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MASTER**RESISTIVE TOROIDAL-FIELD COILS
FOR TOKAMAK REACTORS**

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ABSTRACT

Water-cooled resistive tokamak toroidal-field (TF) coils fabricated of copper or aluminum can be made readily demountable, thereby ensuring the maintainability of internal poloidal field coils, inboard blanket modules, and the components of a poloidal magnetic divertor. Resistive coils are unaffected by the pulsed fields of a tokamak, require less shielding against neutron irradiation than do superconducting coils, and have simple refrigeration systems. The demountable segments can be built in factories and transported disassembled to the reactor site. To achieve a satisfactory plasma pressure for various reactor applications, the magnetic field can be sufficiently low so that steady-state operation of resistive coils is quite feasible.

This paper analyzes the optimization of the geometry of resistive TF coils of rectangular bore for tokamak fusion test reactors and practical neutron generators. In examining the trade-offs between geometric parameters and magnetic field for reactors giving a specified neutron wall loading, either the resistive power loss or the lifetime coil cost can be minimized. Aspects of cooling, magnetic stress, and construction are addressed for several reference designs. Bending moment distributions in closed form have been derived for rectangular coils on the basis of the theory of rigid frames. Candidate methods of fabrication and of implementing demountable joints are summarized.

TABLE OF CONTENTS

	<u>Page</u>
1. INTRODUCTION	1
1.1 Disadvantages of Superconducting TF Coils	1
1.2 Motivation for Resistive TF Coils	3
1.3 Long-Term Prospects for Resistive Coils	4
1.4 Objectives of the Present Investigation	5
2. TOKAMAK MODEL	7
2.1 Reactor Geometry	7
2.2 Characteristic Parameters for the TF Coils	8
3. REACTOR PLASMA CONSIDERATIONS	12
4. OPTIMIZATION OF COIL PARAMETERS	15
4.1 Figures-of-Merit	15
4.2 Optimization for Neutron Wall Loading	16
4.3 Variation of Power Loss with Coil Mass	21
4.4 Minimum Lifetime Cost	23
4.5 Normalized Neutron Wall Loading Efficiency	24
5. STRESS CONSIDERATIONS	25
6. THERMAL CONSIDERATIONS	30
7. DESIGN OF CONDUCTORS AND JOINTS	33
7.1 Fabrication Considerations	33
7.2 Materials	34
7.3 Options for Joints	36
8. REFERENCE DESIGNS FOR REACTORS EMPHASIZING NEUTRON APPLICATIONS	39
8.1 External OH Coils	39
8.2 Internal OH Coils	41
9. CONSIDERATIONS FOR "PURE FUSION" POWER REACTORS	43
10. CONCLUSIONS	44
REFERENCES	53

1. INTRODUCTION

Practical tokamak fusion reactors must be characterized by long-pulse, high duty-factor operation with steady-state toroidal-field (TF) coils. The usual reasons cited against the use of normal-conducting (resistive) TF coils are excessive power dissipation and the consequent inability to operate in the steady state. However, at the values of $\langle \beta \rangle =$ (plasma pressure/magnetic-field pressure) now thought to be achievable in tokamaks (viz., $\langle \beta \rangle = 5$ to 10%), the magnetic field required for satisfactory performance of tokamak reactors in various applications can be sufficiently low so that steady-state operation of resistive coils is quite feasible.¹ In applications such as radiation test facilities, fusion-fission power production, and the generation of nuclear or chemical fuels, resistive TF coils can prove to be an advantageous alternative to superconducting coils. For very large "pure fusion" electrical power reactors (i.e., having only modest blanket energy multiplication), many reactor parameters are comparable irrespective of whether the coils are resistive or superconducting.

1.1 DISADVANTAGES OF SUPERCONDUCTING TF COILS

While a superconducting coil set has been built recently for a small tokamak (the T-7 device at the Kurchatov Institute of Atomic Energy²), its windings are highly stabilized with copper (10:1 cross-section ratio), and a thick copper shield is located between the TF coils and the plasma vessel. These characteristics are impractical for use in a tokamak reactor. Many bubble-chamber and other superconducting solenoids have been built and operated successfully, but they do not face the formidable conditions that will confront the TF coils in tokamak reactors: strangely asymmetric forces, overturning moments, rapidly pulsed magnetic fields, neutron and gamma radiation, and difficulty of access for repair of the coils.

A number of serious drawbacks of superconducting coils must be overcome before they can be utilized in tokamak reactors:

- Nondemountability - Access to internal components such as poloidal field (PF) coils, divertor hardware, and inboard blanket modules is extraordinarily difficult if not impossible unless the TF coils are readily demountable. Repair of a non-demountable coil would require the removal of an entire reactor segment. Implementation of demountable superconducting coils, however, is probably impractical (although a conceptual design exists³).
- Pulsed Fields - Superconducting coils with practical stabilizer volumes may not be able to withstand (without going normal) the large rate-of-change of vertical fields commonly found in tokamak operation during start up and during plasma disruptions.⁴ In fact, the maximum loop voltage that could be applied to start the plasma current will be limited by the maximum dB/dt tolerated by superconducting TF coils.
- Neutrons - Neutron streaming through injection and pumping ducts introduces special shielding problems for superconducting TF coils.⁵
- Construction - Superconducting coils of the size needed for tokamak test reactors must be built on site. They are far too bulky, heavy, and fragile to be moved from a specialized off-site factory where quality assurance can be much stricter.

- Refrigeration - Cryogenic refrigeration requirements may exceed 50 kW at 4°K, corresponding to 20 MW at room temperature, so that the refrigerator size must be several times larger than the present state of the art. In any event, the refrigeration system would be extraordinarily complex compared with a resistive-coil water coolant system.
- Maintenance - Maintenance of the reactor components is impossible unless the TF coils are shut off, which for superconducting coils implies a down-time of at least two days even for the most trivial maintenance operation.

1.2 MOTIVATION FOR RESISTIVE TF COILS

Water-cooled resistive TF coils fabricated of copper or aluminum can be made readily demountable,⁶ thereby ensuring the feasibility of internal placement of PF coils and of the use of a poloidal magnetic divertor; they are unaffected by pulsed fields, require less shielding against neutron irradiation than do superconducting coils, can be factory-built and transported (disassembled) to the reactor site, have simple refrigeration systems, and can be activated or deactivated in a fraction of a minute, thereby allowing rapid turnaround for minor reactor maintenance.

However, resistive coils do have the following disadvantages:

- Power Dissipation - The power consumption of resistive coils will always be considerably greater than that of superconducting coils (about 20 MW for the cryogenic refrigerator for a full-sized reactor coil set). As shown in this study, however, if sufficient copper is used to allow the resistive coils to operate at steady state, then

the power dissipation can be reduced to the range of 150 to 300 MW for full-scale D-T reactors. This power level will be comparable with the sum of the other electrical power drains in the reactors.

- Volume of Material - Several thousand tons of copper will be required for a resistive TF coil set that can operate in the steady state. This mass represents about one day's production of copper in the U.S.⁷ On the other hand, cryostable superconducting coils require a mass of copper or aluminum of at least several hundred tons in addition to large masses of niobium and titanium or tin. The prices of the latter metals are reaching rather high levels, while the prices of copper and aluminum, which are relatively abundant, are expected to remain low.
- A disadvantage of present day resistive TF-coil sets is that they are pulsed at the same frequency as that of the plasma discharges, so that the coil lifetime is limited by fatigue. This fatigue problem will be eliminated in the steady-state TF coil set of a tokamak reactor.

