included the fact that the cumulative effect of eddy current generation over the width of the conducting sheet becomes manifest in the edges of the conductor. This manifestation occurs over a marginal band of approximately one skin depth. The model simulates the many turns in the windings by dividing the coil radially (i.e., horizontally in Figure 4-3) into a few vertical elements. These consist of a sheet of current, uniformly distributed, extending over the full axial (vertical) dimension of the sheet, except for the marginal bands. In these bands at the edge of the conductor, the cumulative effect of eddy current is represented by current filaments, for each computational element (e.g., eight elements are shown in Figure 4-3).

A second stage was the treatment of the problem in two dimensions, rather than in a cylindrically symmetric geometry. Finally, a magnetic yoke above the top edge of the coil was not included (a simplifying assumption which increases the computed eddy currents at the conductor edges).

The central core was represented by images in the well-known manner. Without any inaccuracy, a horizontal plane (i.e., a plane perpendicular to the axis of the core) could be established as a plane of symmetry, resulting in some simplification in the computations.

The computation is very simple in principle: It determines "edge currents," I_e , in order to satisfy the condition that along the plane containing these

Figure 4-3. First model used for computation of the eddy currents in sheet windings

currents, the horizontal (radial) component of magnetic induction, H , must be zero. The other contribution to H comes, of course, from the uniform current sheets, and the effect of these can be introduced as the result of a simple integration over the current sheet. The problem is determined by the condition governing I_e , stated above, and the additional condition that, for each computational element, I_e plus the current in the sheet must equal a constant. One then has a set of n linear equations in the n I_e 's ($n = \text{eight in Figure 4-3}$), which are readily solved by the usual techniques using a computer.

This simple model was adequate (as proved later) to demonstrate the main features of the eddy current problem, and yielded values for eddy current losses which were within 10% of the correct values.

The greatest weakness of the simple model of Figure 4-3 lay in its neglect of the fact that radial components of H can develop over the vertical margins of the inner and outer windings to roughly one skin depth. Consequently, it is erroneous to assume that the "current sheets" on the innermost and outermost turns of each winding will be sheets of uniformly distributed current. Neglect of this fact affects all of the I_e values, but the effect is most significant for the innermost and outermost I_e in the winding.

The second development in computational technique largely overcame this difficulty, and removed two other restrictions present in the first model. In the first place, the presence of yokes was included by supplementing the model of Figure 4-3 with additional images (Figure 4-4). In the second place, the analysis took proper account of the phase difference between the main load current in the windings and the edge current. Neglect of this in the first treatment yielded a significant simplification, since properly accounting for the phases (or the real and imaginary parts) of the I_e 's doubles the number of unknowns.

CROSS SECTION OF WINDINGS, CORE, AND YOKE
SHOWING IMAGES, COLUMNS AND SUBDIVISIONS

Figure 4-4. Improved model for the computation of eddy currents

Appendix A presents the second computation in detail.

A third development was to abandon the simplification of the two-dimensional plane geometry in favor of the true cylindrical geometry. In the computational task, this means that the simple summation of the field produced by current in a straight wire, in the Biot-Savart Law, must be replaced by elliptic integrals.

Computation of the problem in the true cylindrical geometry is a refinement which is unnecessary in some cases. For a large coil (mean diameter of a turn = 173 cm, radial build = 30 cm), the loss factor (factor by which eddy currents enhance the winding losses) calculated by the plane and by the cylindrical models compared as follows:

	Loss Factors	
	Inner Winding	Outer Winding
Cylindrical Model	1.243	1.167
Plane two-dimensional Model	1.245	1.164

Note that the error due to the plane approximation will increase as the radial build of the coil becomes larger relative to the mean diameter of the coil.

The cylindrical model and computation of eddy currents therein are described in detail in Appendix A.

4.1.4 Comments on Eddy Current Effects in the Proposed Design

Eddy current generation can be resolved into that which is due to the axial component of the reactance flux and that which is due to the radial component. The former is a familiar problem, much like the eddy currents in a laminated core, for example.

Eddy-currents resulting from radial component flux are a more challenging problem and require solving for the current density and the magnetic field. The technique described above has been developed for doing this. The fields and currents have been calculated for a variety of sheet-wound coils, including model coils tested in this work. Thermal tests on a model coil have produced measurements which verify the analysis (see Section 5.3).

Although in some cases current densities at the edge of the conductor may reach five or six times the nominal value, this does not cause insurmountable difficulties. It must be remembered that such high current densities exist only over a narrow region at the edge of the conducting sheet, and, therefore, the total currents flowing at the edge are very small compared to the total conductor current.

Eddy currents increase winding losses, making them larger than losses incurred with current of uniform density. In a typical case, the increase is roughly 15 to 25%.

4.2 Core Design

4.2.1 Cores for Compressed-Gas-Insulated Power Transformers

Large conventional power transformers are usually contained in tanks of the general shape of a rectangular prism. In some cases, it has proved to be overall more economical to use a more complicated tank shape in order to reduce the total oil-filled volume, and/or to gain structural strength.

For compressed-gas-insulated transformers, the "natural" vessel shape is cylindrical, with hemispherical or domed ends. Cylindrical or spherical shapes lead to the uniform, or approximately uniform, distribution of mechanical stresses and make possible a simple, efficient vessel design.

In fact, typical conventional power transformer core configurations are compatible with a cylindrical vessel. In a single-phase unit, for example, two common arrangements are those of Figures 4-5a and 4-5b. Of these two, the three-legged transformer of Figure 4-5b is better suited to a cylindrical pressure vessel. The experimental transformer built in the course of this work is of the three-legged type of Figure 4-5b.

In the case of three-phase transformers, an acceptable arrangement is that of Figure 4-6. These examples are rather obvious and straightforward accommodations of the conventional core and coil configurations to a cylindrical pressure vessel. Another approach is to explore new opportunities for novel core configurations, compatible with a cylindrical containing vessel.

Figure 4-5. Core designs for single-phase power transformers:
 (a) two-legged design (b) three-legged design

Figure 4-6. A three-phase transformer in a cylindrical vessel

4.2.2 The Cruciform Core

The ideal transformer for a cylindrical container would be a transformer having complete cylindrical symmetry in all its parts. In the single-phase case, this would, in part, resemble Figure 4-5b. The central core and the coil would, of course, be cylindrical, as in that figure. However, a cylindrical symmetry could be extended, in principle, to the yokes, which would then be discs, and to the two end limbs, which would become a cylindrical shell. This cylindrical shell would connect at either end to the disc-shaped yokes.

Such cylindrically symmetric cores have been suggested, and some have been built for special purposes;³⁰ for example, in the cores for Betatrons.³¹ Of course, a transformer which is completely enveloped in its own magnetic return path (the "ultimate" in a shell-form core) creates some difficulties regarding such matters as connecting leads. However, the fundamental difficulty is in adopting such a core concept to the use of modern, grain-oriented, electrical steel.

A compromise between the ideal cylindrical shell and the simple two-legged return path of Figure 4-5b is offered by a five-legged, or "cruciform," core. At first consideration, such a core would seem to pose some fabrication problems. However, by utilizing four, identical, two-legged cores arranged symmetrically, the construction and assembly can be considerably simplified. A core of this type has been designed and constructed as part of the present work. The arc of the core was designed to fully utilize the space available in the present pressure vessel (Figure 2-2).

A plan drawing of the cruciform core constructed in this program is shown in Figure 4-7. Figure 4-8 shows the core with and without the yoke in place. The core has principal dimensions as follows:

Cross section of central core	1863 cm ²
O.D. of central core	58 cm
Maximum (clearance) diameter within core window	113 cm
Window height	138 cm
Window width	30 cm

The total weight of the core is 7.27 metric tons.

Basically, the transforming capability of a core is measured by the product of the core area and the window area. On this basis, the cruciform core of Figure 4-7 has approximately 2.6 times the capability of the three-legged core of Figure 2-2, as shown in the following:

	Three-Legged Core	Cruciform Core
Window Height (cm)	100	139
Window Width (cm)	27.4	38
Core Cross Section (cm ²)	1430	1896
Product of Core and Window Areas (cm ⁴)	3.9x10 ⁶	1x10 ⁷

Figure 4-7. Plan drawing of a cruciform core for the experimental transformer

For a single-phase application (Figure 4-5b), the advantages of a cruciform core over the conventional three-legged core appear to be the following:

- A reduction in the fraction of the reactance flux which finds its way into undesirable places such as the core frame or the tank. The flux entering the central core does so in a direction parallel to, or at an oblique angle to, the laminations, with a consequent reduction in core heating.

Figure 4-8. Cruciform core constructed according to the plan drawing of Figure 4-7;
 (a) without the yoke in place
 (b) with the yoke in place

Figure 4-9. Conceptual picture of a novel core configuration for a three-phase transformer

- Compared to the conventional three-legged design of Figure 4-5b, core weight decreases by an amount which varies from 10 to 25%, depending on construction details.
- A slightly smaller tank (10 to 15% reduction in diameter) is possible. Alternatively, for a given size pressure vessel, the cruciform core offers the option of shipping a larger MVA unit than a three-legged core.
- By using a relatively small number of different lamination widths, a core with a large number of "steps", and therefore a high core cylinder space factor, can be achieved. For example, the core shown in Figure 4-8 requires only four different lamination widths.

For a three-phase transformer, novel accommodations to a cylindrical vessel are also possible. One example is shown in Figure 4-9. While the sketch shows three return paths (end limbs), there could just as well be four such limbs. This configuration would be an alternative to the more conventional configuration where the core is positioned vertically (Figure 4-6).

Section 5
THERMAL DESIGN

Section 5

THERMAL DESIGN

5.1 General Description of the Proposed Cooling System

The proposed system for cooling the windings calls for thin, annular cooling ducts wound into the coil at intervals (Figure 2-1). Therefore, heat must be conducted radially through a number of polymer film and conducting sheet layers in order to reach the walls of the cooling duct. Between adjacent layers of polymer film and between polymer film and conducting sheet are very thin layers of included gas, and the heat must be conducted across these as well.

The cooling ducts are made of metal and constitute sealed units, each connected to an inlet and outlet coolant pipe. The duct is a thin annulus typically 2.5 mm in radial thickness. The duct edges protruding from the coil structure are enlarged into headers, circular in cross section and 20 mm in diameter. Figure 3-18 shows the headers and coolant pipes in an experimental transformer built in the course of this work.

The coolant pipes must be capable of insulating the high voltages from coil to ground (see Section 3.3). The insulating coolant pipes connect with grounded manifolds which are mounted on the upper and lower core yokes. Piping connects these manifolds through a pressure flange to the pump and heat exchanger, which are external to the pressure vessel. The system, as employed in the experimental transformer, was capable of operating at pressure drops of four or five atmospheres and with fluid velocities, through the ducts, up to 100 cm/s.

Important considerations in arriving at this cooling system in preference to the various alternatives have been the advantages of a very high performance system which occupies the smallest possible space within the coil. The sealed system, completely separate from the main insulating fluid, which is SF₆ gas, makes this possible. Effective heat removal can be accomplished in a thin duct by using comparatively high fluid velocity in fully developed turbulent flow.

In the proposed system, the cooling fluid must be an electrical insulator; however, the electric stress level required of the coolant pipes and the liquid they contain is much lower than required in the major gas insulated gaps, and within the windings. The cooling fluid may be chosen for its desirable thermal properties, with less emphasis on insulating properties than in conventional transformers. The fluid may also be free of the requirement that it be compatible with the entire transformer structure.

There are two principal alternatives to the proposed cooling system, and some comment should be made regarding them.

- Convective cooling by the compressed gas: This is an entirely feasible scheme and successful power transformers have been built which are cooled in this way.^{32,33} The disadvantages of this form of cooling arise from the poor thermal conductance and very low volumetric heat capacity of compressed gas. Consequently, cooling ducts must be large, and very large gas volumes must be circulated to accomplish cooling.

- Vaporization cooling: The advantages of this scheme lie in the high heat transfer coefficients - especially in heat rejection - and in the fact that little or no power is required for coolant circulation. However, the very narrow ducts extending over large areas are a disadvantage to vaporization cooling. Furthermore, it should be borne in mind that the heat transfer coefficients for heat absorption are not substantially higher in vaporization than they are in liquid cooling without vaporization, i.e., on the order of 10 W/cm² °C. It is at the heat rejection stage of the cycle that vaporization cooling provides substantially higher heat transfer coefficients than does liquid cooling. In other words, vaporization cooling makes possible a smaller external heat exchanger, but, at least at present, it seems unable to provide the compactness of simple liquid cooling.

These considerations led to the decision to adopt the self-contained, forced-liquid cooling system described above.

5.1.1 Core Cooling

The core of the experimental transformer is cooled by the compressed gas, circulating under natural convection and transferring heat to the walls of the pressure vessel. The core contains no cooling ducts. Heat generated within the core is conducted to the outside edges of the laminations and there transferred to the gas. The cruciform core is a particularly advantageous design for cooling in this simple way, which would be applicable to transformers of capacity up to 50 MVA to 100 MVA per phase. Fan assisted convection in the space about the central core would probably be required above 50 MVA per phase. In addition, for capacities above 20 MVA to 30 MVA per phase, direct heat transfer from the gas to the walls of the vessel would have to be augmented by an internal heat exchanger exposed to the gas, within the pressure vessel.

For cores of such size that cooling within the volume of the core is required, thin, sealed ducts with circulating liquid should be used. To use gas for internal cooling in the core would require ducts several cm wide, while forced-liquid cooling can be accomplished in ducts only 1 mm wide.

5.2 Temperature Distribution Within the Winding

As a consequence of the analysis of current distribution within a sheet winding (summarized in Section 4.1.2), it is possible to compute the rate of heat generation, $\dot{q}$, at any point within the winding. Given the relevant thermal conductances and the heat transfer coefficients applicable to the cooling ducts, a complete thermal analysis of the steady-state operation of a given coil is possible.

The temperature field in steady-state heat flow is governed by the well known expression

$$\nabla^2 (kVT) = -\dot{q}, \quad (5-1)$$

where

k = thermal conductivity

T = temperature at a given point

$\dot{q}$ = heat generation at the same point.

Figure 5-1 shows, schematically, a portion of the winding adjacent to a cooling duct. B is the distance between two successive ducts, and h is the axial (z) dimension of the conducting sheet.

It is evident that heat must flow in the radial direction in order to reach a cooling duct. The thermal conductance in the radial direction is governed by the turn-to-turn layers of polymer film and the thin layer of included gas. The conductance of this composite of layers was first measured in a small sample simulating the laminated turn-to-turn gap. A value of .12 W/(m·°C) was obtained. This value was verified closely in experiments (described below) with a model transformer.

The thermal conductance in the z-direction, on the other hand, is dominated by the aluminum conductor and is about 1000 times greater than the conductance in the radial direction. In view of the fact that k is not isotropic, Equation 5-1 becomes, in this case

Figure 5-1. Diagram to accompany the thermal analysis

$$k_r \frac{\delta^2 T}{\delta r^2} + k_z \frac{\delta^2 T}{\delta z^2} = -\dot{q} \quad (5-2)$$

k_r and k_z are, respectively, the specific radial and axial conductances applicable to the bulk winding in the (r,z) plane in Figure 5-1. k_r and k_z are simply related to the material conductances through the winding space factor, S.

