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ORNL/TM-5574

Oak Ridge Tokamak Experimental Power Reactor Study—1976

Part 3 Magnet Systems

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- ① AIR CORE PRIMARY WINDING
- ② DECOUPLING WINDINGS
- ③ V.F. TRIM WINDINGS
- SHIELDING/ V.F. WINDINGS

- T. F. COILS (CONDUCTOR, BOBBIN, DEWAR)
- ⑥ SHIELD
- ⑦ BLANKET
- ⑧ VACUUM CONTAINMENT

OAK RIDGE EPR DESIGN

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Fusion Energy Division

OAK RIDGE TOKAMAK EXPERIMENTAL POWER REACTOR STUDY — 1976

Part 3

MAGNET SYSTEMS

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The other reports in the Oak Ridge Tokamak Experimental Power Reactor Study — 1976 are:

ORNL/TM-5572	Part 1. EPR Summary
ORNL/TM-5573	Part 2. Plasma Engineering
ORNL/TM-5575	Part 4. Nuclear Engineering
ORNL/TM-5576	Part 5. Engineering
ORNL/TM-5577	Part 6. Research, Development, and Demonstration Needs

FOREWORD

The Division of Magnetic Fusion Energy (DMFE) within the Energy Research and Development Administration (ERDA) identifies in the fusion development program for tokamak power reactors progressively larger confinement systems leading to a Demonstration Reactor Plant (DRP) by the end of this century. In this program, one step after the Tokamak Fusion Test Reactor (TFTR) and before the DRP is an Experimental Power Reactor (EPR), with operation in the mid-to-late 1980's.

The Experimental Power Reactor studies which have been performed at Oak Ridge National Laboratory (ORNL), Argonne National Laboratory (ANL), and General Atomic Company (GAC) the last two years have investigated the design and development aspects associated with an EPR producing significant power at a high duty cycle.

In FY 75, scoping studies were performed devoted primarily to determining functional and technological considerations associated with the design of an EPR. A reference concept was selected and during FY 76 this concept has been studied in more depth. From this more detailed examination of a particular concept, a more realistic assessment has been made of the system and discipline requirements for an EPR and the constraints imposed on the design by current theoretical understanding, existing technology, and near-term development.

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ABSTRACT

This part of the ORNL EPR composite report documents last year's design effort in the magnet systems. The discussion covers both the toroidal field (TF) coil system and the poloidal field (PF) coil system. Consideration of coil support structure, cryogenic requirements, and power supplies is included. Particular attention is also paid to the protection scheme of both TF and PF coils. The conductor design is based on forced-flow supercritical helium-cooled cable in conduits. A hybrid system is proposed for the TF coil system. It uses Nb_3Sn in the high field region for a designed maximum field of 11 T, and NbTi in the low field region. Conductor stability analysis in the forced-flow scheme is discussed. Coil connection options to obtain desirable current and voltage ratings in PF coils are described. The refined design in the magnet systems is more self-consistent in every respect.

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1. INTRODUCTION

1.1 Systems Considerations

This part of the composite report describes the magnet systems of the ORNL EPR. These systems include the toroidal field (TF) coils, the poloidal field (PF) coils, and the associated power supplies and cryogenics. Because of the huge forces (on the order of 100 MN) and energies (tens of gigajoules) involved, special attention has been paid to structural considerations and magnet protection schemes. The comprehensive work of the Superconducting Magnet Development Program¹ at ORNL, which is aimed directly at producing the technology needed for the EPR coils, has provided the basis from which much of this study has evolved.

The function of the TF coil system is to provide a steady toroidal field for plasma confinement and stability. Significant power output depends upon the achievement of long plasma burn times (on the order of 1 min) and the minimization of external power input; the use of superconducting magnets is essential for both these requirements. The higher the field, the better the confinement and stability of the plasma will be. Furthermore, the fusion power density is proportional to the fourth power of the field. Therefore, from the plasma engineering point of view, there is a great incentive to go to higher and higher fields. This incentive is counterbalanced, however, by the increasing difficulty of achieving higher fields. After consideration of the existing and developing superconductors, a maximum field of 11 T was chosen as the design reference point. This provides a field of about 5 T (depending on the major radius and the blanket/shield design) at the plasma axis. The required plasma size and blanket/shield thickness dictate a magnet bore of ≥ 7 m for the TF coil. This size and the non-axial symmetric field of a torus make the coil subject to large body forces. The coil is also subjected to pulsed fields and neutron radiation. Therefore, it is a serious undertaking to design a TF coil to operate under such severe environments. A decision on the number and size of the coils should include consideration of the field ripple at the plasma, provision of access for neutral beams, and blanket/shield maintenance.

The function of the PF coil system is to create, preheat, and develop the plasma current, and to maintain and stabilize the plasma column. The ohmic heating (OH) coils provide the necessary flux swing, and the equilibrium vertical field (VF) coils serve the stabilizing function (although the latter usually also supply a significant number of volt-seconds). An important design consideration is the position of the VF coils, i.e., whether they should be inside or outside the TF coils. Placing the VF coils outside the TF coils is an engineering choice. It makes assembly and maintenance jobs simple. On the other hand, placing the VF coils inside could save the ampere-turn and energy storage requirements of the PF coil system. With proper design, the VF coils can also reduce pulse fields on the TF coils and provide an option of shaping the plasma. The physics (and thus the related engineering) advantages were judged to be strong enough to favor the inside approach. By this scheme, a set of shield-vertical field (S-VF) coils is placed inside the TF coils; a counterwrap winding of decoupling coils is placed at the same location as the OH winding, and a set of trim-vertical field (T-VF) coils is used for the fine-tuning of the vertical fields. Because of the engineering difficulties mentioned above, the S-VF coils are room-temperature normal conductor winding. The rest of the VF coils and the OH coils are superconducting, since the power and refrigeration requirements for normal coils have been found to be prohibitively high.

1.2 Key Features of the September 1975 Reference Design

Scoping studies² for EPR with consideration of the basic factors and possible choices resulted in the recommendation of a September 1975 Reference Design.³ The attempt there was to achieve a workable design instead of an optimized one. It embodied advanced design concepts along with traditional

methods to obtain a system meeting the various requirements. Although development will be required before such a system can be reliably and economically built, the new ideas introduced deserve closer examination. The key features of the magnet systems may be listed as follows:

1. The TF coil has an "oval" shape which minimizes the bending moment.
2. The coil structure is of honeycomb type, a distributed solid frame that permits no accumulated forces on the conductor.
3. The conductor uses multifilamentary Nb_3Sn for a designed maximum field of 11 T.
4. NbTi was recommended for use at the low field regions.
5. The conductors are layer-wound to avoid joints in every pancake.
6. The conductor is in cable configuration with helium flowing in strand interstices.
7. Each conductor cable is housed in a stainless steel hollow conduit which serves as both the helium-tight jacket and the structure.
8. The conductor is cooled by forced-flow supercritical helium.
9. Cryostability is obtained from a combination of a moderate heat transfer coefficient and a large surface area in contact with helium.
10. A central column and sets of plate/beams are used for centering force and an out-of-plane loading structure.
11. A hybrid dewar system is used which encloses the inner legs in a common dewar.
12. The PF coil system gives special attention to reducing the stored magnetic energy and the pulsed field on the TF coils. AS-VF coil set closely coupled to the plasma current is a unique feature of this system.
13. The conductor cable for the PF coils (except the S-VF coils) uses NbTi in Cu and CuNi mixed matrix to reduce the ac coupling loss.
14. A fiber-reinforced insulating matrix is used for the structural member to avoid the eddy current loss.

The scheme of cryostabilizing a bundled cable in a hollow conduit by forced-flow supercritical helium is a new concept first developed at Francis Bitter National Magnet Laboratory. Computer analysis and preliminary experimental results³ are encouraging. Because of the large cooled surface area and a moderate heat transfer coefficient of the forced-flow helium, the conductor can recover from a normalcy by depositing the Joule heating into the helium. The single-phase helium will not form bubbles as a result of heat absorption. Therefore, behavior of the conductor can be better predicted. In addition to providing high fields, the use of Nb_3Sn has the advantage of a significantly larger temperature differential between the bath temperature and the critical temperature. For example, at a 8-T and 4.2-K bath, the temperature margin for NbTi (carrying current) is about 1 K, whereas for Nb_3Sn it is about 6 K. Hence a higher heat input stability limit can be obtained with Nb_3Sn (see Sect. 2.4). The main difficulty in utilizing Nb_3Sn (and all the other potential high field superconducting compounds) is its inherent brittleness. The use of cable conductor in a hollow conduit configuration may serve to remove this disadvantage. Since the conductor is not intimately bound to the structure, it does not undergo a large strain as the structure takes the forces. The cable conductors, in contrast to large monolithic conductors, can be supplied in very long lengths. This factor, plus the layer winding, keeps the amount of the undesirable superconductor splicing to a minimum.

1.3 Investigations of Critical Design Features

Many of the concepts in the magnet systems (and in other systems of the ORNL EPR reference design) are being employed for the first time in any conceptual tokamak design. The working guideline since the last reference design has been to evaluate the last design rather than to begin a new one.

Changes of the reference design are made only when judged to be necessary. Parametric studies, sensitivity checks, and detailed calculations have been made on several selected subsystems. Special attention has also been paid to self-consistency and to the possibility of extrapolating the systems to larger demonstration and commercial power reactors. The major modifications and refinements, which are discussed in more detail in later sections of this part, are listed as follows:

1. The coil shape of the TF coils has been carefully reexamined. It is found that with a minor modification of the radius of curvature, the oval shape is a viable optimum choice.
2. The winding scheme has been simplified by using L-shaped structural segments. Each segment encloses multiple conductor conduits. The number of welds has been greatly reduced, so that significant savings will be made in time and cost.
3. With several conductors running in parallel, the series current has been raised from 20 kA to 78 kA. This eases the protection problem in reducing the discharge voltage and also shortens the cooling path length.
4. The conductor cable composition has been optimized. Better understanding of the thermal behavior and better analytic tools enable us to optimize the conductor in the forced-flow scheme.
5. Stability of the force-cooled cable has been thoroughly analyzed. Quantitative stability limits for sudden heat input and gradual heat input can now be identified for a conductor at a specific temperature and field environment.
6. The pumping power requirement has been minimized with no sacrifice of stability. This is achieved by operating the helium flow at lower speeds for lower field layers while retaining the same stability limit.
7. Parametric studies of ac losses on the TF coils are performed. Because the pulse fields have components both perpendicular and parallel to the conductor, it is found that an optimum choice of filament twist pitch exists.
8. Extensive discussion on the position of the S-VF coils has concluded with a decision to leave them inside the TF coils.
9. The placement of the PF coils has been changed to further decrease the stored energy and the ampere-turns.
10. Several possible connecting schemes for the OH coils have been proposed after consideration of the requirements for current and voltage in these coils.
11. The ac losses in the PF coils are now calculated by taking into account the detailed behavior of the magnetic field in the coil inside the windings.
12. Specific cooling passages and power consumptions have been identified for the OH central solenoids.
13. A protection scheme which relies on external dump resistors and active switchings has been proposed and examined in detail. Satisfactory results on thermal excursions, terminal voltage, and mechanical loads can be achieved by connecting the TF coils in several parallel loops and isolating and discharging the quenched coils only. The protection problems of the PF coils are also examined.
14. A cold pump loop has been recommended for inclusion in the refrigeration cycle for the forced flow. Some experimental data on forced-flow cooling for bundled cable are available for incorporation in the revised estimation for refrigeration load.
15. Discussions and recommendations have been made on the energy storage and transfer scheme for the OH coils. However, a satisfactory scheme cannot be identified without more extensive studies.

In summary, the key features of the original reference design have all been judged viable concepts. The integrated honeycomb coil structure is still the backbone of the TF coils. It is worth noting that the modified structure can also be used for a pool-boiling or convection-flow cooled conductor. Detailed analysis of the forced-flow cooled cable has shown that the conductor can be optimized and that additional pumping power requirements will not be excessive. Forced-flow supercritical helium cooled conductor cable is still recommended for both the TF and PF coils. No insurmountable problems in the magnet systems or their protections were identified.

1.4 Outline of the Remaining Sections

The following sections discuss in detail the TF coils, structural considerations, the PF coils, coil protection, cryogenics, and power supplies. In each section, a report on the studies made of the system is followed by a discussion of the application of the studies to the present EPR design.

Section 2 discusses the TF coil design. A simplified winding scheme is described. Detailed description of the conductor optimization is followed by a discussion of the stability of the forced-flow supercritical helium cooled conductor cable. Consideration of ac losses and radiation damage effects is also included. Section 3 describes the various loadings on the TF coils and the structural selection. Section 4 discusses the problems in the PF coil system. The inductive coupling and the voltage and current requirements of the individual coils are discussed in detail. Section 5 surveys the protection problems of both the TF and PF coils. A grouped connection and switching scheme for TF coils is described. Section 6 summarizes the cryogenic requirements. Section 7 considers the power supplies.