1.3 LONG-TERM PROSPECTS FOR RESISTIVE COILS

It will be argued that in the long term, all toroidal reactors must have superconducting coils. However, even if reliable superconducting coils permitting easy reactor maintenance and having acceptable cost are developed in fifty years time, certain other factors will continue to weigh in favor of retaining the simpler resistive TF coil sets, at least for D-T reactors:

- It seems probable that methods of generating toroidal plasmas of increasing beta will be developed so that the magnetic field required for a given reactor application will be reduced.
- It is not clear that economically recoverable helium will be available in fifty years.⁸ Even if it is, there are many other applications requiring helium with which magnetic fusion reactors will have to compete. Besides its use as a general purpose coolant, helium may be required for applications such as dirigibles, superconducting transmission lines and levitated trains. What helium is available for fusion reactors should be retained for cooling blankets and for cryopumping.
- The price of niobium, like that of most relatively rare metals, could become exorbitant in fifty years.

It seems likely that superconducting coils will be relegated to use with fusion reactors using "advanced fuels" such as D-D, where the power densities are so low that the coil power dissipation must be truly minimized.

1.4 OBJECTIVES OF THE PRESENT INVESTIGATION

A practical tokamak reactor must have readily demountable TF coils to ensure ease of access for the maintenance of internal PF coils and the components of a poloidal magnetic divertor, and for the processing of inboard blanket modules. In the case of failure of a TF coil, demountability allows its replacement in an acceptable length of time. While demountable superconducting coils are probably impractical, demountable resistive TF coils

have been found to perform well in the ISX,⁶ Doublet III,⁹ and PDx¹⁰ experimental tokamaks, as well as the FM-1 spherator.¹¹ The ISX coils, in particular, lend themselves to rapid disassembly for changeout of the machine interior.

Reference 12 has reported an analysis of steady-state resistive coils for a tokamak reactor, but using nondemountable coils of circular bore. The present study parameterizes the performance of reactors with steady-state resistive TF coils of rectangular geometry designed especially for ease of demountability. Sections 2 to 4 of this paper determine tokamak and coil configurations that give optimal values of certain figures-of-merit related to near-term reactor goals. It is found that practical reactors can be realized with resistive losses of 200 to 250 MW, at the expense of copper masses between 2400 and 4400 tons. Consideration of the trade-offs between capital cost and operating cost for TF coils in reactors of specified performance shows that the total lifetime expenditures are reasonable.

Various stress and thermal aspects of the coils are examined in Sections 5 and 6, examples of TF coil joints are illustrated in Section 7, and the parameters of representative designs of tokamak reactor are given in Sections 8 and 9.

2. TOKAMAK MODEL

2.1 REACTOR GEOMETRY

The analysis is based on the rectangular coil geometry shown in Fig. 1, although qualitatively the results are equally applicable to more general elongated shapes. The plasma, of rectangular cross section $2a_p \times 2h_p$, is surrounded by a scrape-off layer 0.25 m thick. Space 1.5 m high is allocated to a single-null poloidal divertor. The vacuum vessel, blanket, and shield assembly are assumed to have a total thickness of 1.0 m. The half-width of the magnet bore is a_c and the half-height b_c . The radial build is denoted by d , and the axial build by w .

The plasma is centered at radius R_p and the coil at R_c relative to the major axis of the reactor, leaving gaps of d_{OH} inboard and d_v outboard between the shield and the coil. The space d_{OH} accommodates the ohmic heating solenoid for design options where it is topologically-linked with the TF coil system. The outer equilibrium-field coils are located in d_v , the magnitude of which is chosen in part to give an acceptable TF ripple at the plasma. The radius corresponding to the maximum magnetic field strength B_M is denoted by R_M . To allow the coils to be self-supporting against the centering magnetic forces by nesting on assembly, the inboard faces are chaved to radius R_s , arbitrarily assumed to be $R_M + d_{OH}$. Out-of-plane loads are to be taken out through shear/compression panels positioned between the coils or through radiation shielding.

Two families of tokamak layouts are proposed. For Type A, the OH coils are located in the throat of the reactor, and $d_{OH} = 0$. Type BI provides a much larger area for the OH flux swing by placing the OH coils in the bore of the TF coils. The TF coil inboard leg can then be extended to the reactor centerline, to yield electrical and structural benefits. The BI option is

practical only for a reactor with readily demountable TF coils. Based on the theoretical concept that the plasma current can be established and sustained by a combination of neutral beam injection and the EF flux swing,¹³ Type BII completely eliminates the OH coils, so that again d_{OH} can be zero.

2.2 CHARACTERISTIC PARAMETERS FOR THE TF COILS

The TF coils are characterized by their mass M_C and their resistive power loss P_{res} , parameters which represent respectively most of their capital and operating costs.

For a conductor of arbitrary shape carrying a current density j , the power dissipated is

$$P_{res} = \int \rho_e j^2 d\tau \quad (2-1)$$

where ρ_e is the resistivity of the material, and the integral is carried out over the volume of the conductor. For a constant resistivity, and the current density uniform across the cross section of a conductor in keeping with the steady-state assumption, the total dissipation of a composite of N conductors of cross-sections A_i constant along a common length L , each carrying a current I , is

$$P_{res} = \rho_e I^2 L \sum_{i=1}^N \frac{1}{A_i} \quad (2-2)$$

This quantity can be minimized subject to the constraint that the total cross-section be fixed to give

$$P_{res} = \rho_e (NI)^2 \frac{L}{A} \quad (2-3)$$

The minimum power dissipation for a group of N conductors constituting a cross-sectional area A and each passing the same current, is realized therefore when the conductors have equal areas, or, alternatively, when the current density is constant not only across each conductor but across the whole cross section. Then the composite structure can be treated as a single large conductor of area A carrying a uniform current density NI/A .

The characteristics of the TF coil system are calculated on the basis of the following model:

- All turns of a coil are of equal cross-sectional area and carry the same current. In particular, there are to be 12 coils of 25 turns each. The conceptual design anticipates assembly from high-conductivity copper plates with cooling channels machined in the faces at regular intervals. Calculations have assumed CDA 102 copper alloy with 101% IACS conductivity or a resistivity of $1.9 \times 10^{-8} \Omega\text{m}$ at 50°C .
- The packing factor, the fraction of the coil cross section occupied by the copper conductor, is specified for the outboard leg and varies elsewhere so as to keep unchanged the area devoted to cooling passages and insulation. Detailed calculations assume a packing factor of 0.85 for the outboard legs, with the remaining area subdivided to allow 10% for insulation and 5% for cooling passages.
- The resistive dissipation and, for consistency, the coil mass are calculated excluding the relatively small contributions of the corner regions. In an engineering design the outboard corners could be rounded in order to reduce stress levels¹⁴ while simultaneously

eliminating unnecessary mass and electrical resistance. Since the extent of rounding is restricted on the inboard side, a correction of 5% is added to the calculated values.