Let k_i be the thermal conductance of the turn-to-turn insulation in the radial direction (viz., .12 W/(m·°C), as above), and let k_c be the conductance of the metal sheet (viz., 234 W/(m·°C), for aluminum). Then,

$$k_r = \frac{k_i}{1-S}$$

and

$$k_z = k_c S.$$

The first thermal analysis performed in this work was a simple one. It was based on the model of Figure 5-1, and made certain simplifying assumptions, e.g., replacing the cylindrical winding configuration with the two-dimensional, plane model of Figure 5-1. This analysis describes the essential physical features of the thermal problems and leads to an approximate expression for the temperature field.

In this simplified analysis, the heating due to eddy currents which flow in a thickness, δ , at the top edge of the coil, is made manifest by a heat flux, $F(n)$, entering the coil normal to the plane $z = h/2$ (Figure 5-1). This establishes a boundary condition at this surface,

$$\frac{\delta T}{\delta z} = \frac{1}{k_z} F(n); \text{ at } z = \frac{h}{2}.$$

The duct surface at the plane $r = 0$ is assumed to be at a constant temperature; say $T = 0$. This, together with the assumed symmetry of the problem, leads to the additional boundary conditions,

$$T = 0 \text{ at } r = 0$$

$$\frac{\delta T}{\delta r} = 0 \text{ at } r = B/2$$

$$\frac{\delta T}{\delta z} = 0 \text{ at } r = z = 0$$

Subject to these boundary conditions, Equation 5-2 has a series solution in terms of elementary functions, which is

$$T(r, z) = \frac{\dot{q}}{2k_r} (Br - r^2) + \sum_{n=0} A_n \sin \frac{(2n+1)\pi r}{B} \cosh \alpha \frac{(2n+1)\pi r}{B} z \quad (5-3)$$

where

$$A_n = \frac{4}{\sqrt{k_r k_z} (2n+1) \pi \sinh \frac{\alpha (2n+1) \pi h}{2B}} \int_0^{B/2} F(r) \sin \frac{(2n+1)\pi r}{B} dr \quad (5-4a)$$

and

$$\alpha = \sqrt{\frac{k_r}{k_z}}$$

An approximate polynomial expression for the hottest spot (which will occur at $r = B/2$ and $z = h/2$) can be obtained from Equation 5-3 by assuming that between two cooling ducts, $F(n)$ can be considered constant, say $F(n) = F_0$. Then Equation 5-4a for A_n may be simplified to

$$A_n = \frac{4F_0 B}{\sqrt{k_r k_z} (2n+1)^2 \pi^2 \sinh \frac{\alpha (2n+1) \pi h}{2B}} \quad (5-4b)$$

For any reasonable values of h and B , the A_n 's in Equation 5-4b decrease so rapidly with increasing n that, for the present purposes, only the first term ($n = 0$) in Equation 5-3 need be retained. Then, setting $r = B/2$ and $z = h/2$ in Equation 5-3,

$$T_{\max} = \frac{\dot{q}}{8k_r} B^2 + \frac{4F_0 B}{\sqrt{k_r k_z} \pi^2 \tanh \frac{\alpha \pi h}{2B}} \quad (5-5)$$

The first term in Equation 5-5, as well as the first term in Equation 5-3, is the contribution to the temperature from the component of the winding current which is uniformly distributed over the width of the sheet. The second term in Equation 5-5, as well as the summation in Equation 5-3, represents the contribution to the temperature from eddy currents.

The second term in Equation 5-5 is usually about 10 to 20% of $T_{\max}$, which is verified in a general way by the more precise, second analysis and by the experimental measurements. This is an important result: Although current densities at the very edge of the conductor can, in some cases, be 5 or 6 times those in the bulk of the winding - with $\dot{q}$ at the edge 25 or 36 times the $\dot{q}$ in the bulk of the winding - this does not result in a large temperature difference in the z direction. The physical explanation for this is evident, namely, that k_z is so much larger than k_r that the conducting sheet is approximately an isothermal surface (as viewed in a cross section plane through the axis of the coil).

Two principal weaknesses of the above simplified analysis are the following:

- It represents the effect of eddy currents as heat injected into the edge of the winding, rather than heat generated in a narrow band, of width δ , at the edge of the winding.
- It assumes that the cooling duct is a constant temperature surface at the boundary of a perfect heat sink. In reality, the duct and its internally flowing liquid have a finite heat transfer coefficient. Also, allowance should be made for the sensible rise in coolant temperature as it traverses the duct.

Including these two factors greatly complicates the simple boundary-value problem (Equation 5-2 and following). The second, improved analysis therefore turned to a different method, namely, solving Equation 5-2 as a different equation over a grid of points covering the region between ducts. The details of this procedure are described in Appendix B.

5.3 Experiments with a Model Transformer

The first model constructed with forced cooling was intended to test the general hydraulic and thermal performance of the cooling duct concept, and to obtain a practical value for the effective radial thermal conductance which could be achieved in an actual winding.

The coil built for these experiments is shown in a schematic cross section in Figure 5-2. The primary winding was designed to have conducting sheet of generous cross section in order that heat generation in the primary winding would be negligible. Virtually all heat generation occurred in the secondary winding, i.e., in the region between the two ducts in Figure 5-2. The coil design was suitable only for thermal, however, and not for operation at high voltage.

Some principal dimensions of this coil are as follows:

Primary winding (350 turns)

Conductor	Aluminum sheet, 20.3 cm x 0.32 mm
Turn insulation	Polymer film, 0.051 mm thickness

Secondary winding (130 turns)

Conductor	Aluminum sheet, 20.3 cm x 0.076 mm
Turn insulation	Polymer film, 0.025 mm thickness

Figure 5-2. Schematic drawing of the first, force-cooled coil

Cooling ducts

Width of annular liquid channel 0.76 mm

Thickness of copper wall 0.8 mm

I.D. of toroidal manifolds at top and bottom of each duct 16 mm

Water taken directly from the water mains was used as the coolant fluid. The ducts were fabricated of copper and, for convenience of manufacture and assembly, were made in quadrants, that is, four more or less identical pieces were required to make up a single, complete, annular cylindrical duct. Each of the four pieces is a closed, separate unit and, therefore, is supplied with an inlet and an outlet pipe.

Figure 5-3 shows the coil under construction. In the photograph, the primary winding has been completed and the set of four pieces making one duct has been laid in place over the primary. Over this duct was wound the secondary; finally, the second duct was placed around the outer surface of the secondary.

Installed in the transformer and with pipework connected, the coil appeared as shown in Figure 5-4.

Applying the methods of eddy current analysis described in Section 4.1.2 to this thermal test coil yielded a maximum current density at the edge of the conductor of 1.6 times the density of the uniformly distributed current in the bulk of the winding. Thus, $\dot{q}$ at the edge of the conductor had a maximum value about 2.6 times the value of $\dot{q}$ in the bulk of the winding. In applying this to the computation of temperature distribution (Section 5.2), no measurable temperature difference in the axial direction would be expected in this coil.

Figure 5-3. The first force-cooled coil under construction. The quadrants making up the innermost cooling duct have been laid in place.

Figure 5-4. The experimental transformer with the first force-cooled coil

In summary, the model coil (Figure 5-4) was designed to be a simulation of the situation between two ducts in as simple a form as possible.

A total of 40 thermocouples were wound into the coil in a variety of positions in order to explore temperature gradients axially, radially, and circumferentially. Thermal profiles (Figure 5-5) were obtained for winding currents up to 90 A (580 A/cm²). During all tests, the coolant flux was sufficient (approx. 5 l/s) to limit the temperature rise of the cooling water to less than 2 °C.

Figure 5-5. Temperature distribution between cooling ducts in the transformer of Figure 5-4

The cooling ducts were made of thin (0.8 mm) copper sheet. Thus, by increasing the pressure within the cooling system, the radial compression of the winding could be varied. (The change in pressure was accomplished by throttling an exhaust valve on the system.) As might be expected, increasing radial compression increases the effective thermal conductivity radially through the winding (Figure 5-6).

Figure 5-6. The effective thermal conductivity of layers of polyester film as a function of the compression applied to the layers

The second coil on which temperature measurements were made was a more realistic model of a sheet-wound, compressed-gas-insulated transformer. The general dimensions were such that very marked eddy current effect would be present. The coil was capable of operating at high voltage (100 kV, rms) and had two complete, cylindrical annular cooling ducts. Cooling was accomplished with a fluorocarbon liquid (R-113) under forced circulation; insulating pipes of the type discussed in Section 3.3 connected the cooling ducts to a grounded manifold (Figure 3-18). The coil was wound with high tension and was self-supporting (see Section 6.2).

The coil was constructed of aluminum sheets, 61 cm wide. The inner (primary) winding had 235 turns of sheet conductor 0.076 mm thick, and the outer (secondary) winding 1870 turns 0.025 mm thick. The turn-to-turn insulation consisted of two sheets of polymer film, each 0.025 mm thick; this film was 62.23 cm wide, thus extending beyond the edge of the conductor by 0.615 cm at each end of the coil.

The cooling ducts were made of 304 stainless steel. Copper had been used for the cooling ducts in the first, force-cooled coil because it was believed to result in easier fabrication. For power transformer application, however, copper has potential disadvantages: The high electrical conductance of copper could lead to substantial eddy current generation in the duct walls and especially in the headers at the ends. Whether or not this would really be a difficulty depends upon the details of the particular design. A further potential problem with copper is its possible chemical interaction with the coolant liquid over long periods of time.

The current distribution over the conducting sheet was also analyzed for this second model coil by the methods of Section 4.1.2. The machine computation took account of the presence of the core yokes as well as the central core. The computation divided each winding into three columns vertically, and then into six segments axially, with the subdivisions becoming progressively finer near the edge where the current density begins to change rapidly. Figure 4-4 illustrates this computational scheme. The calculated current density distribution for the coil is shown in Figure 5-7.

Figure 5-7. Computed current distribution in the second force-cooled coil

Temperature distribution was also calculated for this coil using the methods discussed in Section 5.2. The presence in this coil of a significant eddy-current generation and detectable axial temperature gradients provided an opportunity to compare directly the experimental measurements with the calculated values.

In a coil wound under high tension in order to be self-supporting, and suitable for testing at high voltage, the introduction of thermocouples must be

handled with care. In a technique similar to that employed for temperature measurement in the first force-cooled coil, thermocouples with flattened leads and junctions were made: Figure 5-8 shows the 0.127 mm iron-constantan thermocouples before and after the junction and leads were flattened.

Unlike the earlier case, these thermocouples were not wound into the coil at various depths in the winding, but were employed only for measuring the temperature at the edge of the winding conductor. The plan was to insert these thermocouples into the ends of the completely assembled coil. The thin, flattened thermocouples reached between the overhanging film insulation just far enough to touch the edge of the conductor. Because of the delicacy of the thermocouples and the fineness of the conductor thickness, the insertion of thermocouples and the fineness of the conductor thickness, the insertion of thermocouples required great care.

A structure designed to bridge the high voltage winding in the radial direction was designed and built (Figure 5-9). These bridges, which held as many as six thermocouples, could be located at any position around the winding, as shown in Figure 5-10. In Figure 5-10, foam insulation can be seen laid over the end of the winding. This created an approximation to a thermally-insulating boundary at the end of the conductor, which made the experimental arrangement agree with the assumption made in the thermal analysis.

Figure 5-8. Thermocouples with flattened junctions and leads

Figure 5-9. Supporting bridge for thermocouples

Since six thermocouples can be inserted radially between ducts in the high voltage winding, the eddy current and thermal analysis were computed for the same six radial positions. The experimentally measured temperatures and the calculated temperatures are compared in Figure 5-11, in which the solid lines show the calculated values and the circles show the measurements. Except for the graphs at the two lowest currents, points are shown for various circumferential positions: As in the earlier work, there is no significant dependence of temperature on circumferential position. The discrepancies between experimental and theoretical temperature rise is in no case greater than 8%. This is within the expected error range for temperature measurement.

A further check on experimental procedures and measurements was provided by passing direct current through the windings and again measuring temperatures as in the alternating current case. The results are shown in Figure 5-12.

Figure 5-10. Supporting structure, with thermocouples, installed in a force-cooled coil

Figure 5-13 summarizes these results in a single graph by plotting the temperature rise at the hottest spot (approximately halfway between cooling ducts) as a function of winding current.

In Section 5.2 it was remarked that thermal conductance is so very much larger in the axial direction than in the radial direction that the conducting sheets are approximately isothermal surfaces. It must also be noted that the conducting sheets are approximately isothermal surfaces circumferentially around a turn of the winding. A rather striking demonstration of this was made by closing off the coolant flow to one of the four quadrants making up the outermost cylindrical duct. At first sight, one might expect that the winding conductor adjacent to the closed duct would consequently experience significantly higher temperatures. In fact, closing this one quadrant has so small an effect that it is scarcely detectable, as seen in the graph of Figure 5-14.

5.4 Fluid-Dynamic Aspects of the Cooling System

In terms of fluid dynamics, the cooling ducts are the only unusual elements in the cooling loop. As described above, these ducts are thin cylindrical annuli ending in circular "headers," or manifolds, where the duct emerges at the ends of the coil. Piping connects to each of these headers at a single point, and liquid flows along the header and thence through the duct.

Consider the inlet header shown in Figure 5-15. As fluid moves from point A to point B, it loses some pressure because of wall friction, but it also gains some pressure (momentum recovery) because of a flow deceleration resulting from the loss of fluid toward point C. Also, a frictional pressure drop, described by the Fanning friction factor, exists between points C and D. The following equations describe these components:

$$P_A - P_B = \frac{1.25\rho}{2} (1.36U_B^2 - 0.64U_A^2 - 0.72U_A U_B) \quad (5-6)$$

(a)

(b)

(c)

(d)

(e)

(f)

Figure 5-11. Experimental and calculated temperatures in the force-cooled coil shown in Figure 2-1

(a)

(b)

(c)

(d)

(e)

(f)

Figure 5-12. Experimental and calculated temperatures with dc in the winding

Figure 5-13. Temperature of hottest spot in the winding as a function of winding current

Figure 5-14. Temperature in winding adjacent to cooling duct quadrant which was closed to coolant flow

$$p_A - p_C = \frac{1.25\rho}{2} (1.8U_C^2 - 0.368U_A U_C) \quad (5-7)$$

$$p_C - p_D = 4f_n \frac{L_n}{D_n} \frac{\rho U_C^2}{2} \quad (5-8)$$

The nomenclature used in these and subsequent equations is shown in Table 5-1. U_A , U_B , and U_C are the coolant velocities at the positions A, B, and C, respectively; f is the friction factor for the duct; L_n is the duct length;

Figure 5-15. Schematic diagram of flow through the inlet header of a cooling duct

Table 5-1
NOMENCLATURE

a	Duct passage thickness (gap)
b	Duct passage width
C_p	Coolant specific heat
De	Equivalent (hydraulic) diameter of duct
Δp	Pressure drop
ΔT	Temperature change
η	Efficiency
f	Friction factor (Equation 5-19)
h	Heat transfer coefficient
k	Thermal conductivity of coolant
L	Duct length
μ	Coolant viscosity
Nu	Nusselt Number, hDe/k
p	Pressure
P	Electrical or thermal power
Pr	Coolant Prandtl Number, $C_p \mu / k$
q_{th}	Rate of heat transfer
Re	Duct Reynolds Number, $\rho U De / \mu$
ρ	Coolant density
U	Average coolant velocity

and D_h is the hydraulic diameter of the duct. The factor of 1.25 is included to provide margin for some inlet flow maldistribution.