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2. M. Roberts, *Oak Ridge Tokamak Experimental Power Reactor Study Scoping Report*, ORNL/TM-5038, Oak Ridge (to be published).
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2. TOROIDAL FIELD COIL DESIGN

2.1 Coil Shape

The idealized 1/R toroidal field dependence of a torus has been studied in an analysis¹ which concluded that a noncircular constant tension D-shape, with a uniform central force support cylinder, is the optimum shape for the TF coil. Consideration of the plasma, blanket/shield envelope, divertor locations, and the maintenance of blanket/shield has resulted in a modified D-shape² which is no higher than necessary. Our original choice of an oval shape has also taken into account the fabrication difficulties, the bending moments, the net centering forces, and the out-of-plane pulsed field loads. Studies have also been made to find the shape of bending stress minimized coil with discrete point supports³ as compared to the uniformly supported case.

A recent study by Moses and Young⁴ has refined the 1/R toroidal field variation and derived an analytic expression for magnetic forces which takes into account the finite number of coils and the finite coil cross section. This expression is used to evaluate the oval shape. It is found that with a small curvature modification, the oval shape has a constant tension over 75% of its circumference. By fixing the maximum height and the outermost point, a constant tension is found to bend inwards short of the innermost point. A smooth curve is drawn between the bending and innermost points to complete the coil. This completed coil is shown in Fig. 2.1 together with a D-shape which has the same horizontal diameter. The D-shape produces 60% higher centering force and 28% higher stored magnetic energy than the oval shape. Furthermore, the higher D-shaped coil will be subjected to a larger flipping torque by the out-of-plane pulsed field loads. It will also increase the ampere-turn requirements of the PF coils, because a taller OH solenoid is needed. Further consideration of the coil shape optimization in conjunction with support structures and system costs is given in the next section.

2.2 Coil Winding Scheme

Because of the importance of reliable operation of the TF coils, careful attention must be given to quality assurance in the design, procurement, fabrication, testing, and installation of the coils. The required equipments and technologies are either nonexistent or undemonstrated. A Superconducting Magnet Development Program at the Oak Ridge National Laboratory⁵ was initiated to assure the development and demonstration of reliable and economical superconducting magnets on a schedule which is compatible with the EPR schedule. Cooperation with industry in accomplishing these goals is emphasized in this program.

Winding facilities should allow accommodation of any of the more likely concepts of coil geometry, fabrication, and testing. The scope of the fabrication facility is determined by the need for the following features:

1. A conductor preparation area for the equipment required for preparing materials, including superconductor, structural reinforcement, and insulation, for winding into coils.
2. A subassembly fabrication area for the equipment required for the fabrication of vapor-cooled leads, liquid helium coolant channels, cryostats, superinsulation, and other miscellaneous features required to support coil assembly.
3. A coil fabrication and assembly area for the equipment required for coil winding and final assembly of coils.
4. A receiving and storage area for receipt and storage of materials.

ORNL-DWG 76-9886

	"D"	"OVAL"
VARIATION OF TENSION	CONSTANT ALL THE WAY	CONSTANT FROM 3.4 m OUT
AVERAGE TENSION	91×10^6 N	76×10^6 N
CENTERING FORCE	350×10^6 N / coil	220×10^6 N / coil
CIRCUMFERENCE	34 m	30 m
TOTAL ENERGY STORED	37×10^9 joules	29×10^9 joules

Fig. 2.1. Comparison of a constant tension D-shape to an oval shape for the TF coils. The dashed line shows the deviation of the oval shape from a constant tension D-shape. From this line out the oval shape has a constant tension.

The coil fabrication equipment will be needed for:

1. Coil bobbin rotation. Winding tables must operate smoothly and have suitable fixtures, clamps, and controls for firmly securing the windings during the winding operation.
2. Laser welding (or other type of welding) with sensitive positioning controls to permanently enclose the conductors.
3. Conductor insulating and cleaning.
4. Accurate conductor tensioning and recording.
5. Paying out conductor from a spool without adding to the conductor strain.
6. Reliable conductor splicing using a reasonably rapid procedure.
7. Handling coil components and completed coils within the fabrication facility.

Special transport equipment will be needed for moving coils to the EPR site.

Two manufacturing concerns are the orientation of coil axis during winding and the question of whether the bobbin should rotate or be stationary. The advantages of a horizontal axis in winding are a smaller floor space requirement and elimination of additional supporting structures and handling equipment to erect the completed coil. One obvious disadvantage, however, is the continually changing elevation of work position for craftsmen as an oval coil rotates. A vertical axis appears to have overriding advantages, namely: ready access to coil for work, inspections, and corrective operations; ease of arranging and maintaining auxiliary machines such as those for insulating and pre-tension devices; and simple support structures for the winding operation.

At first glance, it would appear that with very large and heavy coils it would be best to hold the bobbin stationary and move conductor spools around the bobbin. This does not look as attractive, however, if the moving equipment must be bulky and complex. Although the stationary bobbin option should be reviewed periodically, it is the current opinion that rotating the bobbin about a vertical axis will be preferred.

Conventionally, large coils are wound either in layers or in pancakes. Pancakes are often preferred since less complicated equipment is needed if windings spiral out radially on one plane. In pancake-wound water-cooled coils, the cooling path can be shortened, and the resistance of numerous splices does not cause concern. However, splices should be kept to a minimum in superconducting coils. Also, it is desirable to grade the conductor in such a way that less expensive NbTi can be used in low field regions, a technique which will also lower the ac losses. These objectives can best be obtained by layer winding.

Significant advantages can be obtained by winding several insulated conductors in parallel. Aside from magnetic, electrical, and cooling advantages (see Sects. 2.3 and 5.4), multiple conductors wound in parallel reduce winding time and the amount of welding. Winding four conductor conduits in parallel and enclosing them in structural segments, as shown in Fig. 2.2, can result in considerable savings in time and cost. As the conductors are laid into position, stainless steel segments having an L-shaped cross section are pressed over the conductors and laser-welded into place. Additional welds are made across each segment, joining it to the previous segment. Grooves across the surface of the L-shaped structural segment are provided for auxiliary cooling and precooling. Alternatively, the conductor conduit corners can be rounded if this poses no mechanical problems. The mutual inductances of the layer arrangement of the conductors are so small that the current imbalance between the conductor cables is negligible ($<0.1\%$). Laser welding is chosen for these reasons: the beam does not generate damaging heat to the insulation, it can be transmitted and focused from a distance, and it can be accurately aimed at a target. A fabricating pressure wheel holds the segments firmly in place while welding. Conductors must be sufficiently snug after welding to prevent movement when the coil is energized.

ORNL-DWG 76-9885

Fig. 2.2. Cross section of a partially wound TF coil.

The resulting honeycomb cross section⁶ is structurally robust and only a thin bobbin jacket is added to the completed coil as a secondary enclosure.

The actual winding is begun on a bobbin sleeve supported by a sturdy spindle, as shown in Fig. 2.3. The spindle, which is mounted on the winding table, must be strong enough to prevent the bobbin from collapsing due to winding tension and must also support the weight of the coil during winding and assembly. A continuously controlled tension device with recorder will be used with each conductor. It is expected to apply tension without moving the factory-applied insulation. There will be no internal splices since the conductors will be long enough to wind at least two layers. Splicing equipment will be available to make external splices and splices needed due to accidental damage. To prevent distortion of the conductor, factory spools and tension devices will be synchronized to maintain proper elevation with respect to the feeding point on the coil. The voltage breakdown condition of the conductors will be continuously monitored during winding. A completed coil cross section in relation to the coil shape and details of the conductor (see Sect. 2.3) is shown in Fig. 2.4.

2.3 Conductor Design

The honeycomb stainless steel coil structure is sized for an allowable strain of $<0.2\%$ in the conductor, rather than for the yield stress of the stainless steel itself. If the stainless steel alone is supporting the hoop stress, it will have an average strain of 0.15% . The cavities left for the conductor cable plus helium space require a current density of 6200 A/cm^2 to produce the maximum field at the winding of 11 T . It has been shown⁷ that a cryostabilized conductor with this current density can be achieved by a bundled cable cooled by forced-flow supercritical helium. The large cooling surface areas of the small-diameter strands, plus a sufficiently high pressure drop to obtain moderate heat transfer coefficients, allow high stability limits. Recently, L. Dresner⁸ found that it is possible to optimize these forced-flow cooled conductors by varying the copper-to-superconductor ratio and/or the metal-to-helium ratio. For a given stability level, the pressure drop and pumping power may be substantially reduced by optimization.

Whether or not the superconductor can recover after a sudden temperature rise depends on a complicated competition between heat transfer from the metal to the helium and production of Joule heat in the metal. Each of these depends in turn on the cooled perimeter of the metal, the heat transfer coefficient, the helium inventory, the metal fraction, the normal conductor fraction of the metal, the normal conductor resistivity, and the current density. Furthermore, recovery occurs when the temperature falls to the current-sharing threshold, which depends on the current, the bath temperature, and the magnetic fields. With a given set of these parameters, it is possible to calculate the limiting heat input per unit volume, ΔH_S , which can be deposited suddenly and uniformly over the entire length of the conductor and still allow it to recover the superconducting state. One way of optimizing the conductor can be achieved by varying the parameters to obtain a maximum ΔH_S with the minimum amount of required pumping power. First of all, the pumping power decreases as the 2.9th power of the cooled perimeter for a fixed product of heat transfer coefficient and cooled perimeter. Reduction of the strand diameter (at the cost of increasing the number of strands and the difficulty of manufacture) increases the cooled perimeter and reduces the required pumping power.

More interesting is the fact that ΔH_S has a maximum which is a function of the conductor fraction, F_{CO} , and the normal metal fraction of the conductor, F_n . For a given F_{CO} , if F_n is small, there is little copper and the Joule heating will be high when the superconductor is driven normal. Such a conductor can only recover from a small temperature excursion above the current-sharing threshold, T_{SC} . On the other hand, if F_n is large, there is little superconductor, and the current density in the superconductor will be very close to the critical current density. The current-sharing threshold is low and the heat to be removed before the conductor temperature drops to T_{SC} is high. If F_{CO} is small,

F-g. 2.3. Coil winding set-up.

Fig. 2.4. Shape, winding, and conductor configuration of a TF coil.

there is little metal, and either Joule heating will be high or T_{SC} will be low. Finally, if F_{CO} is large, the helium inventory is low and the heat it can absorb will be low. Therefore, some intermediate value of F_{CO} and F_n will give the optimum result. Other considerations are: smaller F_{CO} reduces conductor cost but endangers mechanical stability and fault protection; larger F_n not only reduces the amount of costly superconductor but also eases the protection problem.

Strands can be arranged in concentric layers inside the circular cavity of the conduit. They will occupy about 76% of the space (for four layers or more) and this fraction increases only slightly as the number of strands increases. If each strand further consists of a triplex (as shown in Fig. 2.4), the metal occupies 65% of the strand. Thus for a triplex strand configuration, F_{CO} is a fortunate 50%. If the strands are fluted,⁷ then, depending on the number of grooves and the groove depth, F_{CO} could vary from 40% to 76%.

To study the effect of changing the configuration of the strand and varying F_n for a given F_{CO} , we calculate ΔH_S for a 93-strand cable. The result is shown in Fig. 2.5. One sees that for each value of F_{CO} , ΔH_S goes through a maximum as a function of F_n . This maximum changes proportionally with the groove depth, and thus with the F_{CO} of the fluted strand. The highest maximum occurs at $F_{CO} \approx 0.56$, which corresponds to six grooves of a depth about 30% of the strand radius. Also shown is the result for the triplex strands. It is seen that for $F_n \leq 0.875$ the triplex has a higher stability limit than all the fluted cases. We have therefore chosen the triplex as the strand configuration. An additional advantage of this configuration is that one can make the triplex from two superconducting composite wires plus a normal metal (Cu or Al) wire to ease the manufacturing process. An aluminum wire would be a better choice for the stabilizer because of its higher conductivity at helium temperature and lower magneto-resistance. Since the technical compatibility of Al wire with superconducting wires has not been determined, our analysis was based on copper stabilizer.

A closer search for the optimum normal metal fraction of the conductor in a triplex cable is shown in Fig. 2.6. When the heat transfer coefficient, h , is varied, the magnitude of ΔH_S changes and moreover its maximum position shifts. For lower h , it would be better to have higher F_n so as to reduce the level of Joule heating. An optimum value of F_n for the Nb_3Sn conductor is found to be 87%. This gives a 20% Nb_3Sn filament in a CuSn matrix for the two superconducting wires, a reasonable value based on the present manufacturing technique. A similar optimization process results in a value for F_n of 85% for the NbTi conductor at low field regions.

2.4 Stability and Pumping Power

The stability of a forced-flow conductor depends not only on the configuration of the cable but also largely on how it is operated, particularly on the velocity of the helium flow and thus the amount of pumping power required. Since all the foreseeable and unforeseeable heat inputs that a magnet can receive and the stability limit for which to design are not known, it is advisable to discover the relationship between the stability limit and the pumping power required. Figure 2.7 shows the functional dependence of ΔH_S on the pumping power, P_p , at a given field for different temperatures; Fig. 2.8 shows the same dependence at a given temperature for different fields. A common feature of these curves is that ΔH_S varies in a certain limited range only, less than two orders of magnitude. That is to say, although ΔH_S increases monotonically as P_p increases, it saturates fairly quickly. Large variation of ΔH_S occurs only in a range of P_p less than an order of magnitude, above that range which corresponds to raising the conductor temperature to the superconducting critical temperature. Therefore, because the stability margin for a given conductor is limited, it is unwise to try pumping the helium at a speed faster than that giving the steeple ΔH_S gain.