For Type A geometry, the resistive power loss for N_c coils is given by

$$P_{res} = \frac{4\pi\rho_e}{2\mu_o} C_R B_M^2 \frac{1}{d/R_M} \left[\frac{2a_c + b_c}{f_o} + \frac{2b_c}{f_i(2 - d/R_M)} \right] \quad (2-4)$$

under the restriction $\pi/N_c \ll 1$, where f_o and f_i are the packing factors in the outboard and shaved portions of the coil respectively, μ_o is the magnetic permeability, and C_R the correction factor to account for the corners (1.05 in the calculations). The mass for N_c coils can be written as

$$M_c \left[\frac{1 - R_M/R_c}{R_M/R_c} \right]^2 = 2\pi\rho a_c^2 C_R \frac{d}{R_M} \{ (2 - d/R_M) f_i + 2 f_o \} b_c + 4a_c f_o \} \quad (2-5)$$

where ρ is the density of the conductor. The relation between b_c and a_c is

$$b_c = \frac{1}{2} \frac{b_p}{a_p} [2a_c - (d_{OH} + d_v + 2.5)] + 2.0 \quad (2-6)$$

so that for a fixed plasma elongation, P_{res} and M_c are functions of a_c alone with the parameters d/R_M and R_M/R_c , having ranges between 0 and 1, representing the shape of the coils in nondimensional form.

The corresponding relations for Type B geometry are

$$\begin{aligned}
 P_{res} = \frac{4\pi\rho_e}{\mu_o} C_R B_M^2 \frac{1}{d/R_M} & \left[\frac{2a_c + b_c - d_{OH}}{f_o \left[1 + d_{OH}/a_c \frac{1 - R_M/R_c}{R_M/R_c} \right]} + \frac{2b_c}{f_i} d/R_M \right. \\
 & \left. + \frac{R_M/R_c a_c}{1 - R_M/R_c} \ln \left\{ \frac{f_o \left[1 + d_{OH}/a_c \frac{1 - R_M/R_c}{R_M/R_c} \right]}{(f_o - 1) \left[1 - d_{OH}/a_c \frac{1 - R_M/R_c}{R_M/R_c} \right] + 1} \right\} \right] \quad (2-7)
 \end{aligned}$$

$$\begin{aligned}
 M_c \left[\frac{1 - R_M/R_c}{R_M/R_c} \right]^2 &= 2\pi\rho a_c^2 C_R d/R_M \left\{ \left[2f_o \left(1 + d_{OH}/a_c \frac{1 - R_M/R_c}{R_M/R_c} \right) + \frac{f_i}{d/R_M} \right] b_c \right. \\
 & \left. + 4a_c f_o \left[1 + d_{OH}/a_c \frac{1 - R_M/R_c}{R_M/R_c} \right] - \frac{d_{OH}^2}{a_c} \right\} \quad (2-8)
 \end{aligned}$$

3. REACTOR PLASMA CONSIDERATIONS

For a deuterium-tritium plasma with equal densities of deuterons and tritons, the fusion reaction rate per unit volume is

$$R = \frac{1}{4} \frac{p^2}{k^2} \frac{\langle \sigma v \rangle}{(T_e + T_i)^2} \quad (3-1)$$

where p is the total plasma pressure, T_i the mean ion temperature, T_e the electron temperature, k the Boltzmann constant, and $\langle \sigma v \rangle$ the fusion reactivity. In terms of β , the ratio of plasma to magnetic pressure, Eq. (3-1) is rewritten as

$$R = \frac{1}{(4\mu_0 k)^2} \frac{\langle \sigma v \rangle}{(T_e + T_i)^2} \beta^2 B_t^4 \quad (3-2)$$

where B_t is the magnetic field at the center of the plasma.

For a given plasma pressure, the maximum fusion power density P_f is attained at the maximum of $\langle \sigma v \rangle / T_i^2$, which for a D-T plasma occurs near $T_i = 15$ keV. Then for $T_e = T_i$ (in MW/m³)

$$P_f = 1.3 \beta^2 B_t^4. \quad (3-3)$$

Enhancement of $\langle P_f \rangle$ by a factor of 2 to 3 is realistic with tailored plasma profiles.¹⁵ A value of 2.3 is chosen to yield a space-averaged fusion power density (in MW/m³) of

$$\langle P_f \rangle = 3.0 \langle \beta \rangle^2 B_t^4. \quad (3-4)$$

For the driven (subignited) plasmas that are most pertinent to this work, the above relations would require some modification. Nevertheless, Eq. (3-4) is used throughout this analysis.

For the rectangular geometry defined in Section 2 the total fusion power output (in MW) is

$$P_{\text{fus}} = 24.0 \pi \langle \beta \rangle^2 \frac{b_p}{a_p} \left(\frac{a_p}{R_p} \right)^2 R_p^3 B_t^4 \quad (3-5)$$

of which 80% is associated with the neutron flux. An average neutron wall loading ϕ_w , defined on the basis of a fictitious first wall placed at the 25 cm scrape-off distance all around the plasma, becomes (in MW/m²)

$$\phi_w = 2.4 \langle \beta \rangle^2 \frac{b_p}{a_p} \left(\frac{a_p}{R_p} \right)^2 \frac{R_p^2 B_t^4}{a_p \left(1 + \frac{b_p}{a_p} \right) + 0.50} \quad (3-6)$$

MHD stability considerations of tokamak equilibria place an upper bound on the average plasma pressure through the relation¹⁶

$$\max \langle \beta \rangle = 0.12 \frac{a_p}{R_p} \frac{b_p}{a_p} \quad , \quad (3-7)$$

thus limiting the achievable fusion power density and the neutron wall loading. This relation is valid for plasma elongations up to approximately 2. The present study of TF coil parameters is carried out for a single ratio $b_p/a_p = 2.0$.

The variables B_t , R_p , and a_p that refer to the plasma can be rewritten in terms of coil parameters using the expressions

$$B_t = \frac{(R_M/R_C) a_c}{(1 - R_M/R_C)} B_M \cdot \frac{1}{a_c / (1 - R_M/R_C) + (d_{OH} - d_v)/2} \quad (3-8)$$

$$R_p = \frac{a_c}{1 - R_M/R_C} + \frac{d_{OH} - d_v}{2} \quad (3-9)$$

$$a_p = a_c - 1.25 - \frac{d_{OH} + d_v}{2} \quad (3-10)$$

4. OPTIMIZATION OF COIL PARAMETERS

4.1 FIGURES-OF-MERIT

Various figures-of-merit may be defined on the basis of the fusion power output, the neutron loading on the first wall, the resistive power dissipated in the coils, and the coil mass. When tempered with engineering considerations, different optimizations provide a basis for the choice of reference designs.

Detailed calculations have been carried out for Type A configurations with $d_v = 0.80$ m and for Type B with $d_{OH} = 0.45$ m and $d_v = 0.50$ m. Figures 2, 3, 4, and 13, 14, 15 show P_{res} , M_C , and ϕ_w for the two configurations as functions of a_c , with d/R_M and R_M/R_C as parameters.

The fusion electrical power gain factor Q_c for the coils, defined as the ratio of the potential gross electrical output from the reactor to the electrical power input to the TF coils, is

$$Q_c = 0.28 \frac{P_{fus}}{P_{res}} M_{bl} \quad (4-1)$$

for a thermal-to-electrical conversion efficiency of 35%. M_{bl} is the average fusion neutron energy multiplication in the blanket modules. Q_c for $M_{bl} = 1$ is shown in Figs. 5 and 16.