A collection header is shown on Figure 5-16, to which the following equations apply:

$$P_E - P_F = \frac{1.25\rho}{2} \left[2U_F^2 - 0.05U_E^2 - 2U_F \left(0.205U_D \frac{W_D}{W_F} + U_E \frac{W_E}{W_F} \right) \right] \quad (5-9)$$

$$P_D - P_F = \frac{1.25\rho}{2} \left[2U_F^2 - 0.4 U_D^2 - 2U_F \left(0.205U_D \frac{W_D}{W_F} + U_E \frac{W_E}{W_F} \right) \right] \quad (5-10)$$

Note that the ratio of mass flow rates enters this equation along with the velocities.

Figure 5-16. Schematic diagram of flow through the outlet header of a cooling duct

Figure 5-17 portrays two complete inlet and outlet header systems. The approximate analysis followed here will assume (1) uniform flow distribution through n equal parallel paths of area A_n , (2) maximum maldistribution of flow between the 1st and the n th parallel paths, and (3) uniform headers of area A_m . Under these assumptions, the variation in pressure drop between the 1st and n th passes is calculated. The flow maldistribution is estimated to vary as the square root of this difference.

Figure 5-17. Two possible arrangements for the flow of liquid within the coolant ducts

Consider the 1st path. Following the notation of Figures 5-16 and 5-17 and defining U_O as the velocity in the header when 100% of the mass is flowing, then:

U-Bend

$$U_A = U_O$$

$$U_B = \left(\frac{1-n}{n}\right) U_O$$

$$U_C = \frac{A_m}{nA_n} U_O$$

$$U_E = U_B = \frac{(n-1)}{n} U_O$$

$$U_F = U_A = U_O$$

$$\frac{W_D}{W_F} = \frac{1}{n}$$

$$\frac{W_E}{W_F} = \frac{n-1}{n}$$

Z-Shaped

$$U_A = U_O$$

$$U_B = \left(\frac{1-n}{n}\right) U_O$$

$$U_C = \frac{A_m}{nA_n} U_O$$

$$U_E = 0$$

$$U_F = \frac{1}{n} U_O$$

$$\frac{W_D}{W_F} = 1$$

$$\frac{W_E}{W_F} = 0$$

The pressure drop from point 1 to point 4 (Figure 5-17) is:

U-Bend

$$p_1 - p_4 = \frac{1.25\rho U_O^2}{2} \left[2.0 + 1.45\xi^2 - 0.368\xi - 0.410\frac{\xi}{n} - 2\left(\frac{n-1}{n}\right)^2 \right] + 4f_n \frac{L_n}{D_n} \frac{\rho}{2} U_O^2 \xi^2 \quad (5-11)$$

Z-Shaped

$$p_1 - p_4 = \frac{1.25\rho U_O^2}{2} \left[1.45\xi^2 + \frac{2}{n} - 0.368\xi - 0.41\frac{\xi}{n} \right] + 4f_n \frac{L_n}{D_n} \frac{\rho}{2} U_O^2 \xi^2 \quad (5-12)$$

where $\xi = A_m/nA_n$ (area and velocity ratio).

Now consider the pressure drop from point 2 to point 3 (nth path):

U-Bend

$$U_A = \frac{1}{n} U_O$$

$$U_B = 0$$

Z-Shaped

$$U_A = \frac{1}{n} U_O$$

$$U_B = 0$$

$$U_C = \frac{A_m}{nA_n} U_O$$

$$U_C = \frac{A_m}{nA_n} U_O$$

$$U_E = 0$$

$$U_E = \frac{n-1}{n} U_O$$

$$U_F = \frac{1}{n} U_O$$

$$U_F = U_O$$

$$\frac{W_D}{W_F} = 1$$

$$\frac{W_D}{W_F} = \frac{1}{n}$$

$$\frac{W_E}{W_F} = 0$$

$$\frac{W_E}{W_F} = \frac{n-1}{n}$$

U-Bend

$$P_2 - P_3 = \frac{1.25\rho}{2} U_O^2 \left[1.4\xi^2 - 0.778 \frac{\xi}{n} + \frac{2}{n^2} \right] + 4f_n \frac{L}{D_n} \frac{\rho U_O^2}{2} \xi^2 \quad (5-13)$$

Z-Shaped

$$P_2 - P_3 = \frac{1.25\rho}{2} U_O^2 \left[2 + 1.4\xi^2 - 0.778 \frac{\xi}{n} - 2 \left(\frac{n-1}{n} \right)^2 \right] + 4f_n \frac{L}{D_n} \frac{\rho U_O^2}{2} \xi^2 \quad (5-14)$$

The header-to-header pressure drop will now be taken as the average of the 1st path and the nth path:

$$\Delta p_{HH} = \frac{(p_1 - p_4) + (p_2 - p_3)}{2} \quad (5-15)$$

Now consider the header axial pressure drop, assuming again that the flow withdrawal is uniform. After noting that the momentum pressure recovery is reasonably independent of the number of manifold branches,²⁹ the following inlet manifold equation can be written:

$$\Delta p_{im} = \frac{U_O^2}{2} \left[1.2 - \frac{4f_O L_m}{3D_m} \right] \approx (p_2 - p_1) \quad (5-16)$$

where the friction factor is calculated based on the inlet (maximum) header velocity U_O . Similarly, in the outlet manifold:

$$\Delta p_{om} = \frac{\rho U_O^2}{2} \left[1.8 + \frac{4f_O L_m}{3D_m} \right] \approx \begin{cases} (p_3 - p_4) & \text{U-Bend} \\ (p_4 - p_3) & \text{Z-Shaped} \end{cases} \quad (5-17)$$

Now the estimated flow ratio between path 1 and path n is:

$$\frac{W_i}{W_n} \approx \frac{\Delta p_{HH}}{\Delta p_{HH} + \Delta p_{im} - \Delta p_{om}} \quad \text{U-Bend} \quad (5-18a)$$

$$\frac{W_i}{W_n} = \frac{\Delta p_{HH} - \Delta p_{om}}{\Delta p_{HH} + \Delta p_{im}} \quad \text{Z-Shaped} \quad (5-18b)$$

Note that even under the previous assumptions, this is not a unique definition of this ratio. Nevertheless, it will serve for the present comparisons.

General guidelines based on various analyses of manifold configurations suggest the following:^{34,35}

1. Design for

$$\xi = \frac{nA_n}{A_m} \leq 1.0$$

2. Design for

$$\frac{L_m}{D_m} \leq 70.0$$

3. Under the same assumptions as those given in the previous section, the U-bend arrangement provides the best distribution when

$$\frac{f_o L_m}{D_m} < 0.9 \quad (\text{Momentum dominated in the header})$$

A Z-shaped arrangement provides the best distribution when

$$\frac{f_o L_m}{D_m} < 1.35 \quad (\text{friction dominated in the header})$$

Note that while this factor shows whether the manifold flow is friction or momentum dominated, the magnitude of the branch-to-branch maldistribution is a function of the amount of flow resistance in series with the manifold flow resistance.

By using the analysis described by Equations 5-15 to 5-17, a number of cases can be considered. Assume the following conditions:

- L_m = header length = 1.667 m
- Flow per header = 118 gpm of R-113
- Circular cross-section header
- 38 parallel rectangular flow passages of dimension 3.81 cm by 2 mm with a length of 3 m

The friction factors f_n and f_o can be calculated using the turbulent flow equations. Results of computations for a variety of header diameters are shown on Figure 5-18 for both a U-bend and Z-shaped configuration. Note that the header is momentum dominated and that the U-bend configuration provides a lower maldistribution. The series pressure drop (Δp_{HH}) through the parallel ducts tends to reduce the effects of the header pressure drop maldistribution. This can be shown by reducing the parallel path flow length by a factor of 2. Data for the

Figure 5-18. Example calculation for U-bend and Z-bend configurations

U-bend configuration are shown on Figure 5-18. Design diameters producing a 10 or 25% maldistribution are given in Table 5-2.

Table 5-2

HEADER DIAMETER REQUIRED FOR A MAXIMUM OF 10% AND 25% FLOW MALDISTRIBUTION

Duct Length (m)	Configuration	Maximum 10% Maldistribution		Maximum 25% Maldistribution	
		Required Diameter		Required Diameter	
		cm	(in.)	cm	(in.)
3.0	U	4.27	(1.68)	3.56	(1.40)
3.0	Z	6.52	(2.57)	5.08	(2.00)
1.5	U	5.00	(1.97)	4.22	(1.66)

A final choice of header diameter requires an evaluation of the permissible maldistribution. This can be accomplished by considering the overall thermal analysis of the system; what would be the effect on hot spot temperature rise due to a variation in the heat transfer coefficient and the temperature rise of the coolant. The sizes indicated here suggest that a reasonable amount of design flexibility is available.

In discussing the overall thermal and hydraulic performance of the cooling duct, four principal design quantities must be considered:

- The axial pressure drop per unit flow length
- The kinetic energy of the flow expressed as a velocity head ($\rho U^2/2$)
- The duct wall heat transfer coefficient
- The sensible temperature rise of the coolant fluid along the duct length.

The axial pressure drop can be expressed as follows:

$$\Delta p_{\text{axial}} = 4f \frac{L}{D_e} \left[\frac{\rho U^2}{2} \right] \quad (5-19)$$

where L is the duct length, f is the friction factor, and D_e is the equivalent diameter (defined in Table 5-2). The correlation in Table 5-3 is an estimate of the "smooth pipe" friction factor in the range of turbulent Reynolds Number to 1×10^5 .

Table 5-3

TURBULENT DUCT FLOW EQUATIONS

Equivalent Hydraulic Diameter:

$$D_e = 4 \frac{(\text{Flow Area})}{(\text{Perimeter})}$$

$$= \frac{2ab}{a+b}$$

Reynolds Number:

$$Re = \frac{\rho U D_e}{\mu}$$

Friction Factor (Smooth Duct):

$$f = 0.079 / Re^{0.25}$$

$$(2100 < Re < 1 \times 10^5)$$

Nusselt Number:

$$Nu = \frac{h D_e}{k} = 0.023 Re^{0.8} Pr^{0.33}$$

$$(Re < 2100)$$

The expansion and contraction pressure losses and the pressure drop associated with turns in the flow can generally be written as

$$\Delta p = C_D \left[\frac{\rho U^2}{2} \right] \quad (5-20)$$

where C_D may be a weak function of Reynolds Number. In a normal system the sum of these pressure drops will be several velocity heads ($\rho U^2/2$). Note also that the axial pressure drop is proportional to the velocity head.

The average heat transfer coefficient along the duct with turbulent flow can be estimated by a Dittus-Boelter type of equation, as given in Table 5-3.

Entrance effects can be neglected, because the length to equivalent diameter ratio of the proposed ducts is so large.

The coolant temperature rise (at 40 °C) can be estimated as a function of the flow rate in gallons per minute (GPM) in the following expression:

$$\Delta T_s = 10.63 \frac{q_{th}}{\text{GPM}} \quad (5-21)$$

where ΔT_s is the temperature use in °C and q_{th} is the heat load in kW.

Figures 5-19 through 5-21 show the values of $\Delta p_{axial}/L$, $\rho U^2/2$, and heat transfer coefficient, h , as a function of coolant flow rate and duct thickness (gap) for a typical duct width of 3.81 cm (1.5 in.). For the calculation of flow per unit of duct perimeter, it is assumed that spacers account for 14% of the perimeter (86% open to flow). These values correspond to those obtained on the second force-cooled coil.

Figure 5-19. Effect of the width of the cooling duct on the axial pressure drop

The curves in these figures can be used to give a preliminary estimate of the duct pressure drop and heat transfer. (An example is given below.)

5.5 External Flow Parameters

The factors influencing the total flow and allowable pressure drop trade-offs are the pumping power requirements, both mechanical and electrical. The pump motor requirements are as follows.

$$P_{\text{pump}} = \text{Pump power (HP)} = \frac{(\Delta p)_{\text{psi}} (\text{GPM}) (0.00054)}{\eta_{\text{pump}}} \quad (5-22)$$

Figure 5-20. Kinetic energy pressure in a cooling duct

Figure 5-21. Effect of width of cooling duct on average heat transfer coefficient

where Δp is the pressure drop (in psi) and η_{pump} is the pump efficiency. For preliminary sizing purposes, η_{pump} may be taken as 0.5.

The electrical power requirements can be calculated as follows:

$$P_{\text{electric}} (\text{kW}) = \frac{P_{\text{pump}}}{\eta_{\text{electric}}} \times 0.746 \quad (5-23)$$

A typical value of η_{electric} is 0.9.

5.6 Sample Calculation

Calculated values of the system parameters based on Equations 5-19 through 5-23 and Figures 5-19 through 5-21 are shown in Table 5-4. The nomenclature employed is shown in Table 5-1.

Table 5-4

SAMPLE CALCULATION OF COOLING SYSTEM PARAMETERS

Inputs:

- a) $q_{\text{th}} = 1000 \text{ kW}$
- b) Duct perimeter (6 ducts) = 30 m
- c) Duct length = 3 m
- d) Duct gap = 2 mm

Assumptions:

- a) Allowable coolant temperature rise = 5 °C
- b) Four velocity heads of pressure drop for inlet or outlet
- c) $\eta_{\text{pump}} = 0.5$ and $\eta_{\text{electric}} = 0.9$

Calculated values:

- a) Total flow = 2126 gpm (Equation 5-21)
 - b) Flow/perimeter = 70.87 gpm/m
 - c) Duct velocity = 2.8 m/s (Figure 5-9)
 - d) Axial pressure gradient = $3.4 \times 10^4 \text{ Pa/m}$ (1.5 psi/ft)
 - e) Velocity head = $0.51 \times 10^4 \text{ Pa}$ (0.74 psi)
 - f) Heat transfer coefficient = $3000 \text{ W/m}^2 \text{ } ^\circ\text{F}$
 - g) Duct pressure drop = $12.2 \times 10^4 \text{ Pa}$ (17.7 psi)
 - h) Pump horsepower (duct Δp only) = 40.6 hp
 - i) Electrical input = 33.7 kW
-

Section 6
MECHANICAL DESIGN

Section 6

MECHANICAL DESIGN

6.1 General Remarks on Mechanical Forces and Design

The large size of a modern power transformer gives rise to certain mechanical problems, for example, those associated with inertial forces in shipment and those which are inherent in the transformer's function. An innovation or a different way of doing things could drastically alter or even eliminate the problem of inertial forces in shipment, but the inherent electromechanical forces would still remain. Therefore, this discussion will focus on the problems inherent in function.

The "transformation" of electric power from one winding to another magnetically interacting, or "coupled," winding in a power transformer is accompanied by mechanical stresses. One way of visualizing this essential connection between mechanical forces and power transformation will be described: In a power transformer, energy flows from the primary winding to the secondary winding. Since these two windings are electrically isolated from one another, how is this energy transfer accomplished?

The answer is that the transfer occurs through the medium of the interacting electric and magnetic fields; this transfer takes place (neglecting fringing fields) across the reactance gap.

Electromagnetic energy flux can be expressed in terms of Poynting's vector, S :

$$S = \vec{E} \times \vec{H} = \frac{1}{\mu_0} \vec{E} \times \vec{B} \quad (6-1)$$

where E is the potential gradient, B is the magnetic flux density, H is the magnetic induction, and μ_0 is the permeability of the free space. In a cylindrically wound transformer coil (Figures 4-2 and 6-1), the vector S is in the radial direction (Figure 6-1). The total power, P , flowing from the primary to the secondary winding will equal the magnitude of S multiplied by the total surface area involved, the cylindrical surface presented by the reactance gap, as in Figure 6-1.