Figure 2.7 also shows that lowering the bath temperature will generally increase the stability limit. However, this trend slows down below a certain level, which seems to indicate that as long as

Fig. 2.5. Sudden heat input stability limit for fluted and triplex strand configurations.

Fig. 2.6. Optimization of normal conductor fraction in a triplex cable for sudden heat input stability.

Fig. 2.7. Temperature variation of the sudden heat input limit dependence on the required pumping power.

Fig. 2.8. Field variation of the sudden heat input limit dependence on the required pumping power. Locations of some Reynold's numbers, R_e , and helium velocities, v , are indicated.

the temperature is low enough to achieve a comfortable margin (less than 80%) of the critical current density, it is not necessary to operate at even lower temperatures. Figure 2.8 shows similar trends as the magnetic field decreases. Inside the magnet winding, the field decreases from the inner layer to the outer layer. A reasonable operation is to set a ΔH_S value near the top of steep variation for the highest field. The layer-wound scheme then enables us to pump the helium at lower velocities on successive layers with a substantial saving of the pumping power. As shown in Fig. 2.8, for $\Delta H_S = 40 \text{ mJ/cm}^3$, the pumping power required for $B \leq 9 \text{ T}$ is less than 2 mW/m . This corresponds to a Reynold's number slightly less than 5000. In order to assure a turbulent flow and thus the correct heat transfer coefficient used in the analysis, it would be wise to use a pumping power of at least 2.5 mW/m . Similar analysis has been performed on the NbTi superconductor at 6.5 T and below. An average pumping power of 6.3 mW/m and an average helium flow velocity of 24 cm/sec are taken for further calculations.

Calculations also indicate that after a sudden heat input of the amount up to the limit discussed above, the conductor can recover to its superconducting state in tens of milliseconds. The sudden heat input stability limit discussed so far applies to that input due to flux jump and possible sudden conductor motion. The slower heat input, such as that from ac losses and neutron heating that allows the helium to reach an equilibrium with the conductor, belongs to another stability limit. This gradual heat input, ΔH_G , is given by the ΔH_S limit for P_p approaching infinity. This is the amount of heat that is required to raise the temperature of the helium plus the conductor to the current-sharing threshold of the superconductor. This stability limit reflects the characteristics of the individual conductor and does not depend on the pumping power. However, because of the long helium path length (390 m), the total heat received by the helium in its transit through the conduit depends on the helium flow velocity. It is thus recommended that the magnet be operated with a pumping power that gives near-maximum ΔH_S at high fields and that arranges the cooling paths to stay comfortably below ΔH_G .

2.5 AC Losses

The ac losses in the superconducting composite wire include hysteresis and coupling loss. They depend on the wire size, the superconductor filament size, the current densities, the twist pitch, and the field pulse. For the Nb_3Sn filament, the present manufacturing technique starting with a $10\text{-}\mu\text{m}$ Nb filament yields a $1\text{-}\mu\text{m}$ -thick Nb_3Sn ring after reaction. For NbTi filament, all indications are that a filament of $50 \mu\text{m}$ or less will not suffer from flux jumps even in moderate field pulses. The TF coils are subject to field pulses from the PF coils and the plasma current with field components both parallel and perpendicular to the conductors. The coupling loss due to a perpendicular pulse field⁹ decreases and that due to a parallel pulse field¹⁰ increases as the twist pitch is tightened. Therefore, depending on the relative magnitude of the field components, an optimum twist pitch can be expected. For $\dot{B}_\perp = \dot{B}_\parallel = 0.05 \text{ T/sec}$, the ac losses in the Nb_3Sn composite wire at 11 T and 6.0 K are shown in Fig. 2.9. The possibilities of higher effective transverse resistivity¹¹ and thus lower perpendicular coupling loss are not used in the calculations. The figure indicates a broad minimum around $\chi_w = 2 \pi r_w / l_t = 0.04$ (r_w = wire radius, l_t = twist pitch). This optimum χ_w also varies with the magnitude of field pulses and background fields. A twist pitch of 63 times the wire diameter (which is about six times looser than the tightest possible twist) is chosen for later calculation. Similar analysis results in an optimum twist pitch of about 32 times the wire diameter for the NbTi composite wire.

The total ac losses in the conductor conduit, P_T , including the eddy current losses in the copper wire and in the Al-alloy conduit, are plotted in Fig. 2.10 as functions of the pulse field, $\dot{B}$. The pulse fields indicated are for component values $\dot{B}_\perp = \dot{B}_\parallel$. The transport current, I_t , induced loss

Fig. 2.9. Hysteresis and coupling losses in the superconducting wire.

Fig. 2.10. Total ac losses in the conductor cable as functions of the pulse and background fields.

increment,⁹ is considered by including a factor $[1 + (\frac{I}{T})^2]$. At lower $\dot{B}$ the hysteresis loss and parallel coupling loss dominate, and P_T varies as $\dot{B}$. At higher $\dot{B}$ the perpendicular coupling dominates, and P_T varies as $\dot{B}^2$. (The eddy current losses are negligible for $\dot{B} \leq 1$ T/sec-) This is also illustrated by the substantial reduction in P_T when the Cu matrix of the NbTi composite wire is replaced by a CuNi matrix (with a matrix resistivity of 4×10^{-7} Ω -m, instead of 4×10^{-10} Ω -m for Cu at 6.5 T). The losses at lower fields for both the Nb₃Sn and the NbTi composite result from the competing effects of increasing current density and decreasing magneto-resistivity of Cu for the NbTi composite, thus increasing losses and decreasing transport current increment factor.

It is interesting to find the maximum $\dot{B}$ which the presently designed conductor can stand. As discussed in Sect. 2.4, the ac losses would most probably apply to the gradual heat input stability limit, ΔH_G ; as the helium flows through its conduit the heat input accumulates from successive shots. If the helium enters on one layer and exits from the next layer, the average transit time is about 27 min. If there is a 2-sec pulse every 100 sec, the number of heat pulses the helium receives before it comes out is about 16. Figures 2.8 and 2.10 indicate that the limiting $\dot{B}$ is a few tesla per second.

2.6 Consideration of Radiation Effects

In addition to the heat deposit in the coil volume mentioned before, the radiation leakage through blanket/shield may also change the properties of the magnet components. The damages on the insulator and structural material will be addressed in the next section. The damages on the conductor affect both the superconductor and the stabilizer (copper or aluminum).

A recent review by Guess et al.¹² has indicated that for fast-neutron fluences up to approximately 5×10^{18} n/cm² ($E > 0.1$ MeV), it appears that the problem of degradation of important superconducting parameters ($J_c(H)$, H_{c2} , T_c) by radiation damage to NbTi and Nb₃Sn is minimal. The radiation-induced increases in resistivity of copper and aluminum can be quite large, with an initial resistivity increase rate of $(8 + 1) \times 10^{-26}$ (Ω - cm)/(n/cm²) for copper and $(30 \pm 10) \times 10^{-26}$ (Ω - cm)/(n/cm²) for aluminum, for low-temperature fluences up to about 2×10^{18} n/cm². The damage rate then gradually saturates for even higher fluences. Thus, radiation damage in the stabilizer can be expected to have significant effects on the stability characteristics of the composite cable. Annealing of copper at 300 K can remove approximately 85% of the damage-induced resistivity in copper and about 100% for the case of aluminum. About 40-50% recovery has also been observed at 70-K annealing.

Based on the 800 MW(t) design point, the neutronics calculation described in Sect. 6 of ORNL/TM-5575 estimated a neutron fluence per year of about 1.6×10^{17} n/cm² ($E > 0.1$ MeV) at the front of the TF coil which attenuates to about 1.3×10^{15} n/cm² at the rear of the TF coil. Compared with the values listed above, degradation in superconducting parameters need not be a concern during the entire reactor life of about ten years. The resistivity increase will, however, reduce the sudden heat input stability limits, ΔH_S . The degree of reduction depends strongly on the operating point on ΔH_S vs P_p curves (see Fig. 2.7 and Fig. 2.8). When it is in the steep variation range, a large reduction can be expected. Otherwise, it would be insignificant. For example, the 20% increase in resistivity induced by radiation at the high field point, where operation is expected to be near the top of the steep variation range, can result in a reduction of more than 80% in ΔH_S . However, doubling the pumping power at this cooling channel is more than sufficient to regain the same stability margin. Thus, the procedure of increasing pumping power can be used as a temporary method of keeping a sudden heat input stability margin. An annealing to 77 K or, even better, to 300 K once a year should be sufficient to recover most of this degradation.

The gradual heat input stability limit, ΔH_G , on the other hand, will not be influenced by resistivity increments. This is because the gradual heat input does not raise the conductor temperature higher than the current-sharing threshold of the superconductor; no resistive heating is involved. Smaller gradual heat losses can also be expected, since the eddy current loss is inversely proportional to the resistivity.

2.7 Summary of the TF Coils

The analysis discussed above has led to a choice of a TF coil design. Each of the 20 TF coils has an oval shape with a vertical bore of 9.7 m and a horizontal bore of 7.4 m. The coil structure is a distributed stainless steel honeycomb constructed by welding L-shaped segments together. Four conductor conduits are layer-wound in parallel to form a 78-kA current path. There are 16 layers in a coil. The six inner layers use multifilamentary Nb_3Sn and the ten outer layers use $NbTi$ for the superconductor. The conductor cable is tightly housed in the Al-alloy conduit and cooled by forced-flow supercritical helium. There are 93 strands in a cable, and each strand consists of a triplex of two superconducting composite wires and one normal metal (Cu or Al) wire. The cooled perimeter is 70 cm with the interstitial helium occupying 48% of the cable space. The current density over the cable space is 6200 A/cm^2 , while that over the coil case is 1800 A/cm^2 . For an average helium flow velocity of 24 cm/sec and pumping power of 6.3 mW/m, the sudden heat input limit at 4 K is about 300 mJ/cm^3 of the cable conductor. Its gradual heat input limit is about 700 mJ/cm^3 . With the pulse field component $\dot{B}_\perp = \dot{B}_\parallel = 0.05 \text{ T/sec}$, the total average ac losses of the TF coils are about 1.3 kW, while the total pumping power required is about 1.6 kW. Table 2.1 lists the physical parameters of the TF coils.

Table 2.1. Physical parameters of the toroidal field coils

Item	Value or Description
<u>Overall</u>	
Major radius (m)	6.75
Maximum field at winding (T)	11
Field at plasma axis (T)	4.9
Number of coils	20
Coil shape	oval
Core bores	9.7 m vertical, 7.4 m horizontal
Total energy stored (GJ)	29
Total ampere-turns	126×10^6
<u>Composition</u>	
Winding dimension (cm)	65 x 52
Number of layers	16
Number of turns per layer	6.5 (4 conductors per turn)
Superconductor	Nb_3Sn for the six inner layers $NbTi$ for the ten outer layers
Conduit dimension (cm)	2.2 x 2.2 Al alloy
Cable diameter (cm)	2.0
Strand number	93

Table 2.1 (continued)

Item	Value or Description
Strand diameter (cm)	0.182
Strand configuration	two superconductive composite wires, one normal (Cu or Al) wire (triplex)
Wire diameter (cm)	0.0845
Conductor fraction	0.52
Superconductor fraction of the conductor	0.13 for Nb ₃ Sn, 0.15 for NbTi
Filament size	1- μ m ring in 10 μ m Nb for Nb ₃ Sn, 40 μ m for NbTi
<u>Current Densities</u>	
Conductor cable current (kA)	19.5
Critical current density (A/cm ²)	Nb ₃ Sn at 11 T, 6.0 K: 1.39×10^5 NbTi at 6.5 T, 4.0 K: 1.12×10^5
Overall current density (winding and coil case)	1.79×10^3
Current density over conductor conduit	4.03×10^3
Current density over cable space	6.21×10^3
Current density over cable conductor	11.9×10^3
<u>Cooling</u>	
Cooled perimeter (cm)	70
Hydraulic diameter (cm)	0.086
Coolant path	one layer inlet, next layer outlet; 8 paths (each consists of 4 parallel channels)
Average helium flow velocity (cm/sec)	24
Average helium transit time (min)	27
Average pumping power required (W/m)	6.3×10^{-3}
Average temperature rise due to pumping loss (K)	0.25
Average temperature rise due to ac losses inside conduit (K)	0.071
Inlet helium temperature (K)	4.0
Average steady-state heat transfer Coefficient (W/cm ² -K)	0.065

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3. STRUCTURAL CONSIDERATIONS

The structural considerations for the components producing the magnetic field involve a number of factors. The major points considered in reaching the current magnet designs are discussed in the following sections. Additional factors and some details are also presented in Sect. 2 of ORNL/TM 5576.