The ratio of the net electrical power output P_{out} to the total recirculating power P_{circ} , representing the sum of P_{res} and the power lost to other reactor subsystems, is given by

$$\frac{P_{out}}{P_{circ}} = Q_C \frac{P_{res}}{P_{circ}} - 1 \quad (4-2)$$

If the resistive dissipation is comparable with other types of power consumption so that, for example, $P_{res}/P_{circ} = 1/2$, a fusion reactor that gives a net power output of the same magnitude as the recirculating power would require $Q_C = 4$.

For reactor applications where the neutron wall loading is the most appropriate measure of performance, an efficiency parameter analogous to Q_C is ϕ_w/P_{res} , which represents the wall loading obtainable per unit operating cost (Figs. 6 and 17). For fixed coil shape parameters and for fixed B_M , the efficiencies Q_C and ϕ_w/P_{res} and the coil mass M_C are increasing functions of bore size a_c . Consequently, both Q_C and ϕ_w/P_{res} can be increased at the cost of a larger M_C . A measure of the effectiveness of the utilization of the conductor would be the parameters Q_C/M_C and $\phi_w/(P_{res} M_C)$ (Figs. 7, 8, and 18, 19). Both pass through maxima as functions of a_c , thus defining possible "optimal" geometries, with the maximum of $\phi_w/(P_{res} M_C)$ falling in a range that could be of practical interest. The magnitudes of these parameters are proportional to B_M^2 so that they can be increased readily by raising the magnetic field strength.

4.2 OPTIMIZATION FOR NEUTRON WALL LOADING

Instead of defining global optima, it is of greater practical relevance to examine optimizations under suitable constraints. A comparison of all the configurations that give a specified time-averaged wall loading can lead to geometries that minimize the coil dissipation or that minimize the total

lifetime cost associated with the coils. In reactors intended for fusion-neutron applications, ϕ_w should be in the range of 0.3 to 2.5 MW/m², depending on specific goals.¹⁷ The general results of this study are adapted to define reactors that achieve a spatially-averaged ϕ_w of 0.5 MW/m². The local value on the outboard wall can be expected to be about 25% higher.¹⁸ In any case, for a given reactor geometry and β the wall loading from a thermonuclear plasma can be raised as B_M^4 if higher values are required.

Under the simplifying assumptions that the displacement of the plasma centerline relative to the coil centerline is small compared with the coil major radius,

$$\frac{d_{OH} - d_v}{2 a_c} (1 - R_M/R_C) \ll 1, \quad (4-3)$$

and that the scrape-off layer thickness is small compared with the plasma half-width,

$$\frac{0.25}{a_c - \delta} \ll 1 \quad (4-4)$$

where δ is the average horizontal space between the plasma and the coil

$$\delta = 1.25 + \frac{d_{OH} + d_v}{2} \quad (4-5)$$

the condition of constant ϕ_w can be written as

$$\frac{a_c^2}{(a_c - \delta)^3} = G(\phi_w, R_M/R_C) B_M^4, \quad (4-6)$$

where

$$G(\phi_w, R_M/R_C) = \frac{0.09216}{\phi_w} (R_M/R_C)^4 (1 - R_M/R_C)^2. \quad (4-7)$$

Eq. (4-6) has a solution

$$a_c = \frac{\delta}{1 - \left(\frac{1}{2\delta G B_M^4} \right)^{1/3} \left\{ (1 + H)^{1/3} + (1 - H)^{1/3} \right\}} \quad (4-8)$$

where

$$H = \left(1 + \frac{4}{27} \frac{1}{\delta G B_M^4} \right)^{1/2}. \quad (4-9)$$

Since for Type A geometry, and for Type B geometry in the limit $d_{OH}/a_c \ll 1$, P_{res} for constant d/R_M is proportional to $B_M^2 (a_c - C)$,

$$P_{res} \sim B_M^2 \left[\frac{\delta}{1 - \left(\frac{1}{2\delta G B_M^4} \right)^{1/3} \{ (1+H)^{1/3} + (1-H)^{1/3} \}} - C \right]. \quad (4-10)$$

C is a constant proportional to δ^{-1} with a weak d/R_M dependence, and arises due to the fact that the height-to-width ratio for the magnet bore differs from the corresponding ratio for the plasma. The effects of the shape parameters for a fixed δ could be essentially absorbed to give a universal function relating P_{res}/B_M^2 to $G B_M^4$. For small B_M ,

$$P_{res} \sim B_M^{-2} \quad \text{and} \quad a_C \sim B_M^{-4}$$

while for large B_M ,

$$P_{res} \sim B_M^2 \quad \text{and} \quad a_C \rightarrow \delta$$

so that P_{res} has a minimum at intermediate values of B_M corresponding to an $a_C > \delta$.

Using Eq. (4-6), the value of a_C at the minimum is readily shown to be a solution of the equation

$$a_c^{\min 2} - a_c^{\min} (4\delta - C) + 2\delta C = 0 \quad . \quad (4-11)$$

A particularly convenient form for the root is obtained by making use of the fact that in practice $(C/4\delta)^2 \ll 1$, so that to first order in C/δ

$$a_c^{\min} = 4\delta \left(1 - \frac{3}{8} \frac{C}{\delta} + \dots \right) \quad . \quad (4-12)$$

Curves of P_{res} as a function of B_M for constant ϕ_w have minima at a value of a_c that is independent of ϕ_w and R_M/R_C and only very weakly dependent on d/R_M (through C). Therefore, in a reactor configuration that gives the minimum P_{res} for the TF coils for any prescribed wall loading and any combination of shape parameters, the plasma occupies approximately 3/4 of the radial bore.

The magnitude of the minimum P_{res} does depend on the shape parameters and wall loading. For Type A reactors,

$$P_{res}^{\min} = \frac{509.8}{2} \frac{\rho_e}{\mu_o} C \frac{1}{d/R_M} \left[\frac{1}{f_o} + \frac{1}{f_1 (2 - d/R_M)} \right] \frac{\phi_w^{1/2} \delta^{1/2}}{(R_M/R_C)^2 (1 - R_M/R_C)} \left[1 - \frac{C}{4\delta} + \dots \right] \quad , \quad (4-13)$$

and for Type B,

$$p_{\text{res}}^{\text{min}} = \frac{509.8 \rho_e}{2 \mu_0} C_R \frac{1}{d/R_M} \left[\frac{1}{f_o} + \frac{d/R_M}{f_i} \right] \frac{\phi_w^{1/2} \delta^{1/2}}{(R_M/R_C)^2 (1-R_M/R_C)} \left[1 - \frac{C}{4\delta} + \dots \right]. \quad (4-14)$$

The corresponding value of B_M in both cases is

$$B_M^{\text{min}} = 1.592 \frac{\phi_w^{1/4}}{\delta^{1/4} (R_M/R_C) (1-R_M/R_C)^{1/2}} \left[1 + \frac{3}{16} \frac{C}{\delta} + \dots \right]. \quad (4-15)$$

The exact solutions for $\phi_w = 0.5 \text{ MW/m}^2$ are shown in Figs. 9, 10, and 20, 21. A configuration of minimum resistive loss is achieved at the expense of large coil dimensions and mass but modest magnetic field strength. The fusion power output may also be too large to be of practical interest. A geometry that gives higher resistive dissipation but smaller coil mass may be preferable to achieve the same wall loading so long as the power loss is still comparable with the total electrical power consumed in the plasma heating systems, the PF coils, and the blanket coolant loops.