Therefore,

$$P = 2 \pi r h S = \frac{1}{\mu_0} 2 \pi r h E B \quad (6-2)$$

where r is the radius of the cylindrical surface, i.e., the radius of the reactance gap.

Equation 6-2 may readily be reduced to the familiar expression for power rating by utilizing the following expressions:

a. If e is the voltage per turn, then

$$e = 2 \pi r E \quad (6-3)$$

Figure 6-1. Fields, currents, and forces in cylindrical transformer windings

b. Neglecting fringing fields,

$$B = \frac{1}{h} \mu_0 nI \quad (6-4)$$

where n is the number of turns in one of the windings and I is the current in that winding.

Applying Equations 6-3 and 6-4 in Equation 6-2, one finds

$$P = neI. \quad (6-5)$$

However, ne is the terminal voltage, V , of the winding in question; therefore,

$$P = VI \quad (6-6)$$

which is, of course, the power transforming capacity of the coil.

Related to the power transforming capacity are radial stresses, which arise from the interaction of winding current I with the magnetic field, B . By the well-known force law, it is evident that a force per unit area, F , is exerted in the radial direction on the winding as a whole; this is readily shown to be

$$F = \frac{1}{2} \frac{\mu_0 n^2 I^2}{h^2} \quad (6-7)$$

F acts in an inward direction on the inner winding and in an outward direction on the outer winding, regardless of whether power is being "transformed" from

the inner winding to the outer or vice versa. (This can be noted in Figure 6-1, in which the vector product I and B indicates a radial direction.)

Combining Equations 6-5 and 6-7, one finds

$$P = he \sqrt{\frac{2F}{\mu_0}} \quad (6-8)$$

which displays the direct relationship between transformed power and radial stress in the windings.

Computation of the radial stress in the winding in a specific case provides an indication of the magnitudes to be expected. For a certain projected sheet-wound, compressed-gas-insulated design with a rating of 500 MVA, 500 kV(Y)/20 kV (Section 8):

$$I = 25,000 \text{ A}$$

$$n = 36 \text{ turns}$$

$$h = 2.462 \text{ meters}$$

Application of Equation 6-7 yields $F = 84,000 \text{ N/m}^2$ (about 12.2 psi). This is the time-average stress. At the current peak, F is twice this number, or

$$F = 168,000 \text{ N/m}^2 \text{ (24.4 psi)}$$

Clearly, these are comparatively small stresses. However, transformers must be built to withstand short-circuit fault conditions, which give rise to considerably larger and potentially very destructive, mechanical forces. [Note the dependence of F on I in Equation 6-7.]

From the standpoint of the transformer, the worst possible fault is one for which currents are limited only by the reactance of the transformer itself. Furthermore, the worst case should also account for the possibility that the fault is initiated at the peak of the ac current wave and is accompanied by a transient "off-set" condition such that current through the fault steadily increases during the ensuing half-cycle, reaching a crest value of twice the peak amplitude of the ac fault-current wave. Since some degree of damping will inevitably be present in the transient fault condition (in any case, at least the resistance of the transformer winding), it is generally accepted procedure to regard the highest possible fault current as equal to the normal rms full-load current increased by the factor

$$1.8 (\sqrt{2})/x$$

where x is the transformer reactance, expressed as a "per unit" quantity. Therefore, for the case above, the highest possible fault current with 11.1% impedance would be

$$I_{\max} = \frac{1.8 \sqrt{2}}{.111} I = 22.93 I.$$

Hence, according to Equation 6-7, the resulting maximum radial stress, F , under fault condition is

$$F_{\max} = 44 \times 10^6 \text{ N/m}^2 \text{ (6400 psi)}$$

6.2 Mechanical Structure of Sheet-Wound Coils

The first coils fabricated for this development had the general structural features shown in Figure 6-2. The coil per se - inner and outer windings - was held radially between inner and outer structural cylinders of glass-reinforced epoxy. Axially the coil was held between upper and lower support plates of glass-reinforced epoxy. To be effective, the end supports must bear upon a structure which is rigid. This meant that the open space lying beyond the edge of the conductor and between the overhanging turn-to-turn insulation (Figure 3-1) had to be filled to make a rigid structure. This was accomplished as the coil was being made; margin filler strips whose thickness exactly matched that of the conducting sheet were wound into this space (Figure 6-2).

Figure 6-2. Sheet-wound coil with plate for end-support

The windings were wound directly on the inner cylinder. When the winding process was completed, the end support plates were placed over the ends of the coil and held in place by retaining rings of glass reinforced epoxy. Finally the outer cylinder was applied. Assembly of the entire coil was completed on the winding lathe. The inner cylinder fitted over the center leg of the transformer core and had an axial height matching the height of the core window. The weight of the entire assembly was borne by the inner cylinder, which, in turn, rested on the core and core frame.

Though the structure shown in Figure 6-2 is conceptually simple and direct, it has a number of disadvantages.

- The end supports are cumbersome: Coils up to 200 kg were built in this way, for which the support plates were 2.5 cm thick. However, projecting this technique to full-sized coils, the plates either become very much thicker or need reinforcement by gussets. Furthermore,

the necessity to bring all leads and coolant pipes through the support plates would create problems in a full-scale application.

- In terms of high-voltage insulation, the design has potential weakness: As used in this work, the average radial electric stress over the plate was not more than 2 kV/mm, and no partial discharges were detected. However, at the gradients which would be encountered in full-scale applications, problems with partial discharges might arise, since, in general, it would be difficult to make these end supports free of internal voids.
- The margin filler strips are an added complexity in manufacture.

After building three coils using the design of Figure 6-2, this technique was abandoned in favor of a self-supporting structure.

Thin sheet material wound tightly over a mandrel will support itself against axial displacement. This is commonplace in the industrial handling of paper, polymer film, and fabrics. However, application of this concept to a sheet-wound transformer is greatly complicated by the necessity for the coil to resist stresses which can be much greater than those arising simply from gravity, and which are in the radial as well as the axial direction.

In order to determine whether there is enough adhesion to hold a winding together in a self-supporting manner and without adhesives, the coefficient of friction was measured for the possible combinations of surfaces: polymer film-polymer film, aluminum-polymer, and stainless steel-polymer.*

The measurements were made by using a jig in which two plates were pressed together by calibrated springs (Figure 6-3) while the pull necessary to slide one surface across the other was measured with an Instron tester. Both the force necessary to start sliding and that necessary to maintain motion were measured and converted to static and dynamic coefficients of friction. The results are shown in Figures 6-4 to 6-7.

That the self-supporting, sheet-wound coil is possible in principle is suggested by the following: Assume a 100-turn winding of the general dimensions of most of the model coils built for this work, i.e., aluminum sheet 0.025 mm thick and 61 cm wide interwound with two layers of polymer film of 0.025 mm thickness. Assume that a winding tension of 900 N/m is applied. Then, taking into account the strain produced throughout the winding, but assuming an inner cylinder of infinite rigidity, it can be shown that the innermost turn (polymer film-to-aluminum) will support a shear load of 7000 N/m, 150 times that imposed by the static gravitational load.

6.3 Calculation of Stresses

6.3.1 Radial Stresses

For the calculation of radial stresses, the configuration of a projected design of a 500 MVA, 500 kV(Y)/20 kV single phase power transformer (discussed

*The specific polymer film for these measurements was the polyethylene terephthalate (polyester) film, Mylar.

Figure 6-3. Photograph of test fixture for measuring the coefficient of friction; pin to the right fits into Instron machine

Figure 6-4. Friction coefficients of Stainless Steel (304)-Mylar film

Figure 6-5. Friction coefficients of Mylar-Mylar-Mylar

Figure 6-6. Friction coefficients of Mylar-Aluminum-Mylar

Figure 6-7. Friction coefficients of Aluminum-Mylar-Aluminum

in greater detail in Section 8) was analyzed. The axial height of the winding was 2.06 m and the radial builds of the inner and outer windings were 12.81 cm and 15.94 cm, respectively. The reactance gap was 1.0 cm. The current densities used in the calculation (density based on the total cross-sectional area, including ducts and insulation) were 294 A/cm² and 243 A/cm² for the inner and outer windings, respectively.

Each winding was considered a linear array of conductors with the same cross-sectional arrangement as the actual winding, the same current densities (corrected by the winding space factor), and the same height and radial build.

The method of these calculations was essentially the same as the method for calculations of eddy current effects (Section 4.1.2). This method involves determining the force on each winding -- a force produced by the other winding, and by the images of both windings in the plane interface representing the core. The problem was analyzed in rectangular coordinates, ignoring the cylindrical geometry of the actual windings. For calculation, each winding was divided into a grid consisting of squares 1 cm on a side. The radial build of the inner winding was taken as 13 cm, to make an even number of squares, and the outer was taken as 16 cm (Figure 6-8).

To calculate the force on a radial column of grids in the inner winding, the effect of each of the squares in the outer winding and in the images of both windings was calculated for each of the 13 squares in the radial column of the inner winding. Since there are $[16 \times 206 \times 2] + 2678 = 9270$ calculations for each of the 13 squares, there are 120,510 calculations to obtain the force on one radial column in the inner winding.

RADIAL FORCES IN A 500 MVA, 500 kV(Y)/20 kV
TRANSFORMER DESIGN

243 A/cm² INNER WINDING; 294 A/cm² OUTER WINDING

Figure 6-8. Scheme for computation of radial forces

Each calculation uses the current density in each winding, the distance between the squares, and the projection of the force on the radial axis, to enable consideration of forces between two squares.

In the calculations, the current is considered to be concentrated at the centers of the squares. If the x-axis is in the radial direction and the y-axis in the axial direction (i.e., vertically), and subscripts 1 and 2 correspond to square 1 and 2, the radial force between two squares is proportional to

$$\frac{I_1 I_2 (x_2 - x_1)}{d^2}$$

where $d = \sqrt{(x_2 - x_1)^2 + (y_2 - y_1)^2}$.

When the currents in the two squares under consideration are in the same direction, the force is attractive, and vice versa. Thus, the outer winding pushes the inner winding inward (Figure 6-1), while the image of the inner winding pulls it inward and the image of the outer winding pushes it outward.

A refinement introduced into these calculations is a consideration of the effect of the enhanced currents in the edges of the sheet (Figure 6-8). Current densities were considered uniform within the bulk of both windings, but in 2 cm at the outer end of each winding, the current was that given by the eddy-current calculation. In the inner winding, the current at the inner edge is greatest. The factor, F, by which the current is enhanced at the edges was taken to be

$$F = 2.213 + 0.248 x$$

for the inner winding, and

$$F = 1.475 + 0.219 x$$

for the outer winding; x being the transverse (radial) distance from the reactance gap. Additional description of the above method is given in Appendix D.

The maximum radial stresses are shown in Figure 6-8. These maximum stresses occur at the inner winding ends (position A) where an inward pressure of 8.7 N/cm^2 is produced. This is the force for full-load current in the subject design.

In Figure 6-8, stresses are also quoted for 10-times-normal currents. A radial force of 8700 N/cm^2 (1260 psi) does not seem unreasonable from the standpoint of supporting the winding, especially when one considers that this high stress occurs in a relatively narrow band at the edges of the coil. For example, if a supporting cylinder 1.4 m in diameter and 1 cm thick were used, the compressive stress at the highest stress point in the cylinder would be $60,900 \text{ N/cm}^2$ (88,200 psi). If the cylinder could be effectively braced against the massive steel core which it surrounds, this stress would be reduced.

In the outer winding, the maximum radial stresses would also occur at the ends (position D) and are calculated to be 730 N/cm^2 (1060 psi) at 10-times-normal current. With a 2-m-diameter outer cylinder 1 cm thick, the tensile stress would be $73,190 \text{ N/cm}^2$ (106,000 psi), assuming that the winding made no contribution to this hoop-tension stress.

As discussed in Section 6.1, under worst conditions, currents can be 20 to 25 times greater than normal currents, yielding 400- to 600-fold increases in the stresses from those shown in Figure 6-8.

6.3.2 Axial Stresses

In addition to the fundamental radial stresses, it is clear from Figures 4-2 and 6-1, for example, that there are axial stresses which arise from the interaction of the winding current edge with the radial H field which also exists at winding edges. The stresses act downward on the top edges of the winding and upward on the bottom edges of the winding. Consequently, no net force results on the winding as a whole, but the conducting sheets experience forces which may cause axial buckling.

One possible force, which in conventional transformer practice sometimes is the most serious of all, is that which produces a net axial displacement force on one entire winding with respect to the other entire winding. It is clear that this can occur only if the current field and magnetic field are not perfectly symmetric about the midplane of the winding.

One way such an asymmetry can arise, illustrated in Figure 6-9, might occur if one winding "slipped" with respect to the other. Figure 6-9 also suggests the manner in which the resulting displacement force could be calculated; namely, by calculating the force of interaction between the small shaded winding with the rest of the windings in the figure. It is evident that the situation illustrated is unstable, i.e., the displacement, 2Δ , results in a force in the direction of greater displacement. Furthermore, this force increases as Δ increases.

Figure 6-9. Axial displacement of inner winding with respect to outer winding

The situation is not this simple, however. Not only have the images in the core been neglected, but, more basically for the sheet winding, displacement as in Figure 6-9 causes a change in the current distribution over the width of the sheets in both windings. It can be shown³⁷ that this redistribution lessens the unstable force, but does not reduce it to zero.

The displacement shown in Figure 6-9 may be a reasonable one to consider in conventional, wirewound, power transformer practice, in which inner and outer windings are fabricated as separate units, and separately supported in the transformer. However, it may be an unlikely type of displacement to be experienced by sheet-wound coils as they are fabricated in the present work.

6.4 Preliminary Mechanical Tests on a Sheet Wound Coil

As a preliminary assessment of the mechanical performance of a self-supporting sheet-wound coil, high current tests were conducted on a test coil instrumented to measure the following parameters:

- Current magnitude and wave shape
- Magnetic flux at various vertical locations above and below the edge of the coil
- Strain at various vertical and circumferential positions on an outer epoxy-fiberglass girdle
- Temperature of the low voltage winding
- Coil motion - using a high-speed movie camera

This coil, wound under tension as described in Section 7, was of the self-supporting type. The inner cylinder was of glass-reinforced epoxy with a 2.5-cm-thick wall. An outer glass-reinforced epoxy cylinder (girdle) was 3 mm thick.

The specifications of the coil were as follows:

Winding Form (fiberglass-reinforced epoxy cylinder)

O.D.	0.55 m
Length	0.775 m
Wall thickness	2.54 cm

Inner Winding (Low voltage winding)

Turns	25
Conductor	Aluminum sheet, 61 cm x 0.078 mm
Turn insulation	Two sheets of polymer film, 62 cm x 0.025 mm

Reactance Gap

Total gap	1 mm
Construction	8 turns of polymer film, 62 cm x 0.125 mm

Outer Winding (High voltage winding)

Turns	1670
Conductor	Aluminum sheet, 61 cm x 0.025 mm
Turn insulation	Two sheets of polymer film, 62 cm x 0.025 mm

In Figures 7-9 and 7-10, the coil is shown during the course of construction. In Figure 6-10, the coil is in position for high current testing. The cylinder was held securely in position by a bar mounted on the top edge which was bolted to the floor. The high speed movie camera is shown to the right.

The high current tests were conducted by short-circuiting the 1670-turn outer winding and applying voltage, through a synchronous switch, to the inner winding. Three cycles of alternating voltage were applied at nineteen voltage levels from 130 to 2300 V. Both symmetrical and fully-offset current waves were produced (Figure 6-11), with peak values ranging from 1400 A to 43,000 A. (The highest value was the limiting value available from the alternator.)