3.1 Environment and Loading for the Toroidal Field Coils

Production of the high magnetic fields needed for satisfactory operation of the EPR requires the TF coils to be superconducting, if the machine is to be economically feasible. To operate in the superconducting mode, these coils must be maintained in a cryogenic environment near 4 K. The current circulating in the coils interacts with the magnetic field produced to yield high body-force intensities in the coil conductors. The integral of these body forces over the coil volume produces net loads and moments on the coil which must be resisted by a support structure holding the coil in its intended position. The support structure must also provide the function of both thermal and electrical insulation of the coil. The magnitude of the distributed load, and the net loads and moments for the TF coils, are presented in Figs. 4.1-4.4 of ORNL/TM 5576 (and also in Sect. 5.5 below). The structural design must provide the necessary resistance to radiation damage in its choice of materials and their post-irradiation properties. The expected environment calls for 10^{18} neutrons/cm² over a ten-year period. The thermal contraction of the various components making up the coil and support structure must also be recognized in the design. Table 3.1 sums up the environmental and loading requirements, including effects not mentioned above.

Table 3.1. Environmental and load effects of structural importance

Type of Loading	Result
Thermal	Homogeneous and differential contraction between components. Temperature range: room temperature - 4 K Transient thermal stresses due to local instabilities or quenches of the magnet
Electromagnetic	Body-force loads in the toroidal field coils, both steady-state and transient
Mechanical	Gravitational Maintenance
Nuclear	Neutron irradiation, 10^{18} neutrons/cm ² in ten years
Natural hazard	Seismic

3.2 Interaction of Toroidal Field Coil Shape, Coil Structure, and Support Structure

In determining the TF coil design, there is usually a choice available as to the shape of the coil. A review of the data and decisions leading to the oval (eccentric ellipse) shape of the ORNL EPR TF coils is presented in this section.

A number of individuals have recognized that structures for which the stresses are uniform throughout can be designed with more structural efficiency, and have searched for optimal shapes for toroidal energy storage configurations. Research in this area has resulted in the conclusion that TF coils should be what has been called D-shaped.¹⁻³ Subsequent to this conclusion, a number of studies have been undertaken to investigate the appropriate shape for a TF coil. Factors involved in the studies have included the importance of a discrete number of coils, different coil support structure, the use of heterogeneous material, and the makeup of actual coils.⁴⁻⁹

In general, the problem of optimum coil shape may be visualized by considering the shape of a long, initially circular membrane (balloon) filled with a dense fluid under the influence of gravity. If the longitudinal axis of the balloon is horizontal and the balloon is placed on a sheet of well-lubricated mylar which is resting on a rigid table, the shape of the balloon is nearly the shape of the so-called D coil [see Fig. 3.1(a)]. If the mylar sheet is grasped at points A and B and the balloon is lifted from the table, as shown in Fig. 3.1(b), the balloon takes on a more nearly circular shape. If the balloon is supported by a single line at the outside, a tear drop shape results, as shown in Fig. 3.1(c). It should be noted that in all cases the surface of the balloon is under constant tension. Examples of coils supported in this manner are presented in Refs. 7 and 9.

A decision on optimum coil shape must include consideration of the effects of support structure cost, the cryogenic isolation concept, and manufacturing difficulties. When all of these were weighed, the shape chosen for the EPR reference design was that characterized as oval. A detailed structural analysis has been performed for this coil shape, considering the interaction of all coils, the stiffness of the support structure, and fault conditions. The results of this study are presented in ORNL/TM 5576, critique of current design. In general, it is felt that while a few structural changes may be made, the current design is structurally sound and represents an efficient concept to support all the magnet loads.

3.3 Support Structure for Out-of-plane Loads

The interaction of the TF coil current with fringe fields from PF coils results in body forces in the TF coils perpendicular to the plane of the TF coil itself. Several concepts have been examined of ways to resist these loads, including external torque frames, tension cables, and the finally accepted concept of allowing torsion of the central support column, bending and shear in the box-beam coil-to-coil support structure, and bending and shear in the outer legs of the TF coils to resist the load. The latter type of structure is also quite useful when a postulated fault is examined (e.g., when one coil is nonconductive).

3.4 Materials Selection

From the structural standpoint, the materials requirements for coil support are straightforward. Support must be provided to restrain the forces produced in the coil. The environmental effects (temperature, electric field, nuclear irradiation, etc.), the desire for long life, and the need for ready access to the blanket/shield region limit the choice in the following ways. The access problem requires the design of a stiff structure with a conflicting requirement for many access openings. A material with a high elastic modulus is necessary. If one rejects the advanced composite materials (boron/aluminum, graphite/epoxy, etc.), then the next choice from a cost standpoint is an iron alloy. In order to provide a high margin of safety, a high yield strength material is an appropriate choice. When the considerations of availability, fabricability, cost, and service life/neutron damage are considered, the preferred structural material is an austenitic stainless steel, e.g., types 304, 310,

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(a) BALLOON SUPPORTED BY TABLE

(b) BALLOON SUPPORTED BY MYLAR SHEET

(c) BALLOON SUPPORTED BY A SINGLE LINE

Fig. 3.1. The shape of a water-filled balloon under different support conditions.

or Armco 21-6-9. This allows the coil structural designer to ignore the interaction of the magnetic field or plasma with the structure, with the exception of eddy current effects. The cost advantage of mild steel components is still attractive, however, and study¹⁰ has shown that the use of a soft magnetic material as a coil casing in a tokamak does not produce excessive field ripple.

The requirement for electrical insulation in the TF coil involves consideration of the detailed winding configuration. Recent surveys^{11,12} tend to indicate that organic insulators may be mere borderline performers in the current EPR design. However, an increase in shielding or careful selection could permit their use. A more interesting choice of insulator calls for the use of inorganic insulators, such as metal oxides.

The requirements of low temperatures (4 K) and large forces ($\sim 2.5 \times 10^8$ N) have resulted in a decision to separate the functions of load-carrying and thermal isolation in the EPR, except in the case of gravity support. This means that the structural requirements regarding thermal insulation are limited to providing the necessary volume and support to allow the thermal insulation to remain in place.

3.5 General Discussion

Each structural consideration presented above had an effect on the structural aspects of the current design. A balance has been sought between the conflicting environmental factors, fabrication cost, and service life. The overall design will require development of specialized fabrication and inspection techniques, some material qualification tests, and further detailed analysis of the coil itself, as opposed to the current emphasis on the support structure. When such analysis is completed, it will then be appropriate to consider such aspects as thermal stress and mechanically related training effects.

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4. POLOIDAL FIELD COIL DESIGN

4.1 Introduction

This report is concerned with the superconducting magnets for the poloidal field (PF) coil system for EPR. In particular, the magnets for the ohmic heating system will be considered in detail. The trim-vertical field (T-VF) coils will be treated as a logical extension of the ohmic heating system, but the decoupling coils will not be examined in great detail, because the various design choices available may make them unnecessary.

The overall purpose of this section is not to present a detailed design and indicate a solution for every problem, but to identify problem areas and indicate where development work is needed.

4.2 General Problems

The poloidal coil system is a fundamental component of a tokamak. This system's magnets provide the primary flux swing that establishes the plasma current, the vertical fields that stabilize the plasma position in the toroidal field coils, and the magnetic fields that determine the shape of the plasma cross section.

The routine operation of this system is as follows. Starting with zero current, the coils are brought to their ready state. The shield-vertical field (S-VF) coils, decoupling coils, and T-VF coils are left with zero current, while the field in the ohmic heating (OH) coils is slowly increased. Then the plasma is ionized and the current in the ohmic heating system is changed, which changes the flux linked by the plasma, thus causing an induced voltage around the plasma. This voltage raises the plasma current, increases the electron energy, and further ionizes the plasma. As the plasma current rises, the major radius of the plasma tends to increase. In order to stabilize and shape the plasma, the current in the S-VF and decoupling windings is raised, and also, if necessary, the current in the T-VF coils is increased.

The most difficult coil in the PF coil system, in terms of superconductor requirements, is the central solenoid of the OH coils. Like all the coils in this set, it must be operated in a bipolar mode, i.e., the current must change direction in the windings during routine operation. The process of decreasing the current is fairly simple, since the critical temperature of the superconductor increases as the magnetic field decreases, and the field decreases as the current decreases. The reverse process is much more difficult, since the critical temperature will decrease as the current and field are increased again. Great care must be taken that the heat generated by the various loss mechanisms during this process does not cause the temperature of the conductor to increase to its critical temperature. As explained below, the heat produced is a function of the total magnetic field excursion and the rate at which the field changes. In order to estimate these losses, the magnet specifications must be derived.

The specifications of the PF coil system magnets are the maximum field in the magnet windings, the maximum rate of field change, the field change schedule, the radiation environment, and the space available. The magnet specifications are determined not only by the plasma physics involved, but also by the particular tokamak considered and by the design philosophy of the PF coil system. Reference designs for three EPR machines were published in 1975.¹⁻³ Each design used slightly different plasma physics scaling and design philosophy, and each produced a different set of PF coils. In this situation and at this early stage of EPR development, it seems prudent to assume the worst case for each component of the system. If any specification or combination of specifications is too severe the magnet may not be feasible. An infeasible magnet is one in which sufficient heat is produced, from all sources, to raise the temperature of the conductor and cause it to become resistive. The

limits of feasible magnet operation should be investigated to see if the system can be made to work.

After the feasibility is established, another investigation should be carried out to minimize capital and operating costs. This will be very important for power reactors, but less important for large physics experiments.

4.3 Uniqueness of the Poloidal Field Coil System

There are several large coil projects concerned with cycling energy in and out of a magnet: the beam magnets used in high energy accelerators, the METS program, and the superconductive energy storage program. Although each of these projects has something in common with the PF coil program, none of them has the same goals, and in order to develop the PF coils, they would have to change their goals considerably. Table 4.1 compares these projects to the poloidal magnet needs. The bases of comparison are the stored energy, the maximum rate of field change, whether or not these coils have to be concerned with radiation damage, and the maximum field in the coil windings. The stored energy is important because it helps to scale the overall size of the system and the approximate minimum amount of material necessary to support the coils. This comparison of amount of material does not strictly apply since each coil is made differently, particularly the larger-sized energy storage coils that will use bedrock for support. The maximum rate of change of field is important since it helps to determine the eddy current losses in the metallic structure of the coil and also helps to determine certain conductor parameters. The maximum field in the windings is also important because as the field increases, the critical current of the superconducting material decreases. Consequently, the higher the field the more material is needed to carry the same series current in the conductor with the same safety factor. The magnetic field swing of the central solenoid of the poloidal coil system goes from full field in one direction to full field in the opposite direction. Since the hysteresis loss depends on the total field swing, the maximum field will then help to determine the amount of superconducting material and the hysteresis loss per unit volume of superconducting material. The other magnet systems share this problem somewhat. Another problem is radiation damage and heating,

Table 4.1. Comparison of different magnet systems that use pulsed magnets

	Max. rate of change of field (T/sec)	Max. field in coil windings (T)	Magnetic energy stored (J)	Is radiation a concern?
PF coil system	3 - 7	3 - 7	$\sim 2 \times 10^9$	Yes
Beam magnets	1	5	$\sim 2 \times 10^4$	Yes
METS	$\sim 1000^a$	2	$\sim 3 \times 10^5^b$	No
Magnetic energy storage	--	3 - 5	$\sim 10^9 - 10^{12}$	No

^aThe 1000 T/sec is only employed in removing the energy from the coil. As explained in the text, this is easier than tokamak PF coil operation.

^bThis is the energy per magnet; the system has many magnets.

though this concerns only the beam line magnets for high energy physics and poloidal magnets. The PF coils are constructed as much as possible without metallic support materials to reduce eddy current losses due to the changing fields. This material will probably be a fiber-reinforced plastic. The mechanical and electrical properties of these materials are not well known at cryogenic temperatures. These properties and the effect of radiation must be measured. It is easy to see from Table 4.1 and the preceding discussion that the PF coils for large tokamaks are unique and that programs are needed to investigate their unique problems and to integrate the results with those obtained in the other magnet programs.

4.4 Description of the System

The poloidal field (PF) coil system for EPR consists of three sets of magnets; the shield-vertical field (S-VF) and decoupling coils, the trim-vertical field (T-VF) coils, and the ohmic heating system (Fig. 4.1). The S-VF coils are not superconducting and are located in the blanket/shield area; they are in series with superconducting coils that decouple them from the voltage induced by the ohmic heating system. The decoupling coils and the T-VF coils present special problems that will be discussed later. The ohmic heating system presents the greatest superconductor difficulties, since it sustains the highest fields at the largest rate of change of field, and has the largest field swing. These field changes cause heat to be produced by the superconductor and the other metal in the coil. The major problem associated with the design of the pulse coils is the reduction of heat produced and its removal from the magnet. If satisfactory methods can be found to design and construct these coils, then the other coils in the system can also be constructed.

The ohmic heating system consists of a number of coils. The central solenoid is in the bore of the tokamak and the outboard coils are outside the bore; some of the outboard coils surround the TF coils. The entire system is designed to produce a given amount of flux and to guide the flux lines around the TF coils. From a superconducting point of view the most difficult area is the central solenoid, because it makes the largest field in its windings and the largest rate of change of field. This coil is the principal subject of the following discussion.

The parameters and locations of the ohmic heating solenoids are presented in Table 4.2. The coils of the system are symmetrically placed about the midplane of the device and are numbered according to Fig. 4.2. The odd-numbered coils are above the midplane, and the even-numbered coils are below the midplane.