4.3 VARIATION OF POWER LOSS WITH COIL MASS

An examination of the trade-offs between P_{res} and M_c as the coil geometry changes but ϕ_w and B_M remain fixed leads to the matter of the lifetime cost of the coil system. An arbitrary limit of 250 MW on P_{res} for realistic geometries suggests that suitable values for B_M would be 6T for a Type A configuration (Figs. 9 and 10) and 7.75T for Type B (Figs. 20 and 21). It is

convenient to look at a sequence of curves of constant d/R_M , with R_M/R_C changing along each curve.

The condition of constant ϕ_w and B_M can be rewritten from Eq. (4-6) as

$$\left(\frac{R_M}{R_C}\right)^2 \left(1 - \frac{R_M}{R_C}\right) = F(a_C; \phi_w, B_M) \quad (4-16)$$

where

$$F(a_C; \phi_w, B_M) = \left[\frac{\phi_w}{0.09216 B_M^4} \frac{a_C^2}{(a_C - \delta)^3} \right]^{1/2}. \quad (4-17)$$

F reaches a maximum permissible value when $R_M/R_C = 2/3$, so that there exists a minimum a_C compatible with the imposed conditions. Its value can be calculated from Eq. (4-8).

The three roots of Eq. (4-16) are

$$\frac{R_M}{R_C} = \frac{1}{3} + \frac{2}{3} \cos \left\{ \frac{1}{3} \left[\cos^{-1} \left(-\frac{27}{2} \left[F - \frac{2}{27} \right] \right) \right] + \frac{2m\pi}{3} \right\} \quad (4-18)$$

corresponding to $m = 0, 1, 2$. From the fact that the range of values for F is $0 < F < 4/27$, the $m = 1$ root can be shown to be always negative and thus not of interest; the remaining two roots are positive and coalesce to the value $R_M/R_C = 2/3$ when a_C reaches its minimum.

Since M_C depends explicitly on R_M/R_C and a_C , and since a_C is linearly proportional to P_{res} , M_C can be written explicitly as a function of P_{res} . For each value of P_{res} above a minimum there exist two values of M_C . The limiting behaviors of the curves are given by

$$P_{res} \sim M_C^{2/5} \text{ for } \frac{R_M}{R_C} \rightarrow 0$$

$$P_{res} \sim M_C^{1/4} \text{ for } \frac{R_M}{R_C} \rightarrow 1.$$

Portions of the exact solutions for Type A with $B_M = 6T$ appear in Fig. 11 and for Type B with $B_M = 7.75T$ in Fig. 22. The minimum P_{res} occurs at values of M_C beyond the range shown. There also exists a minimum M_C for each d/R_M , required in order to allow the curve to evolve from its behavior near the minimum P_{res} to its asymptotic form as $R_M/R_C \rightarrow 0$.

4.4 MINIMUM LIFETIME COST

The capital cost of the TF coil system is essentially proportional to M_C with a much smaller component proportional to P_{res} that accounts for power conversion and cooling equipment. The operating cost is proportional to P_{res} . In the $P_{res} - M_C$ plane, a given total lifetime cost of the coil system is represented by a straight line. As an example, the fabrication cost of a TF coil is taken as \$35/kg, a value in keeping with the large quantities of copper involved and the relatively simple design. The cost of electricity is specified as \$25/MWh. The coil lifetime is assumed to be 12 years at 60%

capacity factor. Figures 11 and 22 show that for each d/R_M there is a coil mass in the range 3,000 to 4,000 tons for Type A and 3,000 to 5,000 tons for Type B that yields a minimum lifetime cost of around \$500M. A locus of minimum lifetime cost can be defined showing its d/R_M - variation as well as that of the parameters P_{res} and M_C associated with minimum-lifetime-cost configurations.

Although the optimal geometry depends on coil manufacturing costs and the cost of electrical power, as shown in Fig. 12, with major effects on the total lifetime cost, for given rates the reactor parameters can vary over a significant range with only minimal increases in lifetime cost. For example, Fig. 11 shows that a variation in M_C by 1,000 tons around the optimum produces changes in the lifetime cost of only a few per cent.

4.5 NORMALIZED NEUTRON WALL LOADING EFFICIENCY

It is useful to compare the optimization curves relating the normalized wall loading efficiency $\phi_w/(P_{res} M_C B_M^2)$ to a_c for constant d/R_M , R_M/R_C , and B_M with those for constant d/R_M , ϕ_w , and B_M . Both are shown on Figs. 8 and 19. The point of minimum a_c which exists for fixed ϕ_w and B_M separates the corresponding curves into upper and lower branches associated with the two roots of R_M/R_C for each a_c . The upper branch with $R_M/R_C < 2/3$ passes through a maximum and in the limit $R_M/R_C \rightarrow 0$ for which $a_c \sim (R_M/R_C)^{-4}$ shows as asymptotic decay $\phi_w/(P_{res} M_C B_M^2) \sim a_c^{-7/2}$. The lower branch for $R_M/R_C > 2/3$, which is omitted from the figures, drops with increasing a_c and in the limit $R_M/R_C \rightarrow 1$, for which $a_c \sim (1 - R_M/R_C)^{-2}$, approaches $\phi_w/(P_{res} M_C B_M^2) \sim a_c^{-5}$. For comparison, it is noted that the curves of constant R_M/R_C decay as $\phi_w/(P_{res} M_C B_M^2) \sim a_c^{-3}$ for large a_c .

5. STRESS CONSIDERATIONS

The large volume of the conductors associated with resistive TF coils designed for steady-state operation, and the moderate magnetic field strengths adequate for satisfactory reactor performance allow the coils to be self-supporting against in-plane loads. Out-of-plane stresses arising from overturning moments or from fault conditions can be taken out through shear/compression panels¹⁹ or through radiation shielding.²⁰

Due to in-plane forces, by far the largest stresses originate with the bending moments (see Table 2). Estimates can be obtained from the theory of rigid frames. With the coils wedged together to a radius R_g under the large centering forces, the coil system can be represented as a series of rectangular frames coalescing into a rigid core formed by the inner beams. There is, however, no difficulty in relaxing the built-in boundary condition and postulating a continuous elastic support along a portion of the inner beams. The more general approach is taken here to give results of wider applicability for which the case of interest represents one limit.