Strain gauges were attached to the inner and outer epoxy-fiberglass cylinders; as shown in Figure 6-12, they were located near the edges of the cylinders and at midpoint (axially).

Since the outer cylinder was only 0.32 cm thick compared to 2.54 cm for the inner cylinder, the stress was greater in the outer cylinder. No strain gauge signals at all were observed on the inner cylinder, although gauges were

Figure 6-10. Photograph of the high current coil ready for testing

Figure 6-11. Current waveform used for preliminary mechanical tests on a sheet-wound coil

shown to be very sensitive by tapping on the cylinder nearby. Only the strain gauge mounted at the end of the outer cylinder in position to detect circumferential strain showed any response and the response was too small to be assigned a quantitative value; it was less than 10^{-5} .

A calculation of the outward radial force at the end of the outer cylinder gave a maximum force of 268 N/cm^2 (see Appendix D) for a current of 43,000 A (peak offset). Since the diameter of this cylinder is 81.3 cm and its thickness

Figure 6-12. Photograph of high current test coil showing strain gauges

0.32 cm, the tensile hoop stress in the fiberglass-epoxy cylinder would be $268 \times 81.3/2 \times 0.32 = 34,044 \text{ N/cm}^2$ (49,700 psi). This assumes that none of the stress is supported by the aluminum conductor. Assigning a modulus of elasticity of $2.07 \times 10^6 \text{ N/cm}^2$ (3×10^6 psi) to the fiberglass epoxy cylinder, a strain of $34044/2.07 \times 10^6 = 0.016$ would have been observed, whereas the observed strain was less than 10^{-5} . These results suggest that the aluminum conductor is actually absorbing most of the load.

At each current level during the tests, high speed movies were taken of the sample coil. The camera was focused on a line drawn radially in black ink over the top edges of the polymer films in both inner and outer windings. Although the high current lead to the coil was observed to jump violently at each current shot, no detectable coil motion was noted in the movies during any of the shots.

Careful inspection of the coil following the tests failed to reveal any discernible damage to any part of the coil.

Thermal measurements were made in two different ways: a thermocouple was positioned under one of the high current leads and measurements were taken starting about 30 seconds after each shot for periods up to 10 minutes; the resistance of the low voltage winding was measured as a function of time starting about 1 minute after the shot. The latter method gave higher readings and was found to be the most accurate.

These temperature measurements showed little temperature rise until the last two shots at 17,000 A and 20,000 A rms current. At 17,000 A, a temperature of 53 °C for the average winding temperature was observed one minute after

the shot. Four minutes later it had dropped to 41 °C. At 20,000 A, the average winding temperature was 66 °C after 80 seconds and dropping rapidly; it was 56 °C after 3 minutes and 40 °C after 21 minutes. Figure 6-13 is a logarithmic plot of the temperature above ambient, showing the linear decay that was found. From Figure 6-13, it is estimated that the temperature momentarily reached 74.5 °C.

Figure 6-13. Average low voltage winding temperature measured following short circuit current of 20,000 A rms, plotted as the difference between average winding temperature and ambient temperature (≈ 20 °C)

It should be borne in mind that this is an average temperature. During the application of the current, a higher current will flow in the edges of the sheet conductor where the temperatures will be higher than in the center of the coil. However, due to the high thermal conductivity of the aluminum, the heat will quickly spread to the rest of the winding.

We believe these temperature measurements illustrate the large amount of energy absorbed by the winding without damage and also show how rapidly heat is dissipated into the cooler parts of the winding.

Conclusions from Preliminary High Current Tests on a Model Sheet-Wound Coil

- Methods have been developed for measuring the reaction of sheet-wound coils to short circuit forces.
- The sheet-wound coil structure has shown itself to be very durable when subjected to short circuit forces. Comparing the measurements of strain on the outer fiberglass girdle with calculations leads to the conclusion that most of the stress is absorbed by the aluminum conductor rather than the fiberglass retaining cylinders. This is considered to be an important beneficial feature of the sheet winding.
- Further tests are needed on coils whose parameters more closely approximate those which would be used in a practical design.

6.5 Summary Comments on Mechanical Design

For a large conceptual power transformer design, the mechanical forces which arise from interaction of reactance flux with winding currents have been calculated. Under fault conditions, the forces produce stresses of significant magnitude. Since it is believed that the sheet-wound, compressed-gas-insulated transformer will be significantly smaller than the conventional transformer of the same rating, it is inevitable that stresses in the proposed design will be even greater than in conventional transformers. However, it is believed that the sheet-wound coil structure is inherently better able to withstand such stresses.

Techniques for stress computations have been developed which incorporate the computed current distributions to be expected in sheet windings. The development of such computations is a necessary part of a mechanical design, but it is not a "design" per se.

A concept for a mechanical design, the pre-stressed, self-supporting design, has been employed in a number of model coils built for this work. This concept is offered as a starting point for future work on these technologies. Its advantages are its structural simplicity and the absence of any interference with an open, gas-insulated gap from the coil to ground. The concept relies on structural cylinders at the inner and outer diameters of the coil and internal winding tension to maintain the integrity of the coil. The inner winding is assumed to contribute nothing to the strength against inward radial buckling forces. The outer winding is assumed to make a partial contribution against "hoop-tension" forces.

Preliminary tests on the mechanical performance of a self-supported sheet-wound coil subjected to high short circuit currents have demonstrated that such designs have a basically strong mechanical structure. Considerable additional work is needed on the mechanical design to ascertain whether the self-supporting winding structure can be applied to large coils or whether a supporting structure will be needed.

Section 7

MANUFACTURE OF SHEET-WOUND COILS

Section 7

MANUFACTURE OF SHEET-WOUND COILS

7.1 The Key Elements in the Fabrication Process

The novel aspects of the proposed design are largely concentrated in the coil. Therefore, the development of coil fabrication methods has been the greatest challenge, and the greatest achievement, of this work. Those aspects of the design which are particularly noteworthy and which are crucial for the fabrication procedure are:

1. Thin polymer film sheets for turn insulation
2. Sealed, metallic ducts wound into the coil
3. A complete transformer coil produced as one structural entity on the winding machine: i.e., primary and secondary (and other) windings, cooling ducts, and core cylinder
4. Prestressed, self-supporting coil between inner and outer structural cylinders.

Certain requirements are implicit in transformer coils possessing these features. Based on the experience gained in the coil-winding development, key elements can be identified:

1. Elimination of particle contaminants above a certain size (2-3 μm)
2. Maintenance of known and controllable tension throughout the winding process
3. Alignment and parallelism among the principal parts of the machine; main driven axle and inner cylinder, idler rolls, and supply rolls
4. Uniformity and concentricity of supply rolls.

7.2 Development of a Winding Machine

The first coils built for this work were wound on the machine shown in Figure 7-1. This machine was comprised of an old winding lathe (visible in the background of the photograph), modified to some extent, and an added structure supporting the supply rolls (visible in the foreground).

As shown in the photograph, a winding of sheet conductor 51 cm wide is being applied, and two layers of polymer film 56 cm wide are being interwound with the conductor. Between the two supply spools of polymer film, in the foreground, can be seen two narrow spools holding the margin filler strips of polymer film which were wound in at the conductor edges. At this stage in the development, coils were not made self-supporting, but relied on supporting flanges at the ends (Figure 6-2).

Even without any requirement for a self-supporting coil, it was discovered early that considerable tension ($3.5 \times 10^7 \text{ N/m}^2$ or better) was required to achieve a smooth, cylindrical winding. Tension was applied to the supply spools by spring-loaded friction brakes, visible in Figure 7-1. This simple system

Figure 7-1. First machine for winding sheet coils

required continual attention in order to make it work properly and had the basic disadvantage that the actually applied tension depended on the speed of winding.

The next evolution in the apparatus of Figure 7-1 dealt with preventing contaminating dirt particles from being included in the winding. Purely from an insulation point of view, only a very thin layer should be required between turns. For example, if a peak voltage of 200 V is to be insulated and a safety factor of 2 is applied against the threshold gradient for partial discharges, then a thickness of only 0.005 mm is required at a pressure of 8 atm. This is the thickness required on the basis of work with laboratory samples (data such as Figure 3-24). However, as a practical matter, use of polymer film/SF₆ gas insulating layer as thin as 0.005 mm requires a perfection in the film itself, and a perfection in the control of spurious particles in the winding, neither of which is attainable with the present techniques.

Some degree of control over spurious particles is possible with an arrangement which directs an array of air jets at the surface of the materials being wound into the coils. Figure 7-2 shows an arrangement which was built for this purpose. In the case of the polymer film, the air jets alone are not sufficient, since particles are held to the insulating film by electrostatic force. (The polymer film can acquire significant static electrification as it is unwound from the supply roll.) However, the air jets can be preceded by a source for ionizing the air near the surface of the film: this has proven very effective in neutralizing the charge on the polymer surface so that the air jets can remove the particles. The static elimination shown in Figure 7-2 is a polonium source.

Figure 7-2. Air jets and device for ionizing the air near the polymer film

It is difficult to provide a quantitative measure of the effectiveness of removal of occasional spurious particles. Experience in this work leads, however, to the following generalizations:

- A specific method for dust removal, such as the air jets and ionization described above, is necessary in all cases. This technique is believed to be capable of removing particles of roughly 2-3 μm size and larger.
- With the techniques which have been tried so far in this work, it has not been possible to fabricate coils with turn insulation less than 0.05 mm thick (two sheets of 0.025 mm thickness) because of the imperfections in film that is thinner than this and the deleterious effect of fine particles on such thin film.

The concept of "contact winding," so-called by paper makers, seemed to have some significant advantages. This technique (Figure 7-3) places the supply rolls in direct contact with the coil being wound. Tension is applied through the supply spools and, in addition, the supply spools can be pressed against the coil to enhance the effective winding tension. A contact winding machine should be easy to align and to keep in alignment, and it eliminates the length of conducting sheet and polymer film stretching from the coil to the supply spool. These lengths of material (called "webs," by a paper maker) are quite evident in Figure 7-1. Long webs are believed to complicate the problem of entraining particles in the winding.

Some preliminary designs for a contact winding machine were produced, and the old winding machine was revised to permit experiments with contact winding. In the course of these experiments, two problems with contact winding were encountered:

Figure 7-3. Simplified sketch of a contact-winding machine

- Any dirt particles or other foreign matter in the supply rolls inevitably were entrained in the winding.
- Wrinkles and similar imperfections were faithfully transferred to the winding, and adjustments in winding tension and pressure appeared inadequate to rectify this situation.

Both of the above problems relate to imperfections in the supply rolls. It would be possible to unroll and reroll the supply materials, to clean them, and to stretch them smooth in the process. These preprocessed supply rolls could then be used in a contact winding machine. However, it was decided to discontinue the idea of building a contact winding machine and, instead, to design and build a machine along more conventional lines.

The resulting new winding machine is shown in Figure 7-4; Figure 7-5 is an overlay of Figure 7-4 to aid in identifying the more important components.

A significant feature of the design is the idler roll (Part A, in Figure 7-5) which can be driven against the winding by two pneumatic cylinders which operate on its support arms (Figure 7-6). A force up to 272 kg was applied to the winding as a high pressure over the small contact area between the idler roll and the winding. The roll must be made with precision. It is stainless steel with a highly polished cylindrical surface.

The idler roll, operating under pressure, results in a tighter winding with higher effective winding tension. Evidently, the thickness of included gas between layers of the supply material is reduced in this way.

The two rolls marked B (Figure 7-5) were added to presmooth the aluminum before it passes onto the winding. The position of the B rolls was quite critical in order to smooth the aluminum effectively. Part C is an electrical corona source, an alternate technique to the polonium source used to discharge the polymer film and thereby to facilitate the removal of adhering particles.

Figure 7-4. New winding machine for sheet coils

Figure 7-5. Guide to accompany Figure 7-4

Figure 7-6. Pneumatically-actuated support arms for idler roll

Part D is a Warner electromagnetic brake, one of which is attached to each of the supply rolls. It is by these devices, shown in greater detail in Figure 7-7, that tension is applied to the winding. The electromagnetic brake and its associated power supply and control circuit (F in Figure 7-5) permit tension to be set as desired. The actual web tension is then maintained constant, even as the supply roll diminishes, through a sensing arm, E, and the potentiometer to which the arm is connected. Part G is a cog brake (visible also in Figure 7-7) which can be engaged to maintain tension when power to the electromagnetic brake is removed.

Figure 7-7. Electromagnetic brake

The greatest single problem with the winding of coils has been with the aluminum. Some typical difficulties with aluminum sheet rolls, as supplied, are

- Imperfections on edges
- Occasional wrinkles within bulk of supply roll
- Nonuniform sheet thickness.

The first difficulty can usually be overcome by machining the edges of the roll. A technique has been developed for setting up the roll in a machine lathe and machining the end-faces of the rolled sheet. Rolling or contouring of the sheet edges may also be beneficial in giving improved dielectric performance. Future studies should address the relative merits of these different methods of finishing the foil edges.

Wrinkles within the supply roll, if not too severe, are usually removed by the combined effects of winding tension and the idler roll (A, in Figure 7-5). If the imperfections are too severe to be removed in this way, the winding must be interrupted, the damaged segment removed, and the sheet spliced.

The nonuniformity in thickness takes a characteristic form which appears to be a consequence of the way in which aluminum sheet is made. The supplier rolls the sheet to a desired final thickness in a width which is usually some 152 to 178 cm. These wide sheets are generally thickest in the middle, and taper so that they are thinnest at the edges. After the wide sheet has been slit into narrower sheets, the narrower sheets are also tapered, i.e., thicker at one edge than at the other, except a segment cut from the center of the original wide sheet.

This sort of nonuniformity, which results in a sheet consistently thicker on one side than on the other, is a much more serious problem for sheet windings than would be a random variation in thickness. At present, this difficulty is overcome in the following way. Sufficient tension is applied to the aluminum sheet so that cold-flow occurs, partly correcting the nonuniformity in thickness. For winding conductor 0.025 mm x 61 cm, a tension of 680 to 908 kg is required to accomplish this.

The winding machine is shown in a side view in Figure 7-8. With the care and precautions outlined above, this machine is capable of winding coils of high quality, as large as 91 cm in axial length and 150 cm in diameter. A completed coil, before the addition of the outer structural cylinder, is shown in Figure 7-9. The outer cylinder of glass reinforced polymer (Figure 7-10) is applied, and cured in place, to complete the coil.

Figure 7-8. Side view of new winding machine for sheet coils

7.3 Cooling Ducts

Figure 5-2 suggests the general shape of the cooling ducts as they have been built for force-cooled coils. As described previously, coolant liquid is pumped from a manifold at ground potential through insulating pipes to the cooling ducts, which are at high voltage. Headers, circular in cross section, at the duct edges distribute the coolant over the inlet edge and collect the coolant from the exhaust edge. The coolant, therefore, flows axially through the duct.

Figure 7-9. A completed coil, before addition of the outer cylindrical cylinder

Figure 7-10. Outer structural cylinder of glass-reinforced polymer

The earliest technique for making ducts was to form the walls of the duct and the headers by a suitable rolling operation on sheets of metal. With the duct formed in two pieces, the final step was to assemble the two pieces by a seam-braze circumscribing the duct (Figure 7-11). Within the duct are metallic spacer strips, equal in thickness to the gap in the duct and running from one header to the other. These spacers are spotwelded to one of the duct halves before the duct is closed with the seam brazing. After assembly, each duct is checked for leaks using a vacuum system and helium leak detector.