The central solenoid is divided into six segments of equal length (Table 4.2). Each segment contains 6.08 MA turns for a total of 36.5 MA turns in the solenoid. Each segment of the solenoid will be pancake-wound of 25.3-kA conductor, and will consist of 240 turns, with 10 turns radially and 24 pies (half pancakes) along its length. The length of conductor in one pancake is 213 m and there are 12 pancakes per section; therefore, the length of conductor is 2560 m per section. The recommended magnet will have two conductors wound in parallel. There will be no current sharing problem with this arrangement; since the parallel conductors will have the same self-inductance, the current will divide equally between them. This is recommended because of power supply limitations that will be discussed below.

The coil segments will be subdivided into 12 equal coolant paths so that a reasonable flow velocity and time to change the helium can be obtained. Table 4.3 illustrates the approximate flow condition required to remove the heat generated in the superconductor. This table represents average values; in a final design, segments 1 and 2 will use slightly different flows from segments 5 and 6. These results take into account the Joule-Thomson effect and the heat added due to superconductor losses (presented in Table 4.4).

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Fig. 4.1. Location of the PF coils.

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Fig. 4.2. Location and numbering of the OH coils.

Table 4.2. Overall dimensions of the ohmic heating solenoids

Coil number ^a	Inside radius (cm)	Outside radius (cm)	Half length (cm)	Distance from torus midplane to OH magnet midplane (cm)
1	1.500E+02	1.900E+02	5.333E+01	5.333E+01
2	1.500E+02	1.900E+02	5.333E+01	-5.333E+01
3	1.500E+02	1.900E+02	5.333E+01	1.600E+02
4	1.500E+02	1.900E+02	5.333E+01	-1.600E+02
5	1.500E+02	1.900E+02	5.333E+01	2.667E+02
6	1.500E+02	1.900E+02	5.333E+01	-2.667E+02
7	1.951E+02	2.369E+02	2.280E+01	3.480E+02
8	1.951E+02	2.369E+02	2.280E+01	-3.480E+02
9	2.231E+02	2.649E+02	2.280E+01	4.260E+02
10	2.231E+02	2.649E+02	2.280E+01	-4.260E+02
11	2.811E+02	3.229E+02	2.280E+01	5.260E+02
12	2.811E+02	3.229E+02	2.280E+01	-5.260E+02
13	4.031E+02	4.329E+02	1.610E+01	6.180E+02
14	4.031E+02	4.329E+02	1.610E+01	-6.180E+02
15	5.665E+02	5.875E+02	1.140E+01	6.430E+02
16	5.665E+02	5.875E+02	1.140E+01	-6.430E+02
17	7.816E+02	7.964E+02	8.070E+00	5.800E+02
18	7.816E+02	7.964E+02	8.070E+00	-5.800E+02
19	9.998E+02	1.010E+03	5.700E+00	4.250E+02
20	9.998E+02	1.010E+03	5.700E+00	-4.250E+02
21	1.155E+03	1.166E+03	5.700E+00	1.990E+02
22	1.155E+03	1.166E+03	5.700E+00	-1.990E+02

^aNumbering corresponds to Fig. 4.2.

Table 4.3. Average helium flow condition required to maintain the segments of the central solenoid, including pulse losses

Mass flow	3.3×10^{-2}	kg/sec
Velocity	.55	m/sec
Inlet temperature	3.0	K
Inlet pressure	7.0	atm
Outlet temperature	4.7	K
Outlet pressure	3.0	atm
Pump power per channel	106	W
Pump power for 12 channels	1270	W

Table 4.4. Superconductor losses in the ohmic heating system for time schedule in ORNL/TM-5042

	Hysteresis losses (J)	Coupling losses (J)	Eddy losses in current conduit (J)	Total losses for each system (J)	Power taken over 200 sec cycle (W)
Central solenoid (Coils 1-6, Fig. 4.2)	5.28×10^4	4.73×10^3	3.27×10^4	9.02×10^4	902
Outboard coils (7-22)	3.71×10^4	1.14×10^3	7.85×10^3	4.61×10^4	460
Total	8.99×10^4	5.87×10^3	4.05×10^4	1.36×10^5	1362

The conductor for this coil will be very similar to the conductor proposed in Ref. 1. The specifications are listed in Table 4.5 and a diagram of the overall conductor can be found in Fig. 4.3. The overall conductor is somewhat larger than the conductor presented in Ref. 1 because of design changes in the solenoid. The conductor used has not been optimized.

The question of using a higher current conductor must be examined carefully. There does not appear to be any fundamental limit on the current a pulsed superconductor can carry; however, certain problems do appear as the current increases. For example, consider the present design of the stainless steel conduit. The relation for the eddy current loss in a hollow tube is

$$p = \frac{\dot{B}^2 R^2}{\rho},$$

where p is W/unit volume of the material of the tube, $\dot{B}$ is the rate of change of magnetic field perpendicular to the axis of the tube, R is the inside radius of the tube, and ρ is the resistivity of the material of the tube.

The volume of material in the tube for a given magnet can be derived in the following way. The volume is the length of the conductor times the cross-sectional area of the material in the tube wall. The length of conductor, S , is

$$S = \frac{1}{I} \lambda j a_1^3 V^*,$$

where I is series current of the cable, j is the maximum current density in the tube averaged over the cable and helium in the tube, λ is the packing fraction for the conductor, a_1 is the inside radius of the coil, and V^* is the normalized winding volume. Note that λj is the average current density over the coil windings. The cross-sectional area of the tube is

$$A = 2\pi R t,$$

Table 4.5. Poloidal field coil conductor parameters

Symbol	Value	Units	Description
<u>NbTi</u>			
J_c	7×10^8 @ 4.2 K, 7 T	A/m ²	Critical current density
J	3.54×10^8	A/m ²	Operating current density
<u>Matrix</u>			
ρ_{Cu}	4×10^{-10} @ 7 T	Ω -m	Cu resistivity
ρ_{CuNi}	5×10^{-8} @ 7 T	Ω -m	CuNi resistivity
<u>Structure</u>			
ρ_{ss}	5×10^{-7} @ 7 T	Ω -m	Stainless steel resistivity
<u>Wire</u>			
I	18.26	A	Current
d_w	5.74×10^{-4}	m	Diameter
--	0.2 : 0.7 : 0.1		Composition NbTi : Cu : CuNi
d_f	4	μ m	Filament diameter
n_f	4106		Number of filaments
--	5.15×10^{-3}	m	Twist pitch
<u>Strand</u>			
d_s	3.9×10^{-3}	m	Diameter
n_w	37		Number of wires
<u>Other</u>			
n_s	37		Number of strands
--	3.48×10^{-2}	m	Bundle outer diameter
--	1:1		Metal/helium area
--	5×10^{-4}	m	SS tube wall thickness
--	3.5×10^{-2}	m	SS tube wall diameter

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**ROUND BUNDLE WITH 37 STRANDS ENCLOSED IN RECTANGULAR
CONDUIT SHOWING TRANSPOSITION OF STRANDS**

Fig. 4.3. Cross section of the PF coil conductor.

where A is the area of the annulus and t is the wall thickness. This relation holds good for $t \ll R$. The volume of material may therefore be derived as

$$V = SA = \frac{2\pi R t \lambda j a_1^3 V^*}{I}$$

But

$$I = \pi R^2 j ,$$

so that

$$V = \frac{2t\lambda a_1^3 V^*}{R}$$

The total power loss is

$$P = pV = 2 R t \lambda V^* B^2 a_1^3 \frac{1}{\rho}$$

One must be careful with the last equation, since the wall thickness is a function of the inside radius of the tube and the internal pressure of the supercritical helium. The exact function is unknown, but it will probably be linear; therefore, P will most likely vary as R^2 , and thus as I .

There are several possible solutions to this problem. One is to try to develop a non-permeable reinforced plastic to contain the helium. Another is to use a very thin layer of metal to make the leakproof container, and reinforce it for bursting strength with a fiber composite. Both methods will require some development.

But even if this problem is solved, there are others that will require some careful experimentation to find a solution. For example, as the cables get larger, does mechanical motion of the strands and wires become more of a problem? And what is the best configuration for the cable?

With these problems in mind it seems necessary to determine whether the system can be made to work with the 25-kA cable. Consider the following example in which only the ohmic heating coils are included. The purpose is to demonstrate that with this design there are several acceptable ways to connect the ohmic heating coils to reasonable power supplies. For this case, the cross sections of the outboard coils are assumed to be square. The size of the cross section is fixed by that coil's ampere-turn requirement from Table 4.2 and an assumed average current density of 1460 A/cm². The idea is to connect subsets of the ohmic heating coils in series, calculate the inductance matrix, and solve for the voltage required on each set of coils, so that each set has the same current. For this part of the calculation, each set of coils is assumed to be powered by a separate power supply. Consider the five coil sets listed in Table 4.6 and illustrated in Fig. 4.2. The inductance matrix for this connection is given in Table 4.7, and the required voltage on each set is given in Table 4.8. The voltages in Table 4.8 indicate that circuits 1 and 2, and circuits 4 and 5, can be operated in parallel. This procedure will double the peak current for the supplies operating the parallel circuits.

However, there are simpler solutions. For example, all the OH coils can be operated in series. This will require a power supply with a peak voltage of 68.9 V and a peak current of 50 kA. Another method would be to run all the OH coils in the upper half-plane in series and all OH coils in the lower half-plane in series. For this example, one supply at 34.4 kV and 100 kA would do the job if the two circuits were operated in parallel.

In the event that a 50-kA (or higher) current conductor can be developed, the circuits that parallel the coil sets will lower the voltage across the coil and increase the current from the power supplies. Homopolar generators would then become a more attractive option.

Table 4.6. List of coils in the five circuit scheme for driving the ohmic heating system
(magnets in each circuit are connected in series)

<u>Circuit number</u>	<u>Magnet number</u>
1	1,4
2	2,3
3	5,6
4	7,9,11,13,15,17,19,21
5	8,10,12,14,16,18,20,22

Table 4.7. Inductance matrix for the circuits listed in Table 4.6. The matrix elements are in henries. The i^{th} now corresponds to the inductances associated with i^{th} coil. Since the inducting matrix is symmetric, only the upper right is given.

.1193	.0806	.0414	.0136	.0229
	.1193	.0414	.0229	.0136
		.1032	.0359	.0360
			.1996	.0107
				.1196

Table 4.8. Voltages necessary to drive the five circuits

<u>Circuit number</u>	<u>Voltage (kV)</u>
1	13.9
2	13.9
3	12.9
4	14.3
5	14.3

For this example, three methods of operating the OH coil set are presented. The voltage to ground is high in the all-series case, but there is space available for high voltage headers so that the voltage does not appear prohibitive. The case involving two circuits in parallel requires 34.4 kV, which is better than the all-series case because of the lower voltage. The first case presented with five circuits appears to be needlessly complex unless one needs homopolar generators. In addition, each scheme presented will require less voltage if a higher current conductor is used. Given a longer time scale for EPR, these conductors may be possible.

The conductor pulse losses for the ohmic heating coils are presented in Table 4.4. These losses include hysteresis losses, coupling losses in the conductor, and eddy current losses in the stainless

steel conduit. The losses in Table 4.4 were calculated by integrating the loss relations (discussed later) over the volume of the coil, taking into account the detailed variation of the magnetic field in the coil windings. The individual coil shapes are the same as used in the previous example and are the total losses for all 22 coils. The waveform shape is the same as that used in Ref. 1.

The principal power loss mechanisms inherent in the design of the PF coil system are superconductor hysteresis losses, superconductor coupling losses, stabilizing material eddy current losses, and structural material eddy current losses. They can be approximated using the following assumptions.

1. For superconductors with small diameters, the hysteresis loss generated during a cycle from $B_{\min}$ to $B_{\max}$ and back to $B_{\min}$ can be expressed (in J/m³) as^{4,5}

$$\frac{W}{V} = \frac{\alpha_c}{2} d \ln \left(\frac{B_0 + B_{\max}}{B_0 + B_{\min}} \right) \quad (1)$$

where d is the filament diameter and α_c and B_0 are defined in the critical-state equation

$$J_c(B) = \frac{\alpha_c}{B_0 + B} \quad (2)$$

2. The coupling losses are due to magnetization currents flowing through the matrix. For a composite wire with radius r_0 , the surface heat flux is given by⁶

$$\frac{P}{S} = \frac{\dot{B}}{2\rho_m} r_0^3 \left(\frac{1}{x_0^2} + \frac{1}{4} \right) \text{ (in W/m}^2\text{)} \quad (3)$$

where x_0 is the normalized twist pitch ($2\pi r_0/\ell_p$), ℓ_p is the twist pitch length, and ρ_m is the matrix resistivity.

3. The model for the structural material eddy current loss is a cylindrical¹ conductor subjected to a time-varying magnetic field perpendicular to its axis. If the flux-penetration depth is assumed to be much longer than the cylinder size, the power dissipation can be conservatively estimated as

$$\frac{P}{V} = \frac{\dot{B}^2}{\rho_{ss}} R^2 \text{ (in W/m}^3\text{)} , \quad (4)$$

where R is the radius of the cylinder and ρ_{ss} is the resistivity of the structural material.