The model and the loadings are illustrated in Fig. 23, with the coil represented by a rectangular frame of mean dimensions $h \times l$ built up of beams with moments-of-inertia I_h , I_l , and I_i . The inner element at radius R_i is supported along a length δ by a continuous elastic bucking cylinder. For a R^{-1} toroidal magnetic field variation in the bore of the coil, the outward force per unit length along the horizontal legs is

$$F_v = \frac{\pi}{\mu_0 N_C} \frac{B^2 R^2}{R} \quad (5-1)$$

where N_C is the number of coils. Along the outer vertical leg,

$$F_H = \frac{\pi}{\mu_0 N_C} \frac{B_M^2 R_M^2}{R_i + h}, \quad (5-2)$$

while along the inside leg,

$$F_H = \frac{\pi}{\mu_0 N_C} \frac{B_M^2 R_M^2}{R_i}. \quad (5-3)$$

The moment distributions are most readily calculated by treating the results given by Kleinlogel²¹ for concentrated loads as influence functions in integrals over the load distributions. On the horizontal legs, the bending moment, taken positive for tension on the inside of the frame element, is given by

$$\begin{aligned} \frac{M_x \mu_0 N_C}{2 B_M^2 R_M^2 R_i} &= \left(1 + \frac{x h}{h R_i}\right) \ln \left(1 + \frac{x h}{h R_i}\right) \\ &+ \frac{x}{h} \left[\frac{1}{12[(\lambda+2) + v(2\lambda+3)]} \left(\frac{h}{R_i}\right)^2 \frac{1}{1+h/R_i} \left\{ 3(1+v) - \frac{3v(1+\lambda)}{2} \left[3 - \left(\frac{h}{R_i}\right)^2 \right] \right. \right. \\ &- \left. \left. \left[1+h/R_i \right] \left[1 - \left(\frac{h}{R_i}\right)^2 \right] \right\} - \frac{(\lambda+1)S - \lambda(1+v)T}{(\lambda+2) + v(2\lambda+3)} - (1+h/R_i) \ln(1+h/R_i) \right] \\ &+ \frac{1}{3[(\lambda+2) + v(2\lambda+3)]} \left[(2\lambda + 3) S - \lambda T \right. \\ &- \left. \frac{1}{4} \left(\frac{h}{R_i}\right)^2 \frac{1}{1+h/R_i} \left\{ 1 - \frac{v(2\lambda+3)}{2} \left[3 - \left(\frac{h}{R_i}\right)^2 \right] - \left[1+h/R_i \right] \left[1 - \left(\frac{h}{R_i}\right)^2 \right] \right\} \right] \end{aligned} \quad (5-4)$$

where x is measured from $R = R_i$, and

$$\lambda = \frac{h}{\ell} \frac{I_{\ell}}{I_h} \quad (5-5)$$

$$\nu = \frac{\ell}{h} \frac{I_h}{I_{\ell}} \quad (5-6)$$

$$S = \frac{5}{2} + \left(\frac{R_1}{h}\right)^2 \left\{ \frac{h}{R_1} \left[1 + \frac{5}{6} \left(\frac{h}{R_1}\right)^2 \right] - \left(1 + \frac{h}{R_1}\right) \left(1 + 2 \frac{h}{R_1}\right) \ln \left(1 + \frac{h}{R_1}\right) \right\} \quad (5-7)$$

$$T = \frac{1}{2} + \left(\frac{R_1}{h}\right)^2 \left\{ -\frac{h}{R_1} \left[1 - \frac{2}{3} \left(\frac{h}{R_1}\right)^2 \right] + \left[1 - \left(\frac{h}{R_1}\right)^2 \right] \ln \left(1 + \frac{h}{R_1}\right) \right\} . \quad (5-8)$$

For the outer vertical leg the corresponding relation is

$$\begin{aligned} \frac{M_{YOC}^U}{\pi B_M^2 R_M^2 R_1} &= \frac{1}{3[(\lambda+2) + \nu(2\lambda+3)]} \left[\lambda \{T(2 + 3\nu) - S\} \right. \\ &\quad \left. + \frac{1}{4} \left(\frac{\ell}{R_1}\right)^2 \frac{1}{1 + h/R_1} \left\{ 2 + 3\nu - \frac{\nu\lambda}{2} \left[\left\{ 3 - \left(\frac{\delta}{\ell}\right)^2 \right\} - \left\{ 1 + h/R_1 \right\} \left\{ 1 - \left(\frac{\delta}{\ell}\right)^2 \right\} \right] \right\} \right] \\ &\quad - \frac{1}{2} \left(\frac{\ell}{R_1}\right)^2 \frac{1}{1 + h/R_1} \frac{Y}{\ell} \left(1 - \frac{Y}{\ell}\right) \end{aligned} \quad (5-9)$$

where y is measured from one end. For the inner leg the result is

$$\begin{aligned}
\frac{M_{Y O C}}{\pi B_M^2 R_M^2 R_i} = & - \frac{1}{\lambda[(\lambda+2) + \nu(2\lambda+3)]} \left(\frac{\ell}{R_i}\right)^2 \frac{1}{1 + h/R_i} \left\{ 1 - \frac{\nu(2\lambda+3)}{2} \left[3 - \left(\frac{\delta}{\ell}\right)^2 \right] \right. \\
& \left. - \left[1 + h/R_i \right] \left[1 - \left(\frac{\delta}{\ell}\right)^2 \right] \right\} \\
& + \frac{(2\lambda+3) S - \lambda T}{3[(\lambda+2) + \nu(2\lambda+3)]} - \frac{1}{2} \left(\frac{\ell}{R_i}\right)^2 \frac{1}{1 + h/R_i} \frac{Y}{\ell} \left[1 - \left(1 + h/R_i \right) \frac{Y}{\ell} \right] \\
\left\{ \begin{array}{ll} 0 & \text{for } 0 < \frac{Y}{\ell} < \frac{1}{2} \left(1 - \frac{\delta}{\ell} \right) \\ \frac{1}{2} \frac{\ell}{\delta} \left(\frac{\ell}{R_i}\right)^2 \frac{h/R_i}{1 + h/R_i} \left[\frac{Y}{\ell} - \frac{1}{2} \left(1 - \frac{\delta}{\ell} \right) \right]^2 & \text{for } \frac{1}{2} \left(1 - \frac{\delta}{\ell} \right) < \frac{Y}{\ell} < \frac{1}{2} \left(1 + \frac{\delta}{\ell} \right) \\ \left(\frac{\ell}{R_i}\right)^2 \frac{h/R_i}{1 + h/R_i} \left[\frac{Y}{\ell} - \frac{1}{2} \right] & \text{for } \frac{1}{2} \left(1 + \frac{\delta}{\ell} \right) < \frac{Y}{\ell} < 1 \end{array} \right. \quad (5-10)
\end{aligned}$$

Nesting increases the effective stiffness of the inner leg of an individual frame. In the limit $\nu \rightarrow 0$, the bending moment distributions on the horizontal and the outer vertical elements are those for a frame with built-in horizontal legs; the moment distribution on the inner leg is superfluous. Fig. 24 shows the normalized bending moments for a representative range of values of the parameters h/R_i , h/ℓ , and λ for a nested coil system ($\nu = 0$) with R_i taken to be R_g . These examples cover the reference cases defined and examined in detail in Section 8. The effects of a finite stiffness of the inner leg and the effects of changes in length of the bucking cylinder are illustrated in Fig. 25.

A particular case, using parameters corresponding approximately to the TF coils of ISX, for which finite element solutions exist based on exact force

calculations,²² is shown in Fig. 26 to demonstrate the accuracy of the rigid frame approach. The compound shape of the inner beam, where approximately the central half has a radial build of 3 inches and the outer sections 5 inches, is best modelled by taking the build to be 3 inches over the whole length with the increased stiffness of the outer portions partly taken into account by a fictitiously-lengthened bucking cylinder.

The highest stresses are found at the extremities and at roughly the mid-spans of the horizontal and vertical legs, with the maximum usually occurring at the outboard corners. Regions of low bending stress and consequently almost pure tension suggest advantageous locations for the joints, as in the ISX TF coils, although even at the corners of the reference designs the stress levels are acceptable. For coils bearing on a bucking cylinder, the solutions depend on the choice of the length of the support as well as on the characteristics of the frame itself. By going from a nested system to one with a bucking cylinder, the bending stresses at the outer corners are affected only slightly. A proper choice of δ/l , however, can significantly reduce the stresses near the inner corners where joints are to be located, and maintain them small along the entire inner beams.