Figure 7-11. Structural features of cooling ducts for the first force-cooled coil

A difficulty with this fabrication technique was the warping which tended to occur in the localized heating necessary to accomplish the braze. Jigging is necessary during assembly in order to control the warping.

If this technique is extended to larger ducts and to stainless steel sheet as the duct material, the required jig is a comparatively massive and complicated structure (Figure 12). A number of ducts were successfully built while using this technique; but the difficulty involved stimulated an innovation in duct design and fabrication.

Figure 7-12. Jig for assembly of a cooling duct

The two key elements in the new fabrication technique were

- A self-jigging duct design
- Final assembly by furnace brazing.

The headers were slotted to receive the ends of the cylindrical duct walls, which are separated by spacer strips (Figure 7-13). The joining technique consisted of the following steps: resistive welding (fusion), TIG*-arc welding, continuous welding, and high temperature (1100 °C) furnace brazing. All materials were 304L stainless steel.

The most critical location during fabrication is the joint between the tubular header and the cylindrical duct walls (view A-A, in Figure 7-13). Due to the thinness of the tube walls and the sheets, the heat required for welding resulted in a significant structural distortion. However, the technique of furnace brazing with commercially available alloy (Microbraz 130**) has eliminated this problem. By using filler strips to help position the header tube, the header can be successfully brazed to the duct walls.

*TIG - tungsten inert gas

**Microbraz is a registered trademark for Wall Colmonoy's braze alloy.

Figure 7-13. Structural features of the improved, self-jigging, duct design

An actual joint corresponding to the view A-A is shown in the photograph of Figure 7-14. In this photograph, the filler strips and panel structures have been chemically etched to highlight the braze joint. Prior to the application of the brazing alloy, the individual components for the duct assembly are welded together, in the steps named above, to produce a rigid, self-jigging assembly.

Figure 7-14. Photograph of a joint in duct of new design

Duct parts before assembly are shown in Figure 7-15, in which some of the internal spacer strips, "tack-welded" in position, may be seen. A completed cooling duct quadrant is shown in Figure 7-16.

Figure 7-17 is a photograph of a duct portion in which the end section of the header has been cut away to reveal the interior of the joint between the header and the duct wall. In the photograph, one is looking directly down into the duct, which has an internal thickness of only 1 mm. The ends of the spacer strips can also be seen.

Figure 7-15. A partly assembled cooling duct

Figure 7-16. Completed quadrant of a cylindrical cooling duct

Figure 7-17. Photograph of a portion of a cooling duct with part of the header cut away

7.4 Improvements in Cooling Ducts

The axial flow duct with tubular headers, discussed above, has been employed in all force-cooled coils built for this work. This type of duct has two inherent disadvantages:

1. It is a rigid structure - due mainly to the stiffening effect of the headers - and, consequently, must be preformed to approximately the right curvature for the place it will occupy in the coil.
2. The tubular headers protrude from the ends of the coil (cf. Figures 3-17 and 3-18), which means that a larger insulating gap must be employed from coil to yoke than would be required without the cooling duct.

Axial coolant flow is not the only possibility: Ducts could be built in which the coolant flow is circumferential. In such a case, the headers would have to be wound into the coil, and therefore would force a departure from a perfect circular-cylindrical winding. This is a disadvantage in mechanical design, but the advantages of the circumferential-flow duct are substantial, viz,

- Ducts could probably be made with sufficient flexibility that they could literally be wound into the coil,
- The axial dimension of a circumferential-flow duct need not be greater than the conductor width: Consequently, the duct would not protrude from the end of the coil.

A more radical approach to cooling ducts is suggested by the very high thermal conductance which the coil structure possesses in the axial and circumferential directions, compared to the conductance in the radial direction through the insulating layers. From this inherent property of the sheet-wound coil one might infer that a complete cylindrical duct is not necessary. Much more localized ducts (e.g., thin, flat strips of cooling duct) might be inserted at intervals in the winding process, and therefore would be distributed circumferentially and radially throughout the winding. The very small effect experienced when one quadrant of the whole duct was closed-off suggests that effective cooling could be accomplished with small localized ducts.

Section 8

A CONCEPTUAL DESIGN FOR A 500 MVA, 500 kV(Y)/20 kV SINGLE PHASE POWER TRANSFORMER

Section 8

A CONCEPTUAL DESIGN FOR A 500 MVA, 500 kV(Y)/20 kV SINGLE PHASE POWER TRANSFORMER

8.1 Review of Assumed Design Quantities

One way of displaying the results of this development is to use them as the basis for a full-scale power transformer design. Such a design would be most useful if it met the specifications usually applied to a transformer of conventional design. A comparison could then be made of the projected values of the principal weights and dimensions of the sheet-wound, compressed-gas-insulated design with those of a conventional transformer. With this in mind, a 500 MVA, 500 kV(Y)/20 kV single phase power transformer was chosen to be the subject of a basic design study.

There are some disadvantages to a design based on the present concepts in comparing it with a design based on present practices regarding capacity, voltage, impedance, and efficiency. These disadvantages include the following:

- The optimum design of a sheet-wound, compressed-gas-insulated transformer may lie at substantially lower loss levels than those which are usual (or possible) with conventional practices.
- It seems likely that impedances could be achieved in the sheet-wound, compressed-gas-insulated design which are significantly lower than those found in wire-wound, oil/paper-insulated designs.

For the purposes of the present program, however, the conceptual design of the gas-insulated transformer will meet conventional design specifications.

Such an approach has the following limitations:

- To produce an optimum design requires detailed information on manufacturing costs, as well as quantitative measures of the value of losses to the user - especially of unusually low losses. This information is not presently available.
- While it is probably true that significantly lower impedance (viz., lower than 17%) is desirable, quantitative application information is needed in order to arrive at a value on which to base a detailed design study.
- A projected design based on an unusual voltage rating, losses, or impedance makes any comparison with conventional transformers difficult.

The prerequisite for a design is the establishment of design practices. For the sheet-wound, compressed-gas-insulated transformer, the design practices are still in the process of development through this and future efforts. A sufficient definition of design practices can be made, however, to serve as the basis of a design projection.

Table 8-1 contains quantities which are specific to the sheet-wound, compressed-gas-insulated design, and shows how the values assumed in the projected design are related to the development program. Table 8-2 shows additional design quantities which must be considered.

8.2 Description of Design

For a given design practice, a transformer design is fixed by the specification of any four variables, since the design inherently contains four independent variables (four "degrees of freedom"). Three variables are usually specified, in one way or another, by the user, viz., power-transforming capacity, impedance, and losses. One degree of freedom then remains to the designer; this should be determined by the requirement of an optimum design. (The definition of this "optimum" is, thus, a question of crucial importance.) Other possible constraints exist, however. In conventional practice, and for units of the largest size, the final degree of freedom must often be consumed in meeting size limitations imposed by shipping requirements.

The subject 500 MVA, 500 kV transformer design projected on the basis of sheet-wound, compressed-gas-insulated techniques is not constrained by shipping dimension limitations and therefore could, presumably, be designed to meet a

Table 8-1
BASIC DESIGN ASSUMPTIONS FOR A
500 MVA, 500 kV(Y)/20 kV
SINGLE PHASE POWER TRANSFORMER

Basic Design Assumptions	Value Used in Design Projection	Level Achieved to Date ⁽¹⁾
Turn-Turn Stress (Electric)	25 kV/mm-Peak ac ⁽²⁾ 67 kV/mm-Impulse	82 kV/mm-Peak ac ⁽³⁾ >26.1 kV/mm-Impulse ⁽⁴⁾
Electric Stress in SF ₆ (8 atm)	20 kV/mm-Peak ac 54 kV/mm-Impulse	22 kV/mm-Peak ac
Electric Stress Along Coolant Pipe	12.6 kV/cm-Peak ac 20 kV/cm-Impulse	>60 kV/cm-Impulse ⁽⁵⁾
Maximum Heat Flux Density Within Coil	2 W/cm ²	1 W/cm ²
Maximum Temperature in Coil	120 °C	125 °C ⁽⁶⁾

(1) Additional testing is needed to establish statistically relevant stresses for full-size insulating structures.

(2) Based on factor of 1.5x higher stress used in test.

(3) Discharge inception stress on model sheet windings at 6 atm SF₆.

(4) Flashover in gas external to coil; further tests will be made in Part 2.

(5) 5 atm SF₆ gas; 30 cm long sections of coolant pipe.

(6) Tests conducted slightly above design temperature with no adverse effects.

Table 8-2
 ADDITIONAL DESIGN QUANTITIES FOR A
 500 MVA, 500 kV(Y)/20 kV
 SINGLE PHASE POWER TRANSFORMER

Flux Density in Core	1.7 Tesla (peak)
Current Density	
Low voltage winding	320 A/cm ²
High voltage winding	323 A/cm ²
Losses*	
Winding losses	1320 kW
Core losses	160 kW
In pressure vessel and internal structure - due to eddy currents	20 kW
Major Insulation Distances	
Coil-to-core end limbs	3.6 cm
Coil-to core yoke	4.5 cm
Reactance gap	2.0 cm

*These losses have not been optimized. Generally, there will be a trade-off between losses and transformer size: lower losses can be realized at the expense of smaller decreases in size and weight.

specified optimum. Here, however, the difficulty mentioned above intrudes, viz., that the quantitative information requisite to establishing an overall optimum design is not available at this stage in the development. What has been done in the absence of this information is to fix the fourth degree of freedom by establishing a design of minimum total weight. This is in lieu of a comprehensive optimum. In examining the design, as summarized in Tables 8-3 and 8-4, it is important to bear in mind that this rather arbitrary prescription has been made. Figure 8-1 is an artist's concept of this design.

In Table 8-1, five basic design assumptions are listed. The values in the first column are those which are significant to the 500 MVA, 500 kV design under study. The second column gives values obtained experimentally during the present program.

Losses caused by eddy currents induced in the pressure vessel and in certain internal structural members are usually a significant component of total losses in conventional transformers. These losses are a consequence of the reactance flux, i.e., they are "load" losses. It is believed that they can be substantially reduced through the use of the "cruciform" core configuration (Figures 4-7 and 4-8).

The quantity of 20 kW presented in Table 8-2 is an estimate for the subject 500 MVA, 500 kV power transformer built on a cruciform core.

Table 8-3

DESIGN PROJECTION OF A
500 MVA, 500 kV(Y)/20 kV
SINGLE PHASE POWER TRANSFORMER

Total Losses*	1500 kW
Impedance*	17%
Basic Insulation Level*	1550 kV
Insulating Gas	SF ₆ - 8 atm
Pressure Vessel	490 cm x 352 cm diameter
Core Dimensions: (Five-legged "cruciform" type)	
Diameter	140 cm
Window Height	215 cm
Window Width	36 cm
Component Weights	
Core	83,000 kg
Coil	7,800 kg
Vessel and Internal Structures	17,000 kg
Insulating Gas	1,160 kg
Total Weight (excludes external heat exchanger)	109,000 kg

*These are values assumed for this design.

Figure 8-1. Artist's concept of 500 MVA, 500 kV transformer

Table 8-4
WINDING DESIGN FOR A
500 MVA, 500 kV(Y)/20 kV
SINGLE PHASE POWER TRANSFORMER

Currents	
500 kV Winding	1732 A
20 kV Winding	25000 A
Low Voltage Winding (20 kV)	
Number of Turns	31
Conductor*	206 cm x 1.9 mm (two sheets)
Turn Insulation	207.5 cm x 0.053 mm
High Voltage Winding	
Number of Turns	459
Conductor*	207 cm x 0.26 mm
Turn Insulation	207.5 cm x 0.053 mm

*Aluminum

Table 8-5
COOLING DESIGN FOR A
500 MVA, 500 kV(Y)/20 kV
SINGLE PHASE POWER TRANSFORMER

Maximum Winding Temperature	120 °C
Number of Cooling Ducts	4 (HV winding) 2 (LV winding)
Inside Thickness of Duct	2 mm
Total Coolant Flux Required at Full Load	190 l/s
Pump and Fan Power Required at Full Load	11 kW

8.3 Heat Exchanger Components

A possible heat exchanger designed for the dissipation of 1 MW of losses to ambient air would be as follows:

- Four 20-tube heat exchangers, each in a panel 1.14 m wide and 2.44 m high. The tubes would be made of copper, 2.54 cm diameter and with a 0.64 mm wall.

Pumping capability (R-113)	2000 gpm
Pressure drop	1 atm

- Four 10-hp pumps would be needed and eight 91 cm fans. Each fan would be driven by a 1.5-hp motor.

- Sound level when operated at full capacity ~75 dB at 1.8 m from the heat exchanger panels.

On a one-of-a-kind basis, the above components would have a cost of ~\$32,000. It is anticipated that the cost of manufacturing these components commercially would be significantly less.

Section 9

ADDITIONAL DESIGN CONSIDERATIONS

Section 9

ADDITIONAL DESIGN CONSIDERATIONS

9.1 Design of High-Voltage Leads

The design of high-voltage electrodes for use in compressed gases is characterized by the use of smooth surfaces (polished and, in some cases, coated with thin, high electrical resistance coatings) and the avoidance of small curvature radii. Barriers or equipotential shields between the high-voltage electrode and ground yield little or no gain in insulating performance. The best high-voltage insulating gap in compressed gas is therefore usually the gap with the simplest geometry.

An example of this is the high-voltage connection to the coil shown in the sketch of Figure 9-1. In this case, the high voltage is at the outermost turn of the coil -- an obvious advantage in any transformer design, but especially so in a compressed-gas-insulated transformer.

Figure 9-1. High-voltage lead for experimental transformer

In this project, the general approach suggested by Figure 9-1 has been employed in all of the model transformers to which high-voltage lead connections were necessary. Figure 9-1 shows the high-voltage lead arrangement which has been used.

9.2 Design of High-Current Leads

One of the basic advantages of the sheet winding is its inherent ability to handle high currents in a relatively simple way. A single sheet of

conductor, although very thin, can have a substantial cross section by virtue of extending continuously through the entire axial dimension of the coil.

The relative simplicity of the sheet windings should make possible a relatively simple lead design. A concept for a high-current coaxial lead and its connection to a sheet-wound coil is shown in Figure 9-2. The coaxial lead has the following advantages:

- There is no external magnetic field (excluding end effects) due to current in the coaxial conductor.
- The coaxial, cylindrical geometry is well-suited for insulation in compressed gas in a similar manner to a gas transmission cable.
- It is comparatively easy to cool using the compressed gas. (This can be achieved by heat transfer from the inner to the outer cylinder, with an added possibility of using openings in the structure.)
- It is inherently rigid and therefore simpler to support.
- If the conductor is made and installed with reasonable accuracy, there are no net electromagnetic forces on its straight segments.

Figure 9-2. High-current leads connected to a sheet winding

Some further details relevant to high current leads can be given with reference to the transformer discussed in Section 8. In this case, the current in the primary winding is 25,000 A. Two sheet conductors, electrically

in parallel, are required in this case. The introduction of parallel conductors immediately raises the question of required "transposition" of the parallel conductors. The manner in which this could be handled, together with a resulting advantageous arrangement of leads, is shown in the sketch of Figure 9-3.