There are two other superconducting PF coil groups in the EPR design: the decoupling coils and the trim-vertical field (T-VF) coils. The purpose of the decoupling coils is to provide a back voltage on the non-superconducting shield (S-VF) coils. To do this the decoupling coils must be in series with the S-VF coils and have the same current and number of turns as the S-VF coils. Since the S-VF coils will carry the same ampere-turns as the plasma, and since the space available limits the number of S-VF coils to 16, the series current in the S-VF coils is very large (~.45 MA). Because of their function the decoupling coils will have to carry the same current. In the previous discussion of the ohmic heating system the current was limited to 25 kA and some possibility was presented for going higher, but there will be problems if the .45 MA cannot be attained in a reasonable time. There are three possibilities:

1. Use parallel conductors in the decoupling coils.
2. Make each S-VF coil multiturn.
3. Eliminate the decoupling coils.

The first possibility will require balancing the inductances in the parallel conductors or accepting a non-uniform current distribution; the problem has not been investigated enough to evaluate the consequences of this solution. By making each of the 16 magnets of the S-VF code several turns instead of one, the series current required may be reduced to an acceptable level, and the problems associated with the first solution avoided. So far no satisfactory method has been presented to make each S-VF coil multiturn. The third solution, elimination of the decoupling windings, will increase the cost of the power supply that drives the S-VF coils, but will eliminate the problems that must be solved in the first two cases. The problems associated with the decoupling windings are formidable and must be given a thorough examination before a solution is selected.

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5. PROTECTION SCHEMES

5.1 Introduction

The magnet systems of a Tokamak Experimental Power Reactor (EPR) store tens of gigajoules¹ of magnetic energy. It is vital to the successful operation of a reactor to be able to extract this tremendous amount of energy from the coils safely and reliably, so that neither the nuclear systems (blanket/shield) nor the magnets themselves are damaged.

When a superconducting toroidal field (TF) or poloidal field (PF) coil quenches (i.e., goes normal in an uncontrollable way), the stored energy will be converted into heat by Joule heating. A damaging temperature rise and the associated thermal strain could result if this energy is not extracted quickly enough. On the other hand, a large terminal voltage may be developed if one tries to discharge a coil too quickly. In the multi-coil system of a tokamak reactor, the latter problem can be reduced by parallel connection of the coils or further segmentation of a coil.²

The pulsing PF coils induce a tipping out-of-plane load on the TF coils under normal operating conditions. The mutual inductive couplings between TF coils, if only one or a few of them are discharged, also induce out-of-plane loads that tend to push the coils out away from the fault ones. Thus, discharging only some of the TF coils introduces additional mechanical problems. On the other hand, if one can limit the number of coils to be discharged, the downtime will be shorter. The plasma, too, should be considered when discharging the TF or PF coils. A sudden disruption of the plasma might deposit excess energy on the blanket first wall. The fewer the number of TF coils to be discharged, the smaller the chance that a disruption could occur. Therefore, careful consideration of a coil connection and discharging scheme is warranted.

Compromises in limiting the temperature excursion, induced terminal voltage, induced current, mechanical fault loads, and complexity of circuitry should be evaluated for a given system. In addition, the initiation of the protection switching relies on a good detection mechanism. A fault detection scheme based on loss voltage measurement³ is also described.

5.2 Fault Detection

A previous study⁴ has concluded that a passive self-protection scheme for a superconducting coil, such as a secondary inductive coil, a resistor permanently connected in parallel, or a high thermal conductivity insulator, suffers from low energy extraction efficiency or longer downtime after discharge. The most promising protection scheme (which is also the most common one at present) provides a standby external resistor that can be switched into the coil circuit for discharge. The success of this scheme depends heavily on a reliable detection of the fault condition in the magnet. It is possible that many non-electrical fault detection devices might be required to cover the whole winding region. They might suffer from slow response time or their sensitivities might be degraded by the strong magnetic field⁵ and/or very low temperature environment. The electrical measurements, on the other hand, are subject to interferences from the charging/discharging of the magnet itself or the pulsing of other magnets. These inductive signals usually overwhelm the fault signal. For example, a normal zone 1 m long in the TF coil produces a voltage of about 70 mV, while the inductive voltage due to a neighboring coil quench is as high as 180 V. The main problem is thus to compensate for the inductive signals. A simple loss voltage measurement scheme can accomplish this. The terminal voltage of a coil in a multiple-couple magnet system can be expressed by

$$V_i = I_i R_i + \frac{d\phi_i}{dt} + L_i \frac{dI_i}{dt} + \sum_{j \neq i} M_{ij} \frac{dI_j}{dt} ,$$

where $I_i R_i$ represents normal zone voltage, and $d\phi_i/dt$ represents voltage induced by the flux motion inside the superconductor. Thus the loss voltage to be measured is

$$V_i^{\text{loss}} = I_i R_i + \frac{d\phi_i}{dt}.$$

The self- and mutual-inductive voltages are proportional to dI_j/dt . These can be picked up by a Rogowski coil at the power supply bus bar (away from the magnets) of each coil or group of coils. Differential amplifiers on line with each pick-up coil make the M_{ij} compensations. A schematic diagram of this approach with two coupled magnets is shown in Fig. 5.1. Compensation down to 0.1% of the inductive voltage was demonstrated in a recent ac loss experiment.³ The compensation factors so obtained are independent of the charging/discharging rate of the coils and can be predetermined by charging one coil and monitoring the voltage in the second coil with all other coils in open circuit. It is desirable to automate this checking system in a tokamak reactor because of the large number of checks required. One might note further that the loss voltage induced by flux motion, $d\phi_i/dt$, appears before the normal-zone $I_i R_i$ voltage. Valuable information may already be in hand before a quench occurs.

5.3 Plasma Considerations and General Strategy

Before a shutdown of the magnet systems, it is prudent to consider what will happen in the plasma, blanket, and shield. In particular, the thermal loading on the first wall should not be taken lightly. One estimate⁶ indicates that for a plasma with a mean particle energy of 2 keV to deposit its thermal energy uniformly on the first wall in half a millisecond, the temperature rise of the surface exceeds 300°C. The magnetic energy of the 7.2-MA plasma current is about 15 times the 2-keV plasma thermal energy. An abnormal discharge of the plasma is most probably unbearable to the first wall. A sudden discharge of all the TF coils is likely to lead to an immediate quench of the plasma. Therefore, it is recommended that only the quenched TF coil be discharged and that all the other coils and plasma be kept (if possible) in operation.

If one of the PF magnets has quenched, it is then unlikely to hold the plasma. In this case, we propose to discharge first the plasma, then all the PF coils. The TF coils may remain in operation. To discharge the plasma, irrespective of which PF coil has quenched, the first step is to reconnect the decoupling coils and the S-VF coils into parallel circuits, as shown in Fig. 5.2. Then the S-VF coils can be discharged quickly (in 2 sec) by the external dump resistor (or by a power supply). The S-VF coils will now act as a discrete conducting shell to the plasma because of their very close mutual coupling, and the plasma current will be driven down together with the current in the S-VF coils. Minor changes in the T-VF and OH coils may be necessary to keep the plasma centered and to supplement the flux swing. After the plasma has been discharged, the remaining PF coils may be discharged.

5.4 Thermal and Electrical Considerations

The temperature excursion of a normal zone in a superconducting magnet depends on the current density, the magnetic field, and the heat transfer characteristics. The circulating helium, which is vital to the stability of the magnet, plays a minor role in protection. Even in the forced-flow scheme the helium flow velocity (approximately 0.25 m/sec) is still slower than the normal zone propagation velocity^{7, 8} (approximately 1 m/sec), i.e., the helium can be treated as essentially stationary. As the temperature of the normal zone goes up, the density and the available enthalpy of the helium go down rapidly. For a temperature rise of 100 K in the TF coil, the enthalpy

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Fig. 5.1. Fault detection by loss voltage measurement. In the equations shown, the left side represents the measurement; the right side represents the physical content. $f_i \propto L_i$, $f_{ij} \propto M_{ij}$.

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NORMAL OPERATION: AS SHOWN

SEGMENT DISCHARGE: (a) CLOSE S_1 AND S_2

(b) OPEN S_3 TO ISOLATE FROM POWER SUPPLY

(c) CLOSE S_4, S_5, S_6

(d) OPEN S_2 AND S_7

(e) OPEN S_5 AND/OR S_6 TO DISCHARGE

Fig. 5.2. Coil disengagement and discharge scheme.

contribution of helium is less than 10% of that of the stabilizing normal conductor. Because of the high current density involved (see Table 2.1), the temperature of a normal zone in a TF coil will rise to 300 K in about 30 sec, if the current is unchanged. It is thus advisable to be able to discharge the magnet in this time frame, if needed. An external dump resistor of 17 m Ω is required for each TF coil if all 20 coils are to be discharged in parallel. The temperature excursion inside the coil is then limited to 100 K with an associated thermal strain of $\lesssim 5 \times 10^{-4}$, and a terminal voltage of 1.3 kV will be developed across each coil. Reduction of this voltage can be achieved by segmenting the coil. For example, dividing a coil into two segments (as shown in Fig. 5.2) will reduce the required dump resistance and the resulting terminal voltage on each segment by half. Because of the magnetic coupling, if the quenched coil alone is discharged, the energy dissipated is less than 5% of the total stored energy. However, for the rest of the coils connected in parallel, the nearest neighbor can have an asymptotic induced current increase as high as 30%.⁴ This in turn leads to a large out-of-plane load, lateral displacement,⁹ and possible magnetoelastic instability.¹⁰ For the rest of the coils connected in series, induced voltages of the same order of magnitude as the quenched coil will be developed.⁴ The maximum voltage may occur at any point in the torus, depending on which coil is quenched, and this makes electrical protection more difficult. As a compromise between induced current and voltage, we propose to divide the 20 TF coils into four parallel groups. The symmetrically located coils in each group are connected in series and charged by a common power supply. If one coil quenches, it is isolated from the group it belongs to and discharged by its own dump resistor. This is necessary because even high currents will be induced in the nearest neighbor groups if all five coils in a group are discharged. This arrangement is shown in Fig. 5.3. With coil #1 quenched, the two groups with coils next to #1 could increase their current by 7% and the other group could increase by 2%, while the coils originally in series with #1 could decrease by 4%. The induced voltages in the undischarged coils are no more than ± 300 V. The discharge time constant for #1 is now 10 sec, and the temperature rise is about 50 K. Thus this active switching scheme produces manageable numbers.

The OH coils are designed to withstand high terminal voltage (up to 35 kV for the two-group option) for the charge-up phase. With a 7-m Ω dump resistor, they can be discharged in 30 sec with a terminal voltage of 1.3 kV. Similarly, with dump resistors of 0.05 m Ω for the decoupling coils and 0.1 m Ω for the T-VF coils, the terminal voltages are 24 V and 40 V, respectively. The temperature excursions are all limited to 100 K. The S-VF coil needs to be discharged more quickly (2 sec). Using four parallel segments with 6.4-m Ω dump resistors, the voltages can be limited to 700 V.

5.5 Mechanical Loading

The magnetic fields due to the normal and abnormal operating current distributions introduce mechanical loadings on each coil. For simplicity, we divide them into two categories: in-plane and out-of-plane loads. The in-plane hoop tensions on a TF coil under normal and fault conditions are shown in Fig. 5.4. The fault loads are on the coil nearest to a quenched coil due to the redistributed currents discussed in Sect. 5.4. The tensions are decreased slightly when the outer portion of the coil remains near constant tension. Figure 5.5 shows the out-of-plane loads under the same conditions. The normal operation loads are due to the pulsed PF coils and plasma current. These produce a torque which tends to flip the coil. The fault loads, on the other hand, are symmetric with respect to the midplane. They are acting in the direction of pushing the coils away from the quenched coil. Even with our distributed scheme, these fault loads are about one order of magnitude higher than the loads under normal conditions. The result of placing all coils in parallel is to increase fault loads by about 20%. Since the PF coils are expected to be discharged in groups, the induced loads on the

1. FOUR SYMMETRIC GROUPS

2. NORMAL OPERATION :

S_1 AND S_1' CLOSED ; S_2, S_2', S_3 AND S_3' OPEN

3. DISCHARGE :

(A) CLOSE S_2' AND S_3' , THEN OPEN S_1' FOR THE QUENCHED COIL

(B) OPEN S_3' TO DISCHARGE THE QUENCHED COIL THROUGH ITS R_D

(C) THE POWER SUPPLY CAN BE ISOLATED THROUGH S_1, S_2 , AND S_3 , IF DESIRED

Fig. 5.3. Protection circuitry for TF coils. Only one of the four groups is shown. The switching sequence for discharge is also indicated.

Fig. 5.4. In-plane mechanical loading for a TF coil under normal and fault conditions. The poloidal angle shown is calculated from the outermost point.

Fig. 5.5. Out-of-plane mechanical loading for a TF coil under normal and fault conditions. The normal load is antisymmetric, while the fault load is symmetric with respect to the midplane.

TF coil will not be worse than those of the normal operating conditions. Therefore, a lateral support structure designed for the TF coil in fault should be sufficient.