6. THERMAL CONSIDERATIONS

Whereas in the pulsed TF coils of present-day tokamaks the heating and cooling take place on different time scales, in steady-state operation there is an equilibrium at all times between the internal heat generated and that convected away by the flowing coolant. Even coils designed for pulsed scenarios can be operated at steady state (as in fact they are for discharge cleaning) at a fraction of their design current, determined either by temperature limitations or by the performance of the external heat rejection system.

The TF coils conceived in this study are assembled from sets of high-conductivity copper plates incorporating surface cooling channels. After passing through the coils, the cooling water is circulated through an external heat rejection system of adequate capacity. Even though the coils are not subjected to the pulsed loadings conducive to metal fatigue and therefore in principle can be operated at higher conductor temperatures with larger temperature gradients, in the interests of system integrity and reliability the conductor, insulator, and coolant temperatures can be maintained well below 100°C for room temperature coolant at the inlet with only modest cooling system performance. An area equal to 5% of the cross section of the outboard leg is adequate for cooling passages.

Estimates of the temperature distributions follow from one-dimensional hydraulics and heat conduction considerations (e.g., Montgomery,²³ Luton²⁴). The bulk temperature of the coolant is a monotonically increasing function of distance from the inlet, deviating from linearity primarily due to changes in cross-sectional area. A global energy balance gives the overall bulk temperature rise. Based on mean local values of resistive dissipation, temperature drops across the boundary layer can be calculated using empirical

relations for the turbulent flow heat transfer coefficient. The local temperature difference in the conductor between the channel wall and any interior point is estimated from a unidirectional conduction model. The nested regions of the coils yield the highest temperature differences so that the temperature profile along the conductor can be non-monotonic. If heat conduction along the conductor were incorporated in the model, the net effect would be a smoothing of the temperature distribution.

Without reference to the locations of the cooling passage inlets and outlets, and with the assumption of a channel length of one turn, the calculated temperature differences for the reference designs are such that even in the worst combination they give peak temperatures that are well below critical values. The thermal stresses should not be an area of concern. The coolant velocity, the pressure loss per channel, and the total pumping power are all reasonable. Specific results of the calculations are given in Section 8 and in Table 3.

A parameter providing an approximate measure of the feasibility of operating the coils at steady state can be defined. The increase in resistive dissipation due to nesting is ignored in the interests of simplicity, and the cross section allocated to cooling passages is taken as one third of the non-conducting area. A balance between the heat generated by resistive dissipation and the heat absorbed by the coolant flowing at velocity V along a channel of length L as its bulk temperature rises by ΔT_b gives

$$\frac{d^2 f_o (1 - f_o)}{B_M^2 L} = \frac{3 \rho_e}{\mu_o \rho_w C_{pw}} \frac{1}{\Delta T_b V} \quad (6-1)$$

where ρ_w is the density of the coolant and C_{pw} is its specific heat. Since there are practical limits that must be imposed on the magnitudes of ΔT_b and V , the value of the parameter $d^2 f_o(1-f_o)/B_M^2 L$ is bounded from below.

The temperature differences across the boundary layers and within the conductor itself generally are considerably lower than ΔT_b and can be taken into account in the choice of ΔT_b . Assuming water as coolant and limiting ΔT_b to 45°C and V to 2 m/sec, Eq. (6-1) can be rewritten to give

$$\frac{d^2 f_o(1-f_o)}{B_M^2 L} > 9.6 \times 10^{-5} \quad (\text{MKS}) \quad (6-2)$$

The factor $f_o(1-f_o)$, with a maximum at $f_o = 0.5$, represents a particular case of the well-known trade-off between areas allocated to current conduction and to cooling so as to give the minimum temperature increase per unit length.

For large coils with moderate values of B_M , coils of the type treated in this study, the current densities in the conductors are sufficiently low that the condition of Eq. (6-2) is readily satisfied for channel lengths of one turn.

7. DESIGN OF CONDUCTORS AND JOINTS

7.1 FABRICATION CONSIDERATIONS

There are three methods of TF-coil fabrication: (1) tape winding, (2) assembly from Bitter plates, and (3) riveting or bolting of plate segments or bars. Type (2) is appropriate for the highest fields, while type (3) can be used when relatively small fields are satisfactory. The first two methods, however, do not lend themselves to demountability. The present conceptual designs make use of type (3) construction with the TF coils assembled from high-conductivity alloy plates.

The components of demountable resistive coils of this type can be prefabricated under strict conditions of quality assurance at specialized factories, transported to the reactor site by existing rail or truck systems, and assembled with minimal work.

Construction itself will be relatively simple: There is no winding process and the milling requirements are straightforward. The large number of identical sections (e.g., several hundred inboard and outboard segments for each reactor) allow essentially mass production with automated machining procedures. Defects can be readily detected by proof-testing, and the segment discarded or refabricated. Hence no defects should be present in the segments shipped to the reactor site.

The insulating material could be fabricated in the form of plates to be compressed between adjacent conductor turns upon coil assembly. Alternatively, insulating material could be bonded to each conductor segment.

Each segment is sufficiently short (12 m or less) and light (10 tons or less) so that it can be transported essentially anywhere with existing rail flatcars or flatbed trucks. The only on-site construction operations required

for the TF coils are joining of the segments. Mass production of segments in a specialized factory and minimization of field assembly work will reduce the total magnet cost, reduce the time needed for reactor fabrication and installation, and maximize the reliability of the completed coil set.

7.2 MATERIALS

7.2.1 Conductors

The price of raw copper is just under \$1 per pound, while the price of machinable copper stock (in large quantities) is about \$2 per pound. The dominant cost will be that of fabrication, however, which at the present time would be in the range of \$12 to \$16 per pound for the relatively simple segments envisaged here. Hence a 4000-ton TF coil assembly would cost \$96 to \$128 million dollars to fabricate. These costs are expected not to rise more rapidly than the general rate of inflation because of the large copper resources in the U.S. and abroad, and because of the increasing displacement of copper by more efficient materials in communications and transportation.

Manufacturers can roll high-conductivity copper plate in thicknesses up to 6 cm for sizes up to 4 m in length and 1 m in width, larger dimensions being prohibited by equipment limitations at both copper refineries and fabricating plants. In any case, the desirability of larger lengths or widths must be weighed against the increasing probability of serious metallurgical flaws for the high-conductivity alloys. With a typical alloy such as CDA 102, the final product is at least half-hard.

The TF coil segments envisaged in this paper would be constructed by joining two or three of the maximum-sized plates by soft soldering or lap riveting. With either technique the strength of the plates would be retained.

Aluminum is the only practical alternative to copper as the TF coil conducting material. Aluminum structural alloy has a conductivity which is only 0.6 times that of OFHC copper, but the density of aluminum is just 0.30 times that of copper, and the two metals have similar costs per pound. Assuming that the fabrication costs per pound are similar, the cost of an aluminum coil would be only one-half that of a copper coil of the same P_{res} . However, copper would still be used for the inboard coil segments where space is at a premium, so that the cost saving would be no more than 35%.