Figure 9-3. Large sheet-wound coil with two transposed sheets, electrically in parallel

The use of two conducting sheets, axially, means that four conducting sheets are in parallel, electrically. Transpositions are accomplished among these sheets as suggested in the sketch. If the winding and the reactance flux are cylindrically symmetrical (or if the reactance field is known in detail), theoretically one transposition is sufficient. In practice, however, two transpositions would probably be necessary.

Four coaxial, tubular conductors supply the high current winding; each of these must carry 6250 A.

It seems possible that the use of sheet windings in power transformers may enable some fundamental improvements in the management of the high currents which are required.

9.3 Design for Ratio Control (Taps)

Provision for voltage-ratio variation (i.e., taps within the winding) is a commonplace requirement of power transformer application. In terms of their power system use, taps fall into two different categories:

- Those which can be changed when the transformer is deenergized from the line (often called "no-load taps").

- Those which can be changed while the transformer is not only energized, but carrying load (often called "load ratio control taps").

Ratio control has not been addressed in the present program, though undoubtedly it is an extremely important practical area which the present concepts must be capable of successfully overcoming. Three aspects of ratio control will need particular attention in future work on these technologies:

- Tap changer design

The tap changer must be capable of functioning properly in the compressed gas environment, especially with regard to cooling and mechanical wear. We believe that designs can be developed to satisfactorily meet the performance needs in the gas environment.

- Leads

As described in Sections 9.1 and 9.2, this is an area requiring considerable attention in future work on these new technologies. The major areas of attention will be electrical design (to avoid high localized electric fields) and cooling.

- Winding discontinuities

Winding discontinuities will have a major effect upon the electric stress distribution which develops in the windings when transient voltages are applied. Therefore, the effect upon transient response will be an integral part of design and construction of taps.

9.4 Overload Capability

Even the largest power transformers are required to be able to handle a significant overload for up to several hours. The requirements, as specified in the standard loading guides, are highly detailed. For the present, it suffices to say that the thermal "time constant" of the transformer as well as the margins inherent in present cooling design practice are utilized in meeting these requirements.

The present practice has a considerable inherent overload capability, even without the deliberate attempt of the designer to provide for it. The proposed new design, on the other hand, with its high-performance cooling system designed to a specific requirement, has only a modest inherent overload capacity. To meet overloads, special attention must be given to that requirement.

For example, the cooling system of the proposed design has a certain over-pressure capability and pumps can be overdriven to a degree for a limited time without serious consequences. These measures would give the cooling system a somewhat higher capacity to meet overloads. Even so, winding temperatures would rise and this would probably constitute the practical limitation, just as it does in conventional practice.

Related to the overload problem is the problem of response to failure of the forced cooling. Again, the conventional designs, which have a natural convection cooling capability, inherently possess an ability to meet the failure of forced cooling which the proposed new design can have only after special attention has been given to the problem.

An example of such special attention would be to divide the cooling system, external to the coils, into two independent subsystems, each a complete cooling loop with its own pumps and heat exchanger. If forced cooling failed in either one of these loops, half of the cooling capability still would remain. This, together with the thermal inertia of the transformer, provides a considerable overload capability. Of course, if the power supply for forced cooling failed, both cooling loops would fail at once. This contingency could be met by one or more of the following measures:

- Providing independent sources for power for each of the two loops
- Providing a standby power for cooling
- Arranging that power for cooling be dependent upon the power which the transformer is handling.

Section 10
CONCLUSIONS

Section 10

CONCLUSIONS

10.1 Accomplishments

New concepts for a compressed-gas-insulated power transformer have been proposed and an initial analytical and experimental evaluation of these concepts has been completed. The major accomplishments of the work to date are summarized below.

Insulation Design

In the area of electrical insulation, the proposed design incorporates two novel features:

- polymer film, impregnated with compressed gas, within the coil structure
- insulating pipes to convey (insulating) coolant liquid to the coil.

The Coil Insulation Structure

The coil structure has been tested in a total of twenty-eight simulated turn-to-turn samples of various thicknesses and total areas. The limits of the insulation system were determined by measuring the 60 Hz threshold voltage for partial discharges. Partial discharge inception stresses greater than 58 kV(rms)/mm have been obtained in turn-to-turn samples consisting of 0.05 mm polymer film (area $\sim 16 \text{ m}^2$) impregnated with SF_6 gas at a pressure of 6 atm abs. Complete model coils have also been tested under ac and impulse voltages. For ac, two large windings (conductor area $\sim 3300 \text{ m}^2$) were tested using 60 Hz induced voltage up to 265 kV (peak) with no detectable partial discharges. For impulse voltages, a 164-turn single winding (conductor area $\sim 168 \text{ m}^2$) was tested up to 200 kV at which point creepage flashover over the exterior of the coil occurred at a stress of 26.1 kV/mm. An additional test coil has been designed to test the full impulse capability of the turn-to-turn insulation; the results will be reported in Part 2 of the Final Report.

The above 60 Hz partial discharge inception stress ($= 58 \text{ kV(rms)/mm}$) is to be compared to an assumed 60 Hz operating stress of 13 kV(rms)/mm for a large conceptual EHV power transformer described in this report. The impulse strength capability of a sheet winding will be measured in Part 2.

The key to achieving a discharge inception stress adequately above the assumed operating stress for a large power transformer appears to be the ability to exclude extraneous particles during winding of the coil.

Coolant Lead Pipe Design

In tests using a special fixture in compressed SF_6 gas at room temperature, an insulating coolant pipe of 20 cm length and containing a quiescent

fluorocarbon coolant liquid withstood impulse voltages of 1200 kV, corresponding to an average gradient of 60 kV/cm along its length; this stress compares favorably with an assumed design value of 20 kV/cm (impulse) for the large conceptual EHV power transformer evaluated in this report. Measurements at 60 Hz have also been made on shorter sections (1.5 cm) of coolant pipe with fluid in forced circulation up to a voltage of 177 kV(rms), corresponding to a stress along the pipe of 188 kV(rms)/cm.

The present program has not addressed two factors which may affect the performance of the coolant pipe system: 1) the long term aging which may occur when a (hot) coolant liquid is passed through a coolant pipe for long periods of time with simultaneously applied electric stress; 2) the possible detrimental effects of electrification caused by the flowing (insulating) liquid in the insulating coolant pipe. Should there be difficulties in the latter area, it is believed that they can be avoided by suitable design modifications.

Electromagnetic Design

A study of the magnetic aspects of the design has included the important question of current distribution in sheet windings. Analytical techniques of general applicability have been developed in this work. A verification of the current distribution predicted by the analysis has been obtained through temperature measurements on a model forced-cooled transformer coil.

A cruciform core construction particularly lends itself to the compact coil design and the pressure vessel shape proposed in the present concepts. In the present program, such a cruciform core has been designed and constructed.

Thermal Design

Thermal design has been confined to the windings and the coil construction. The temperature distribution in sheet-wound coils has been studied analytically and experimentally. The analytical tools yield temperature values which are in good agreement with experimentally obtained temperature measurements on a model transformer.

Work on the cooling system as a whole has yielded some preliminary design quantities for full-sized power transformers. A simple, forced-cooling system for a model transformer has been built and used for this work. Additional attention to cooling system design is needed in future work on these proposed concepts.

Mechanical Design

Two different concepts for mechanical support of the entire transformer coil, including cooling ducts, have been employed in this work:

- End-support by means of annular plates, which are then supported from an inner structural cylinder

- Self-supporting coils, achieved through the use of tension applied during winding. The windings are then held in compression between inner and outer structural cylinders.

Except for the first three coils, model coils built for this work were of the prestressed, self-supporting type.

Example calculations have been made of the radial and axial stresses which would be experienced by a full-sized power transformer coil under fault conditions. However, a detailed mechanical analysis comparable to the magnetic and thermal analyses has not yet been done. The concept of the self-supporting coil requires some additional developments in its details of application before a thorough mechanical analysis is possible.

Axial stability has received preliminary study. It is clear that axial stability (i.e., of relative displacement of the inner and outer windings) qualitatively and quantitatively differs in sheet windings from the stability in wire windings. In sheet windings, stability depends upon the detailed geometry of the displacement.

A successful preliminary experimental evaluation of the mechanical performance of a sheet-wound coil subjected to short-circuit currents has been made up to currents of 20,000 A(rms) (43,000 A on the first fully offset peak): No mechanical damage or distortion of the coil was evident; the radial forces appear to be efficiently absorbed by the aluminum conductor.

Manufacturing Technology

The preliminary development of some of the basic manufacturing techniques for sheet-wound coils has been one of the major achievements of this work. Based on the experience gained in building the first six model coils, a new winding machine was designed and built. This machine incorporates a number of innovations to meet fabrication difficulties specific to sheet windings.

An important challenge in the future will be the task of extending winding techniques to much larger coils than already built.

The excellent dielectric performance of some of the coils constructed, particularly that composed of a dozen dielectric test samples, leads us to the conclusion that our proposed techniques for eliminating particles during winding can be applied to larger coils.

Construction of sealed, metallic cooling ducts, suitable for application to these coils proved to be a challenging task. Techniques have been developed which make it possible to build model forced-cooled coils.

Summary

The results of the work reported have led to the conclusion that it is technically feasible to build sheet-wound, compressed-gas-insulated, forced-liquid-cooled power transformers. These results have led to conceptual designs which offer the following significant advantages over conventional oil-filled designs of similar rating:

- lower losses
- lower weight
- smaller size
- nonflammability
- compatibility with compressed-gas-insulated substations and transmission lines.

Further advantages not studied in this program may be: inherently lower noise transmitted outside the pressure vessel; coil designs which are adaptable to automatic manufacturing processes; opportunity to introduce standard designs.

Results obtained to date, coupled with the above potential technical advantages, lead to the recommendation that further development of the sheet-wound compressed-gas-insulated power transformer should be vigorously pursued to a full-scale demonstration model.

10.2 Recommendations for Future Work

It is clear that a major development effort is necessary before a full-sized, prototype transformer, embodying the proposed concepts, could be built. Based on this investigation, it is recommended that the development effort should address the following areas:

Insulation System

- Turn-to-turn dielectric strength as a function of electrode area, conductor edge length, registration of conductor, tension in the winding, degree of impregnation of the SF₆ gas, effect of particles included in the winding process
- Turn-to-turn dielectric strength as a function of combined thermal and electric stress
- Coolant lead pipe dielectric performance as a function of long-term aging under combined flow, temperature, and electric stress
- Alternate materials for turn-to-turn insulation and coolant lead pipe.

Electromagnetic Design

- Improved techniques for calculating axial and radial forces
- Techniques for calculating magnetic fields throughout the transformer structure, including eddy currents in metallic members

Thermal Design

- Core thermal design
- Cooling of structural members

- Lead thermal design
- Additional work on coil thermal design, including possibilities for circumferential flow cooling ducts

Mechanical Design

- Design of coils to withstand short circuit and shipping forces
- Acoustic isolation of core to provide lower sound levels

Manufacturing Technology

- Development of winding technology to provide large coils with optimal dielectric and mechanical performance
- Comparative evaluation of alternate cooling duct fabrication techniques.

To investigate and pursue the above recommendations will require a major analytical and experimental program building upon the information and experience obtained during the present study. Present U.S. Department of Energy plans for the development of a 1200 kV gas-insulated, compact substation, including gas-insulated power transformers, calls for such a major program and requires the intensive multi-disciplinary effort suggested by the above recommendations.

Section 11
REFERENCES

Section 11

REFERENCES

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APPENDICES

- A — COMPUTATION OF EDDY CURRENTS**
- B — ANALYSIS OF TEMPERATURE DISTRIBUTION
FOR A SHEET-WOUND COIL**
- C — CONTROLLING THE FORMATION OF
GAS BUBBLES IN THE COOLING
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- D — METHOD FOR CALCULATING THE
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CURRENTS**

APPENDIX A

COMPUTATION OF EDDY CURRENTS

The most practical method of determining eddy currents in sheet-wound coils is the numerical method. It consists of dividing the windings into vertical columns and horizontal rows, which form a matrix of rectangles. Through each rectangle flows an unknown current which can be represented by a filament of current for that rectangle (let I_p be the current at point P in each rectangle).

Together, all unknown currents produce a magnetic field in the winding and in the space around the winding. This magnetic field is a linear function of the unknown currents, a function which is expressed in a linear equation for each current. Equations for all unknown currents form a matrix. Because this matrix consists of linear equations, it is easily solvable by a computer program.

In order to introduce an economy in computer time and capacity into the program, one designates the heights of rectangles near the edge of the winding (where current change occurs most rapidly) to be small. Likewise, one designates rectangle heights near the winding center (where current is nearly uniform) to be large. In fact, it is convenient to order rectangle heights in geometric progression. See Figure A-1, in which the height increases as it nears the winding center.

In Figure A-1 note also the Y axis (horizontal) and Z axis (vertical). In addition to these, though not shown, there is an X axis perpendicular to the plane in which the Y and Z axes lie. Using the Y, Z, and X axes, one may derive coordinates for any given point in the winding or in the space around it. Once a point is located, an equation to determine the magnetic field at that point can be constructed. For example, for a point at Y_0, Z_0 , the Y component of the magnetic induction, B_y , produced by a current filament of magnitude I - the current filament is at a different point, Y_1, Z_1 , from point Y_0, Z_0 - is given by the following equation:

$$B_y = -2 \times 10^{-7} IZ/R^2 \quad (1)$$

in units of tesla, where R is the distance between points Y_0, Z_0 and Y_1, Z_1 .

Once magnetic induction B_y is determined for one point in the winding (Equation 1), the total magnetic induction due to the entire winding current can be determined by finding the sum of inductions from all of the current filaments and their images. Then, one can use Faraday's law to write n equations in n unknown currents (n is the number of unknown currents). Following the development of n equations, one can use standard techniques to simultaneously solve these n equations for the value of each unknown current in each current filament. Specific equation details follow.

In applying Faraday's law, the first step is to construct a rectangle in the X,Z plane (winding column rectangles of Figure A-1 are in the Y,Z plane). This, X,Z-plane rectangle is bounded by two current filaments (Figure A-2). The top edge of this X,Z-plane rectangle is current filament P, and the bottom edge is current filament P-1. Top and bottom edges are l m long. The height, h, of the X,Z-plane rectangle is the distance between adjacent current filaments, and

Figure A-1. Cross section of windings showing columns, rows, and rectangles. Note the Y and Z axes, and the increasing rectangular heights nearer the winding center.

Figure A-2. The X,Z-plane rectangle through which magnetic induction B_Y passes.

equals $0.5 (h_p + h_{p-1})$, where h_p and h_{p-1} are heights of the (P)th and (P-1)th column rectangles in the Y,Z plane.

Faraday's law states

$$V = -d\phi_Y/dt, \quad (2)$$

where V is the voltage induced around the rectangular (X,Z plane) path, and ϕ_Y is the magnetic flux through the X,Z-plane rectangle and in a Y-axis direction.

$$\phi_Y = 0.5(h_p + h_{p-1})B_Y \quad (3)$$

Having defined ϕ_Y , one now defines the voltage drop around the X,Z-plane rectangle by the following expression:

$$V = I_P R_P - I_{P-1} R_{P-1} \quad (4)$$

where R_P and R_{P-1} are resistance to current flow for the (P)th and (P-1)th current filaments. Voltage drops along the vertical, X,Z-plane rectangle sides have been neglected for two reasons:

- The vertical side is very small when compared to the length, and,
- The current in the vertical direction is negligible.