The loading on a PF coil depends on its location, current, and current direction. We shall use the first and the last T-VF coils to illustrate the problems. Assume the TF coil on the toroidal angle $\theta=0$ is at fault. The in-plane and out-of-plane loads on a T-VF coil with $R = 1.7$ m are shown in Figs. 5.6 and 5.7. This coil is very close to the OH solenoid and its current is in the opposite direction to that of the OH coils. Under normal conditions, its in-plane load is a constant compression and its out-of-plane load is a pure lifting force. When the TF coil at $\theta=0$ is quenched and the currents redistributed, the compressive loads decrease on the quenched coil side and increase on the far side up to $\pm 3\%$. This results in a bending moment and a net displacement force of 0.21×10^6 N toward the quenched coil. The out-of-plane loads change even more significantly (up to $\pm 25\%$), resulting in a torque which flips away from the quenched coil. For the T-VF coil with $R = 11.7$ m, the field components in the winding due to the PF coils and the TF coils are of the same order of magnitude. This results in a rather complex loading pattern under both normal and fault conditions. However, since the field strengths are weaker, the level of loading is small compared with those on the inner PF coils. The loads shown in Figs. 5.8 and 5.9 indicate local bending moments even under normal conditions. Again a net displacement force (now in the $\theta=\pi$ direction) and flipping torques are developed under fault conditions. Because of the unpredictable location of the TF coil at fault, the support structures of the PF coils should be designed with considerable allowance for the displacement and bending fault loads.

5.6 Summary

The protection problems of the magnet systems have been discussed. A simple yet very informative loss voltage measurement scheme serves to detect the fault signal of the coils. Protection of the blanket first wall is provided by controlled discharging of the plasma current. Active switching and external dump resistors are used to discharge the coils. The TF coils are divided into symmetrically located groups; any quenched coil in the group can be isolated and discharged. This scheme keeps temperature excursion, terminal voltage, and induced currents on adjacent coils all within easily manageable ranges. There is also significant reduction in out-of-plane loads. The PF coils, while posing no problem in containing the temperature excursion and terminal voltages, require a closer look into support of their complex fault mechanical loads.

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Fig. 5.6. In-plane mechanical loading for the PF coil at $R = 1.7$ m under normal and fault conditions. The toroidal angle shown is calculated from the quenched coil. Negative tension represents coil in compression.

Fig. 5.7. Out-of-plane mechanical loading for the PF coil at $R = 1.7$ m under normal and fault conditions. Positive forces point upward.

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Fig. 5.8. In-plane mechanical loading for the PF coil at $R = 11.7$ m under normal and fault conditions.

Fig. 5.9. Out-of-plane mechanical loading for the PF coil at $R = 11.7\text{ m}$ under normal and fault conditions.

6. CRYOGENICS

6.1 Introduction

A liquid helium refrigerator is utilized to establish and maintain the thermal environment of the superconducting magnet systems. The refrigeration system removes the energy deposited in the magnets from structural conduction heat leaks, thermal radiation leaks to the magnet, electrical eddy current and hysteretic heating resulting from the pulses of the PF coils, neutronic radiation absorption by the coils and the cryogenic cooled portions of the casing, and the mechanical energy added to the helium from the pumping power required to force the helium through the conductor.

6.2 Cooling Requirements

Toroidal Field Coils. The cooling requirements for the toroidal field (TF) coils are listed in Table 6.1. The refrigeration system is required to supply the cooling necessary to remove this energy from the magnets and maintain the temperature at levels below 5 K for NbTi superconductor and below about 6 K for the Nb₃Sn superconductor portions of the magnet.

The largest heat leak into the magnets comes from neutron and gamma rays from the plasma and blanket emission (see Sect. 6 of ORNL/TM 5575) and amounts to 800 W/coil, based on design output of 800 MW(t).

Table 6.1. Refrigeration requirements

Energy source	Total load (W)	
	Design	Operating
I. Toroidal field coils (average over duty cycle)		
1. Conduction through structure	1400	1400
2. Surface thermal radiation	500	500
3. Hysteresis and eddy losses	1300	1300
4. Neutron and gamma ray absorption	16,000	8000
5. Conductor pumping power (70% pump efficiency)	6300	6300
Total at 4 K and 5.0 atm	25,500	17,500
II. Ohmic heating coils (average over duty cycle)		
1. Conduction through support	1500	
2. Surface thermal radiation	1600	
3. Hysteresis and eddy losses	1360	
4. Neutron heating (design power level basis)	200	
5. Conductor pumping power (70% eff.)	8240	
Total at 3 K and 5.0 atm	12,900	
III. Electric leads		
1. 195 liters of LHe/lead pair (TF coils)	780	ℓ/hr
2. 60 liters of LHe/lead pair (PF coils)	120	ℓ/hr
Total	900	ℓ/hr

Most of the energy (76%) is deposited in the winding; 24% is deposited in the inner radius casing and bobbin, and about 0.1% in the outside casing. The heat conduction through the supports and the surface heat radiation are 95 W/coil total. The losses in the toroidal field coils due to pulsing of the PF coils are 650 W/coil and include the losses in the windings and casing. (This is discussed more fully in Sect. 2.5.)

The average mass flow of the helium in each of the 32 flow passages in a coil is 4.5 g/sec. The average enthalpy rise of the helium is thus 8.8 J/g during passage through the coil. The pressure drop is 2 atm. In order to maintain the single-phase flow, the outlet pressure is maintained above the critical helium pressure at 3.0 atm. Based on the physical properties of helium¹ and given an inlet temperature of 4 K, these flow conditions will maintain an outlet temperature below 6 K (~ 5.6 K), even taking into account the total enthalpy increase and the Joule-Thomson expansion effect associated with the pressure drop.

The pressure drop through the conductor is estimated on the basis of recent measurements made at the National Magnet Laboratory on similar conductors made of triplex wires wound into a cable.² The pumping power is computed from the dimension of the coil and the friction factor curves shown in Fig. 6.1. The equation for the pressure drop is

$$\Delta p = \frac{2fLG^2}{\rho D_H}$$

where f = friction coefficient

L = length of the flow channel

ρ = fluid density

D_H = hydraulic diameter of the flow channel

= $4 \times$ flow cross-sectional area/wetted perimeter

G = mass velocity of helium through conductor

Δp = pressure drop across conductor of length L .

The power requirement is the rate of doing work or the product of the volume flow rate and the pressure drop, divided by the pump efficiency. The pump efficiency is assumed to be 70%.

The appropriate friction factor is selected by calculating the dimensionless Reynold's number, Re , of the helium flowing in the conductor and selecting the friction factor (from Fig. 6.1) for the triplex wire. At $Re = 5000$, the triplex wire has a friction factor approximately 2.8 times that of a smooth tube. This is due primarily to the convolutions of the flow passages in the conduits. This factor has been utilized in calculating the pressure drop in the magnet coils.

The pumping power added to the helium must also be removed by the refrigeration system. The total pumping power is 315 W/coil, given 32 flow channels per coil with a flow of 145 g of helium per channel and a pressure drop of 2 atm. The total refrigeration load for the TF coils is 25.5 kW for the design point and 17.5 kW for the operating point. In addition, the vapor cooling of the electric leads requires about 780 ℓ /hr for the four lead pairs.

Ohmic Heating Coil. The ohmic heating coils consist of 6 central solenoidal coils and 16 outer coils as described in Sect. 4. The cooling channels run in the conduits of the magnet and are approximately 200 m in length. Helium flows in these channels at 55 cm/sec at an inlet temperature of 3 K. The heat loads in these coils are also listed in Table 6.1.

The hysteresis and eddy loss averaged over the 115-sec duty cycle is 1360 W, about 2/3 of which is generated in the solenoid coils and the remaining 1/3 in the outer coils.

Fig. 6.1. Pressure drop-flow relationship in a triplex cable.

The neutron heating is estimated at 200 W in the solenoid coils if shielding equivalent to the magnet thickness is added around the open area.

The losses due to heat conduction are estimated at 1500 W and are primarily due to the large bearing surface required to support the radial loads. Thermal radiation is estimated at 1600 W, 1200 from the orbital coils and 400 from the solenoid.

The neutron heating and the pulse heating are transported out of the magnets by the LHe pumped through the coils. The remainder of the energy deposited in the solenoid structure is removed by LHe flowing through other passages. Accordingly, the energy added to the conductor coolant in the solenoid (12 passages x 33 g LHe/sec) is 3.8 J/g LHe. The pressure drop at 55 cm/sec is 4 atm (see Fig. 6.1), the pump power is 1270 W at 70% efficiency, and the exit temperature is 4.7 K including the Joule-Thomson effect. The coolant is maintained below 5 K since the conductors are NbTi superconductor.

The flow and heating of liquid helium in the poloidal coils have been treated as averaged values over the duty cycle. In reality, the flow will probably be pulsed along with the magnet current to conserve energy by matching the flow rate to the pulse current and eddy heating. In order to achieve this, the helium flow circuit will probably be as shown in Fig. 6.2 where a pump will circulate the helium through a 3-K helium boiler connected to the vapor leads and the refrigerators. Such a system makes it possible for the refrigerator to operate at a steady state. The pumped helium can be pulsed to match the duty requirement of the magnet by varying the pump speed. This type of flow system was compared with a system where the refrigerator compressor supplied the pumping. The cold pump in a liquid helium closed loop was found to be more efficient if heat leaks could be eliminated.²

The orbital coils have a lower refrigeration requirement, 465 W from pulse heating, 1200 W from thermal conduction and thermal radiation, and a negligible amount from neutron and gamma heating. There are about 60 flow channels 300 m long. The flow required for an enthalpy rise of 3.8 J/g LHe, as in the solenoid, is 11 g/sec. Accordingly, the LHe pressure drop will be 0.9 atm and the pumping power will be 690 W.

The total pumping power for the helium in the ohmic coil is 8240 W. The total refrigeration load is estimated at 12.9 kW.

6.3 Summary

Refrigeration Loads for TF and PF Coils. As is listed in Table 6.1, the refrigerator is required to supply 25.5 kW of refrigeration at 4 K and 5.8 kW at 3 K. If the refrigeration at 4 K can be supplied for 500 W/W and at 3 K for 667 W/W, then the power requirement for the refrigeration is 21.3 MW. An additional 0.4 MW of compressor power will be required for cooling the electric leads. These power requirement estimates are conservative.

An examination of the heat loads of the two magnet systems shows that the neutronic heating levels of the TF coils are about one half the refrigeration load and should be reduced. This can be done by increasing the shielding of the magnets. The addition of at most 7.5 to 10 cm of shielding would bring the neutronic heat load down to levels compatible with the rest of the system.

Additional Comments Relating to Engineering. The magnet cooling systems for forced-flow conductors will require circulating pumps for liquid helium systems, and the development and testing of controls for a magnet cooling helium system. This will include pumps and flow measuring instruments along with the appropriate software. This task will be lengthy and must be done in a manner that assures the reliable cooling of the magnets under the widest range of anticipated system performance, including operational upsets and the consequent demands on the cooling system. Extensive development work on the cable conductor itself, particularly to gain information on the effects of non-uniform flow channels and the strand motion, is also required.

Fig. 6.2. Schematic of a refrigeration loop with a cold pump.

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7. POWER SUPPLIES, ENERGY STORAGE, AND TRANSFER

This section discusses the electrical power system for the magnets. Particular attention was paid to the ohmic heating system, which has heavy power demands. Because of the large energy and peak power involved, it is impractical to supply the OH coils off the electric utility lines.

7.1 Power Supply Requirements

As discussed in Sect. 5, the TF coils will be divided into four groups, each connected to a separate power supply. The solid state dc power supplies can be built in a modular fashion with additional flexibility. Assuming it takes about 4 hr to bring the current up to the operating point, the rating of each module will be 80 kA and 14 V. About 5 MW of power is required during the charge-up period; during routine operation a small amount of power is required to take care of the ohmic loss through the leads. In Sect. 5, it was pointed out that in case of emergency coil discharge, the induced voltage could be as high as 300 V. The power supply modules should also have the feature of protecting themselves against this transient phenomenon.

The S-VF coils and the decoupling coils are connected in series. The S-VF coils use water-cooled copper coils, and will require reactive power and real power. The T-VF coils require only reactive power. Both of these power supplies will be similar in function and can be built of solid state silicon controlled rectifier (SCR) bridges. Figure 7.1 shows the schematics of these two supplies. Both shielding and trim currents will be controlled by a feedback control circuit into which information from a plasma current sensing device is sent. The supplies are shown as giving the stored magnetic energy back to the power system at the end of each cycle; however, it may prove just as practical and economical to dump this energy instead.

The OH coils are required to produce a total flux change of about 132 V-sec in a 2-sec interval in order to provide the necessary plasma loop voltage. The revised OH coil system design described in Sect. 4.4 calls for 61 MA-turns and a total stored energy of approximately 1.7 GJ. It is currently planned to reverse-bias the ohmic heating coils before firing and to switch the primary current from full negative to near full positive in order to lower the amount, and thus the field level, in the OH coil to the operating range of NbTi. This means that the 1.7 GJ of energy must be transferred from the OH coils to some energy storage device and then transferred back again in a period of about 2 sec. Furthermore, because the principal objective is to provide the plasma with correct loop voltages, the energy storage and transfer system must be able to shape the voltage waveform for a very high value in the start-up interval and drop to a low value during the burntime.

Table 7.1 summarizes the overall electrical requirements for the various subsystems. There are separate listings for the resistive power which must be charged as a loss in a power balance and the reactive power which can be recovered at some efficiency over the operating cycle. Also included are the power requirements of the beam injection and various supporting facilities.

7.2 Consideration of OH Supply

The original reference design incorporated a homopolar generator (HPG) as the secondary energy storage element in the OH system. Study has shown, however, that the simple homopolar system envisioned cannot provide the voltage control capability required. At the beginning of a discharge a relatively large loop voltage is needed to enhance breakdown and to quickly establish the plasma current. Within a few seconds the voltage must fall by about two orders of magnitude in order to maintain the discharge current at the desired level. It is doubtful that an HPG can provide such a

Fig. 7.1. Power supplies for the S-VF and T-VF coils.

Table 7.1. Electrical power requirements for EPR

System	Power level		Required during these parts of the operating cycle
	Resistive (MW)	Reactive (MVAR)	
Ohmic heating	10	3500 peak	charge ^a
	5	--	burn
Shielding	60	81 peak	burn
Toroidal field		4.5	-- ^b
Vertical trim coils		80	burn
Beam injectors	200		burn (or start of burn: only for ignition ~5 sec)
Support functions:			
Building services	8		all
Refrigeration and compressors			
Experimental services			
Maintenance shops			
Fabrication Shops			
Cryogenic Plant	50		all
Vacuum system	<1		all
Pump house	3		all
Cooling tower	2		all

^aThe reactive power doesn't have to be supplied by the utility when an energy storage device is used.

^bUsed only during initial charge-up.

waveform shaping. To meet these requirements some sort of hybrid system is indicated, perhaps two or more homopolars for soaking up energy and a solid state supply for relatively fast voltage control.

Conventional motor-generator sets with flywheels and rectifiers have been used successfully in the past and are planned for PLT and TFTR. Although the present EPR design required a different start-up OH current scheme, a change in the control circuit might do the job.

Solid state converters plus some sort of energy storage device may also be very attractive. A superconducting inductor can be used as the energy storage device in the circuits shown in Fig. 7.2. No additional problems would be added to the storage inductor other than those involved in the OH coils themselves. The more complicated dc-ac-dc double conversion bridge in the Wisconsin scheme¹ (Fig. 7.2a) offers higher transfer efficiency than the Karlsruhe scheme² (Fig. 7.2b). Although another bridge would be required for the reversing current, no extensive development work is required for either of these solid state bridge elements. It has also been demonstrated in a small scale

(a)

(b)

Fig. 7.2. OH coils power supply with a superconductive inductor: (a) Wisconsin scheme, (b) Karlsruhe scheme.

experiment² of the Karlsruhe scheme that a transfer efficiency higher than 80% is obtainable.

7.3 Cost Considerations

The choice among the different power supplies should be based on a consideration of their technical adequacy and the total cost of the systems. The technical merits of each system have been discussed above in Sects. 7.1 and 7.2.

The total cost of each system includes development costs, capital costs, and operating costs. The development costs for the solid state power supplies will be less than the development costs for the homopolar generators, because large solid state units have been built and are already operating. The several kilovolt homopolar generators have yet to be developed. The capital cost estimates for the two systems, based on estimates by Westinghouse and the University of Texas given at the Tokamak Ohmic Heating Study Group Meeting in Austin, Texas on February 12, 1976, indicate that the homopolar generators will cost less than the solid state power supplies and their associated energy storage devices. The estimates for the homopolar generators are based on constant capacitance units, and, of course, do not include the costs of pulsed field magnets and the power supplies necessary to vary the capacitance. There is not enough information now to compare the operating costs of the two systems.

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Appendix

Brief Description of the EPR Reference Design

M. Roberts (ed.)

BRIEF DESCRIPTION OF THE EPR REFERENCE DESIGN

As a result of intensive evaluation of the blanket and magnet systems of the September 1975 Reference Design, along with a continuing examination of the plasma engineering basis, an updated Reference Design has been generated. Although significant improvements have been made in the key systems, this updated design does not represent a full iteration of the design; in particular, there is no change in plasma shape or impurity control, both of which would be changed in a new Reference Design. A full description of the Reference Design is contained in ORNL/TM-5576.

1. Summary

The EPR is an ignited, D-T burning, power-producing, air core tokamak with superconducting toroidal field (TF) and, in large measure, poloidal field coils. The duty cycle is 87%, with a burn time of 100 sec. The machine has a major radius of 6.75 m, and a circular plasma radius of 2.25 m (aspect ratio of 3.0). A blanket is provided to transfer a nominal 410 MW(t) from the burning plasma to a steam power cycle. For design purposes, the nuclear aspects of the EPR are based on a neutron wall loading of $\sim 1 \text{ MW/m}^2$, which is equivalent to a power output of 800 MW(t). Operation at this power level is considered an optimistic upper limit. A neutral beam system is provided to inject 50 MW of power into the plasma to achieve ignition. Figures 1 and 2 illustrate the elevation and plan views, respectively. Figure 3 presents a composite chart of each of the key preparatory steps before operation and the key events during an operating cycle. An initial plant layout is shown in Fig. 4, identifying 20 facility areas necessary for the reactor and balance of plant.

2. Key Systems

2.1 Toroidal Field System

The toroidal field of the EPR is generated by 20 superconducting coils. These coils are arranged on a stainless steel structure to form

a torus, with the noses of the coils contacting a cylindrical center bucking column. The coils have an oval shape with a minor (horizontal) bore diameter of 7.4 m and a major (vertical) bore diameter of 9.7 m. These coils produce a maximum field at the winding of 11 T and an on-axis field of 5.0 T at the plasma. In the high field area the conducting elements are composite wires composed of superconducting Nb_3Sn filaments in a Cu-Sn matrix; in the lower field portion of the coil ($<6.5 \text{ T}$) superconducting NbTi filaments in a copper matrix are used. The conductor coolant is supercritical liquid helium used in a forced-flow mode.

2.2 Blanket and Shield System

The blanket and shield are located inside the toroidal field coils. The blanket is composed of 60 circular wedge-shaped segments which, when assembled, make up the torus. Each segment is further divided into 12 modules which constitute the basic building blocks of the blanket (see Fig. 5). The first wall of the blanket, which intercepts the non-nuclear energy, is cooled with low-pressure water. The liquid metal absorber portion of the blanket receives the high-energy neutrons from the plasma and is cooled by high-pressure helium.

The shield is located outside the blanket; it consists of 20 segments composed of stainless steel and lead. The shield is cooled by borated water and forms a thermal barrier between the high-temperature blanket and the cryogenic TF coils. Neutron flux coming through the blanket is attenuated by a factor of 2×10^3 . The shield segments serve as the mechanical support for the blanket, and these segments themselves rest on tripod support columns.

2.3 Poloidal Field System

The poloidal magnetic system is composed of the ohmic heating (OH) coil, the shield-vertical field (S-VF) coil, the decoupling coil, and the trim-vertical field (T-VF) coil. A major portion

Fig. 1. EPR elevation view.

① AIR CORE PRIMARY WINDING	⑤ T.F. COILS (CONDUCTOR, BOBBIN, DEWAR)
② DECOUPLING WINDINGS	⑥ SHIELD
③ V.F. TRIM WINDINGS	⑦ BLANKET
④ SHIELDING/V.F. WINDINGS	⑧ VACUUM CONTAINMENT

Fig. 2. EPR plan view.

Fig. 3. EPR operating chart.

of the ohmic heating and decoupling windings are located inside the center bucking column of the structure. The remainder of these windings, and all of the trim-vertical field windings, are located outside the TF coils, and their mounting structure connects to the TF coils.

The OH coil serves to generate the toroidal electromagnetic driving force on the plasma. With the expected plasma impedance, a flux change of 130 W in the central solenoid results in a 7.2-MA plasma current. The coil current is driven from fully positive to fully negative values. The S-VF coil delimits the magnetic fields from the plasma current, thus shielding the TF coils. The S-VF coil also produces the major portion of the vertical field maintaining plasma column equilibrium. The decoupling coil, closely coupled to the OH coil, is connected in series

opposition with the S-VF coil to cancel the electromagnetic force generated by the OH coil in the S-VF circuit. The T-VF coil provides an auxiliary vertical field supplementing the S-VF coil. Table 1 presents some key distinguishing features of these four related coil systems.

2.4 Beam, Vacuum, and Tritium Systems

The neutral beam injection system deposits 50 MW of power into the plasma at an energy level of 200 keV for 5 sec each cycle, to contribute to the start-up heating of the plasma and to raise the plasma temperature to the ignition level. Six beam lines, with two sources per line, penetrate the plasma through six 40-cm-diameter apertures. Each beam line includes a negative ion source, a neutralizer region, a

Fig. 4. EPR plant layout.

Table 1. Key features of the poloidal field system

<u>Coil</u>	<u>Conductor Type</u>	<u>Number of Windings</u>	<u>Location Key on Fig. 1</u>	<u>Sense Relative to Plasma Current</u>
OH	superconducting	1220	1	opposite
S-VF	copper	16	4	opposite
Decoupling	superconducting	16	2	same
T-VF	superconducting	16	3	not applicable (creates vertical field only)

Fig. 5. Schematic of blanket construction.

bending magnet to deflect ions which are not neutralized, a vacuum system, and a drift tube to connect the beam line to the torus.

The primary vacuum system penetrates the torus through two vacuum ports located between each TF coil; it is a cryosorption system in which helium is used to maintain the cryosorption panel at 4.2 K. The forty 1-m-diameter cryopanel provide a total net pumping speed of 1,100,000 liters/sec. The roughing vacuum system, which is used for initial pumpdown of the torus, consists of three 5000-cfm Roots Blowers coupled to three 500-cfm mechanical pumps.

A tritium handling system used to process approximately 2 kg of tritium per day is provided. The tritium handling system includes equipment to (1) evacuate the plasma vessel and the neutral beam injectors and maintain a vacuum there, (2) purify and recycle tritium and deuterium for the plasma fuel cycle, (3) recover tritium from experimental breeding modules, and (4) provide tritium containment and atmospheric cleanup.

3. Key Parameters

A tabulation of overall system parameters and their values for the EPR is shown in Table 2.

Table 2. Overall system parameters for the EPR

A. General Characteristics1. Requirements

	<u>Symbol</u>	<u>Value</u>
Device lifetime	t_L	10 years
Duty cycle	dc	87%
Availability - normal	--	80%
Thermonuclear power (ignition)	P_o	410 MW

2. Other Features

Plasma radius	a	2.25 m
Major radius	R_o	6.75 m
Aspect ratio	A	3.0
Plasma edge to winding distance	Δ	1.40 m
Plasma volume	V	675 m ³
Overall height	H	15 m
Overall diameter	D	23.3 m

B. Plasma Parameters

Burn time	t_B	100 sec
Current	I_p	7.2 MA
Confinement measure	$N_e \tau_E$	2.7×10^{14} sec cm ⁻³
Plasma temperature		
Ion temperature	T_{ion}	12.2 keV
Electron temperature	T_e	13 keV
Beta poloidal	β_p	1.9
Safety factor	q	2.5
Electron density	N_e	7.4×10^{13} cm ⁻³
Fuel ion density	N_i	7.0×10^{13} cm ⁻³
Impurity level	Z_{eff}	1.34
Injection power into plasma	P_b	50 MW
Injected deuteron energy	E_b	200 keV
Particle confinement time	τ_p	9.6 sec
Energy confinement time	τ_E	3.7 sec

C. Electromagnetic Parameters

Maximum toroidal field	B_{max}	11 T
Toroidal field on axis	B_T	4.8 T
Number of toroidal coils	N_C	20
Magnetic field ripple at plasma edge	ΔB	2.2%
Conductor configuration	--	composite cable in a square aluminum conduit
Superconductor		
in the high field region	--	Nb ₃ Sn in Cu-Sn matrix conductor
in the moderate and low field region	--	NbTi in Cu matrix conductor

Table 2. (cont.)

	<u>Symbol</u>	<u>Value</u>
Average poloidal field at plasma edge	B_p	0.64 T
Poloidal field coil core type	--	air
Poloidal coil volt-seconds	VS	165 Wb
D. <u>Thermal</u>		
Maximum power rating of blanket	P_R	800 MW
Blanket coolant	--	helium
Outlet temperature of blanket coolant	T_{out}	370°C
Blanket coolant pressure	P_{He}	70 atm
Shield coolant	--	borated water
Coil coolant (superconducting)	--	helium
E.. <u>Nuclear Parameters</u>		
Neutron wall loading (operating point)	P_w	0.55 MW/m ²
Neutron wall loading (design point)	P_w	1.07 MW/m ²
Neutron flux on first wall (operating point)	ϕ_w	$2.4 \times 10^{13} \text{ n/cm}^2/\text{sec}$
Neutron flux on first wall (design point)	ϕ_w	$4.7 \times 10^{13} \text{ n/cm}^2/\text{sec}$
Tritium breeding ratio (in one experimental module)	BR	1.15

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