The next step is to set the induced voltage of equation 2 equal to the voltage drop around the X,Z-plane rectangle (equation 4), which results in

$$-d\phi/dt = I_P R_P - I_{P-1} R_{P-1} \quad (5)$$

Finally, assuming steady-state sinusoidal currents, one can resolve equation 5 into real and imaginary parts in the following expressions:

$$I_P = I_{P_r} + jI_{P_i}$$

$$\phi = \phi_r + j\phi_i$$

which lead to

$$j\omega\phi_r - jI_{P-1} R_{P-1} + jI_P R_P = 0$$

$$-\omega\phi_i - I_{P-1} R_{P-1} + I_P R_P = 0$$

In these expressions, the ϕ s are determined by the currents, I_P . Therefore, these two equations constitute the determining expressions for unknown eddy currents.

APPENDIX B

ANALYSIS OF TEMPERATURE DISTRIBUTION FOR A SHEET-WOUND COIL

From the eddy current computation (Appendix A), the heat generation at the edge of the coil can be calculated. This heat generation is a function of the coil radius, r . The first step in the computation of the temperature distribution is to construct a polynomial representing the "edge heating" at any given value of r . This is done by referring to the results of the eddy current calculation. The eddy currents are computed for certain discrete radial positions, and heat generated at these discrete coil-edge positions is computed, then generalized over the entire coil edge.

The heat generation throughout the bulk of the winding, excepting only the edges, is a constant, $\dot{q}_0$. From the detailed edge current calculations (Section 4.1.3) it has been found that the heat generation at the edges can be expressed as a polynomial:

$$\dot{q}(r) = c_1 + c_2 r + c_3 r^2.$$

The nomenclature of Figure 5-1 will be used:

h = axial height of the conducting sheet

B = distance between cooling ducts

t_h = coefficient for heat transfer into the cooling duct

t_s = temperature of cooling fluid.

In general, at $r = 0$, there is a thin layer of insulation. Let d_e be the thickness of this layer and k_e its thermal conductance. The resistance to the flux of heat from the winding into the cooling fluid is given by the expression

$$R = \frac{1}{t_h} + \frac{d_e}{k_e}$$

$\dot{q}_0$ is the result of Joule heating. Therefore,

$$\begin{aligned}\dot{q}_0 &= \frac{I_u^2 \rho_o}{S} [1 + a_t (T - T_o)] \\ &= \frac{I_u^2 \rho_o}{S} [1 + a_t (T - T_s) + a_t (T_s - T_o)]\end{aligned}$$

where ρ_o = specific electrical resistance of conductor at a reference temperature T_o

a_t = temperature coefficient of specific resistance

S = space factor applicable to the winding volume between cooling ducts

Let $a_1 = \frac{I_u^2 \rho_o a_t}{S}$ and $a_2 = \frac{I_u^2 \rho_o}{S} [1 + a_t (T_s - T_o)]$

The temperature $T(r,z)$ is governed by the equation

$$k_r \frac{\delta^2 T}{\delta r^2} + k_z \frac{\delta^2 T}{\delta z^2} + \dot{q}_0 = 0 \quad (1)$$

Let $\theta = T - T_s$ and $\beta^2 = k_r/k_z$

$$\frac{\delta^2 \theta}{\delta z^2} + \frac{1}{\beta^2} \frac{\delta \theta}{\delta r^2} + \frac{A_1 \theta}{k_z} + \frac{A_2}{k_r} = 0 \quad (2)$$

The applicable boundary conditions are at:

$$\begin{aligned} z = 0 & \quad \theta_z = 0 \\ z = h/2 & \quad \theta_z = \dot{q}/k_z \\ r = 0 & \quad \theta_r = 0 \\ r = B/2 & \quad \theta_r = -\frac{\theta}{Rk_r} \end{aligned}$$

The differential equation in T can be solved by a transformation, viz.,

$$\theta = \psi(r,z) + \phi(r),$$

which leads to the two equations

$$\psi_{zz} + \frac{1}{\beta^2} \psi_{rr} + a_1 \frac{\psi}{K_z} = 0 \quad (3)$$

$$\phi_{rr} + \frac{a_1}{K_z} \beta^2 \phi + \frac{a_2}{K_z} \beta^2 = 0 \quad (4)$$

with boundary conditions at

$$\begin{aligned} z = 0 & \quad \psi_z = 0 \\ z = h/2 & \quad \psi_z = \dot{q}(y)/K_z \\ r = 0 & \quad \psi_r = 0 \\ r = B/2 & \quad \psi_r = -\psi/RK_r \\ r = 0 & \quad \phi_r = 0 \\ r = B/2 & \quad \phi_r = -\phi/RK_r \end{aligned}$$

As a simplification let

$$e_1^2 = \frac{a_1}{K_z} \times \frac{1}{\beta^2} = \frac{a_1}{K_r}$$

Then
$$\phi(r) = \frac{a_2}{a_1} \frac{\cos e_1 r}{\cos \eta - R K y e_1 \sin \eta}^{-1} \quad (5)$$

where
$$\eta = e_1 B/2$$

By the usual technique for separation of variables, let

$$\psi(r, z) = X(z)Y(r)$$

Then
$$X'' - e_2^2 X = 0$$

and
$$Y'' + Y(\beta^2) \left[\frac{a_1}{K_z} + e_2^2 \right] = 0,$$

with the solutions

$$X = A_3 \cosh e_2 Z + A_4 \sinh e_2 Z$$

$$Y = A_5 \cos e_2 r + A_6 \sin e_3 r$$

The eigenvalues e_2 and e_3 are related by

$$e_3^2 = \beta^2 \left[\frac{a_1}{K_z} + e_2^2 \right]$$

at $Z = 0, \quad \psi_z = 0, \quad \text{therefore } A_4 = 0$

$r = 0, \quad \psi_r = 0, \quad \text{therefore } A_6 = 0$

$$\psi_n(r, z) = A_n \cosh e_2 Z \cos e_3 r \quad (6)$$

at $r = B/2, \quad \psi_r = \psi/RK_r$.

Therefore

$$\gamma = e_3 B \text{ and } \delta = B/2RK_r$$

at $Z = h/2, \quad K_z \psi_z = C_1 + C_2 r + C_3 r^2$

Satisfaction of this boundary condition determines the constants $A_n (n > 6)$. Using Equation 6 and integrating over the region between ducts ($0 \leq r \leq B$), one finds

$$A_n K_z e_2 \sinh e_2 \frac{h}{2} \int_0^B \cos^2 e_3 r \, dr = \int_0^B (C_1 + C_2 r + C_3 r^2) \cos e_3 r \, dr.$$

The transcendental Equation 7 is solved to determine the e 's, whereupon the rest of the solution follows:

$$\theta = T - T_s$$

$$= \psi(r, z) + \phi(r)$$

$$\psi(r, z) = \sum_{n=1}^{\infty} B_n F_n(Z) \cos \frac{2\gamma r}{B}$$

$$F_n(Z) = \frac{\cosh(e_2 z)}{\sinh(e_2 h/2)}$$

$$B_n = \frac{4[C_1 \gamma^2 \sin \gamma + C_2 b(\gamma(\cos \gamma - 1) + \gamma^2 \sin \gamma) + C_3 b^2(2\gamma \cos \gamma + (\gamma^2 - z) \sin \gamma)]}{K_z e_2 \gamma^2 (2\gamma + \sin 2\gamma)}$$

The entire procedure has been reduced to a computer program which will print the temperatures for any desired coordinates r , z within the winding.

Appendix C

CONTROLLING THE FORMATION OF GAS BUBBLES IN THE COOLING LIQUID, INCLUDING LIQUID-SF₆ SEPARATION

The new design includes heat removal from the transformer windings by circulation of liquid coolant through sealed, self-contained cooling ducts. The coolant temperature increases as the liquid passes through the duct. Note, however, that the liquid does not change in phase. The liquid temperature then decreases as coolant circulates through a heat exchanger external to the transformer pressure vessel.

In the model transformers constructed for this work, both water and R-113* have been used as cooling liquids; however, only R-113 has been used in the recent tests. R-113 is only one of a number of possible cooling liquids. Since the coolant must also be a good electrical insulator, it appears that water could be used only while including provisions to maintain an adequately low electrical conductivity.

The formation of bubbles within the cooling liquid could be troublesome for either or both of the following reasons:

- Bubbles might interfere with liquid circulation or heat transfer. If a parallel arrangement is used and bubbles occur in one leg, the resulting maldistribution of flow rate is considerably enhanced (positive feedback occurs).
- Bubbles could be the loci of electrical discharges in the insulating portion of the coolant pipes (from the high voltage coil to ground).

Pipework for the cooling system passes through the transformer vessel, which contains SF₆ at elevated pressure. The cooling system itself operates with significant pressure differences associated with the circulation of the coolant. Substantial temperature changes also occur in the coolant as it circulates through the system. It is evident, therefore, that a number of possibilities exist for bubble formation, viz,

1. Boiling may occur as the liquid contains hot surfaces, or as it passes through lower pressure areas in the cooling system.
2. Leaks in piping, or the diffusion of gas through solid walls, may cause SF₆ gas to be introduced into the liquid stream.
3. Changes in temperature and pressure may cause the saturation concentration of gas solution in the cooling liquid to decrease. Gas released in such circumstances could then form bubbles.

In a liquid cooling system, provision must be made for expansion and contraction due to temperature changes caused by load cycling. The most common arrangement is an expansion chamber partly filled with liquid below a sealed volume of gas. When the liquid expands, the gas is compressed into a smaller volume (Figure C-1). This was the arrangement used for thermal tests with previous experimental coils.

*R-113 is the generic designation used for the refrigerant trichlorotrifluoroethane.

Figure C-1. Liquid cooling system with expansion tank

Within the expansion chamber, however, gas may enter the coolant at the gas-liquid interface. Consequently, even if the liquid does not boil, and even if no gas leaks into the coolant, bubble formation can still occur by the third method described above. In this regard, it should be noted that the solution of gases in liquids obeys Henry's law:

$$X_1 = F(T) P_1$$

where X_1 is the equilibrium concentration of gas within the liquid and P_1 is the partial pressure of the gas. The temperature dependence, $F(T)$, is a very strong inverse function. In the case of SF_6 in R-113, it has been determined¹ that

$$F(T) \approx T^{-5.3}$$

An alternative to the simple expansion chamber is the gas-liquid separator, invented by Jaster and Staub and developed by Jaster and Canada.²⁻⁴ Figure C-2 shows a cooling loop incorporating this technique. The separator chamber serves the functions of an expansion chamber and of a device for maintaining the level of dissolved gas in the cooling liquid below any given concentration, depending on the operating parameter of the separator.

The principle of the separator resembles that of a percolator. It connects to the system through inlet and outlet pipes (6 and 7 in Figure C-2). The inlet connects to the cooling loop at a place where coolant temperature is high, and the outlet from the separator fastens to the cooling loop at a place where the pressure is low. Conditions in the separator must be such that the equilibrium concentration of gas in the liquid contained in the separator is lower than at any other place in the system.

A small portion of the total coolant flow is drawn off at point 4 into the separator. The fluid in the separator is heated to yield a condition of low-equilibrium gas concentration. (In general, heating to the boiling point is

2. The very high solubility of SF_6 in R-113, coupled with the very strong dependence of the solubility of SF_6 in R-113 on temperature results in the possibility of significant bubble formation when changes in liquid temperature and/or pressure occur (the third method of bubble formation). Most fluorocarbon liquids behave similarly with SF_6 gas; however, other coolant liquids are available which may be better in this regard.

In summary, if a simple expansion tank proves inadequate, Jaster and Staub's gas-liquid separator appears to be a promising and comparatively simple method of controlling bubble formation in the liquid-cooled, compressed-gas-insulated power transformer.

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Appendix D

METHOD FOR CALCULATING THE RADIAL MECHANICAL FORCE EXPERIENCED BY TEST COIL DUE TO SHORT CIRCUIT CURRENTS

The method used for calculating the radial mechanical force in a sheet winding due to short circuit currents was outlined in Section 6.3.1. The method was used to calculate the maximum forces experienced by the test coil described in Section 6.4. Additional description of the method and the assumptions made are given below.

The calculation of mechanical forces in sheet wound coils is based on the fundamental principle that the force between two parallel current carrying wires is directly proportional to the product of the currents in each wire and inversely proportional to the distance apart of the wires. When the current is in the same direction, the force is attractive; when it is in the opposite direction, repulsive.

To apply this principle to sheet wound coils, the assumption was made that the forces between wires in circular windings are the same as would be obtained if the windings were cut and opened out into straight sections. This is approximately true because the forces between those sections of wire which are not parallel are weak due to the greater distances between one another.

Another simplification was that the load on any radial column of wire sections will be borne entirely by the part of the fiberglass cylinders directly inside and outside that radial column. In actuality, some of the forces acting on the outermost columns of wires would be borne by adjacent areas of the fiberglass cylinders. Thus, the stresses calculated for the outermost radial columns of the winding are somewhat larger than reality due to the load sharing which must take place.

Figure D-1 illustrates a transformer cross section with an inner and outer winding. The inner winding has a radial build of 13 cm; the outer winding, a radial build of 16 cm. The difference is due to the greater number of turns of thinner foil in the outer winding with a consequent greater number of insulating layers and a lower fraction of total volume occupied by conductor (lower space factor).

For the purpose of calculating forces at any place in the windings, the cross sections of the windings were arbitrarily divided into sections 1 cm on a side. There would then be $200 \times 13 = 2600$ such sections in the inner winding and $200 \times 16 = 3200$ sections in the outer winding. The currents in all such sections (except the end sections) in the inner winding were then taken to be equal to the average current density in the inner winding, due allowance being made for the fraction of each section occupied by conductor, carrying a known current, and the fraction occupied by insulation carrying no current. In the end sections, the currents were assumed to be enhanced by the amounts previously calculated (Section 4). The enhancement is greatest near the inner edge of the inner winding and the outer edge of the outer winding as shown in Figure D-1. The factor, F , by which the current is enhanced at the edges was taken to be

$$F = 2.213 + 0.248 x$$

Figure D-1. Radial forces in a 500 MVA, 500 kV(Y)/20 kV transformer design; 243 A/cm² inner winding; 294 A/cm² outer winding

for the inner winding, and

$$F = 1.475 + 0.219 x$$

for the outer winding, where x is the radial distance from the reactance gap.

To calculate the radial force on the supporting cylinder at the outer end of the inner winding, the force contributions of all 3200 sections of the outer winding were summed for each of the 13 sections at the outer end of the inner winding. For each of these pairs of interactions, the force = $K \cdot I_1 \cdot I_2 / d \cdot Y/d$. Here $K \cdot I_1 \cdot I_2 / d$ gives the direct force between the two sections, while the factor y/d converts the direct force into its radial component. I_1 and I_2 are current densities in each of the sections suitably corrected for the edge current enhancement. Note that the forces due to sections in the same winding are not included in the calculation; this is equivalent to saying that we are considering each winding as a cohesive unit. If forces from all sections of the inner winding were also included on the outer section of the inner winding there would be a slight pull on the outer edge of the inner winding outward diminishing to a small extent the drive of the inner winding inward by the forces from the outer winding. This is small and can be neglected due to symmetry.

In the same manner, it can be shown that the force on the outer edge of the outer winding due to all sections of the inner winding is made up of the same sum of pairs of $K \cdot I_1 \cdot I_2 / d \cdot y/d$ terms.

Using the above method, a computer calculation was made of the maximum radial force (at the coil edge) experienced by the mechanical test coil described in Section 6.4. At the highest current level to which the coil was tested (43,000 A peak off-set), a force of 268 N/cm² was sustained at the end of the outer cylinder.