The use of aluminum as the conductor must be evaluated on the basis of the following advantages and disadvantages:

Advantages of Aluminum

- (1) Cost per segment about one-half that of copper.
- (2) Substantially less long-term activation by neutron irradiation than copper.

Disadvantages of Aluminum

- (1) Strong tendency to oxidize, which may cause excessive electrical resistance at the TF coil joints.
- (2) Lower strength than copper, thus placing a more severe limit on the allowed B_M .
- (3) The TF coil joints tend to gall if many assembly and disassembly operations are made.
- (4) Smaller access width between adjacent TF coils.

In view of the disadvantages of aluminum, and because the inboard segments would have to be fabricated of copper in any event, it seems preferable to specify the use of copper for all the coil segments.

7.2.2 Insulators

While organic insulation may be more reliable mechanically, the plate construction advocated here lends itself to the use of inorganic insulation, which may be desirable in order to extend the lifetime of the coil assembly against irradiation. The insulators separating the copper turns can be thin plates (~0.4 cm) of G-10 organic insulation or ceramics such as alumina or magnesia.

7.3 OPTIONS FOR JOINTS

While at least four toroidal confinement devices have been constructed with demountable TF coils, only the FM-1 and ISX devices had coils that were readily demountable. Additional demands on the successful engineering of the joints for the TF coils of a reactor are imposed by the requirement of demountability by means of remote machinery, or at least by semi-contact techniques.

The most probable failure in a TF coil would be associated with the joints; potential problems include an increase in contact resistance, coolant leakage, and bolt fatigue. Thus the greatest attention must be paid to the design of reliable joints, or at least joints that can be repaired easily by remote techniques.

A contact pressure of at least 2000 psi is necessary between two plates being joined, in order that the electrical resistance of the joint be negligible. Exertion of this pressure requires either bolting or hydraulic techniques. In certain designs a coil case may be necessary to take up the shear stresses.

Table 1 lists the various types of joints that have been examined. Several of these have been studied in detail, and the conclusions are reported in Ref. 26. A butt joint probably takes up the least amount of space, but it requires very careful alignment and a fairly elaborate clamping system to exert the required compressive stress. A lap joint, on the other hand, offers the simplest overall joint assembly, but requires the most space and is unsuitable for the inboard joints. The pins used in finger joints must also take up shear stress.

7.3.1 Bolted Joints

Figure 27 shows a TF coil with bolted lap joints at positions of low bending stress on the outboard legs and bolted butt joints on the inboard side. The design of the joints between the upper or lower segments and the outboard segment is straightforward. The joint can be placed in the region of lowest stress, in the range of 4,000 to 8,000 psi when $B_M = 7.5$ T, and there is no access problem. The most suitable joint for the outboard segment is a lap-type joint such as that implemented in the ISX machine.

The design of the joints between the upper and lower segment and the inboard segment, on the other hand, presents extreme difficulties: first because of the very large stresses (perhaps 20,000 to 25,000 psi) in the most desirable regions, namely at the corners (thus allowing the greatest access to the coil bore); and second because of the severe restriction on space and accessibility for the joint, especially if the TF coils are wedged. These problems could be alleviated markedly by displacing the joint outward by several tens of centimeters.²⁶ In that case, however, the OH solenoid cannot be removed without prior disassembly. In Fig. 27, the inboard corners are also held in place by hydraulically clamped stainless steel plates keyed into the center column. The large stresses in the bolts would require the use of

Inconel or an equivalent alloy. To minimize fatigue problems, the bolts would be replaced every time the top (or bottom) segment is disassembled.

A modification of this scheme involves moving the inboard bottom joint to a much larger major radius where it would be readily accessible and could be made into a lap joint. The inboard segment could then be removed from the bottom.

7.3.2 Hydraulic Joints

A great advantage of purely hydraulic clamping (i.e., without bolts) when used with an external shear restraint in the joint area, is that the joint can literally be dismantled by simply releasing the oil pressure to the hydraulic jacks. A stainless steel casing would be necessary to take up shear stress. More discussion can be found in Ref. 26.

8. REFERENCE DESIGNS FOR REACTORS EMPHASIZING NEUTRON APPLICATIONS

Several reference designs have been selected on the basis of the foregoing analysis, tempered with engineering considerations. For fusion neutron applications with wall loadings of 0.5 MW/m^2 , two designs are of Type A in which the OH coils sit in the throat of the reactor, while two are based on an optimization of Type B geometry where the OH coils are placed in the bore of the TF coils (BI) or are omitted altogether (BII). The basic geometries are illustrated in Fig. 2S, and the important parameters are listed in Tables 2, 3 and 4. The locations of the design points for AI and AII are shown in Figs. 2 to 11 and for BI in Figs. 13 to 22. The four illustrative designs have the following features in common:

- (1) $P_{\text{res}} = 200 \text{ to } 250 \text{ MW}$
- (2) $M_c = 3100 \text{ to } 4200 \text{ metric tons}$
- (3) $n\tau_E \sim 3 \times 10^{13} \text{ cm}^{-3}\text{s}$
- (4) $P_{\text{fusion}} = 320 \text{ to } 410 \text{ MW}$
- (5) $\langle \phi_w \rangle = 0.50 \text{ MW/m}^2$.

Other parameters have considerable variation.

8.1 EXTERNAL OH COILS

Configuration AI gives nearly the optimal value of $\phi_w/(P_{\text{res}} M_c)$ associated with reactors of wall loading 0.5 MW/m^2 while the trade-off between P_{res} and M_c results in a lifetime cost of the TF coils only slightly higher than the minimum possible with $d/R_M = 0.50$, approximately \$500M. This is probably comparable to the cost of an equivalent superconducting coil set considering

the latter's higher capital cost, expensive liquid helium refrigeration system, and the refrigeration power requirement. A lower lifetime cost could be obtained with a higher value of d/R_M at the cost of increased coil mass.

For a manufacturing unit cost sufficiently high or an electrical power cost sufficiently low, there is an optimum d/R_M for minimizing total lifetime cost, although its value may be too high to be of practical value. For the rates assumed in Fig. 11, however, this optimum does not occur before d/R_M reaches its physical limit of 1.

The coil mass for Type AI is lower than that of any of the other reference designs requiring an OH solenoid. Radial access is large for ease of installation of test modules, beam lines, and diagnostics. The space between the blanket and the outer leg of the TF coil, defined on the basis of toroidal-field ripple, can accommodate substantial shielding. The ripple values from an exact calculation for rectangular coils are substantially lower than the values in Table 2 which are obtained from an empirical relation derived for circular coils,²⁵ and in fact are sufficiently low to be acceptable for ignited fusion plasmas.

The equilibrium field coils can supply at least one-third of the total flux swing (resistive + inductive) needed to start the plasma current I_p . Since the resistive component is no more than half of the inductive component $L_p I_p$, and since the additional flux swing required to maintain I_p for a flat-top of 100 s is a relatively small 4 Vs for $\langle T_e \rangle \approx 8$ keV, the OH transformer must supply only an amount equivalent to $L_p I_p$. This requirement determines the change in the magnetic field that must be generated by the OH solenoid in the throat of the tokamak.

The main disadvantage of the AI configuration is that the throat diameter is at the very minimum needed to accommodate the required $L_p I_p$ with a

realistic range for B_{OH} . The maximum absolute value B_{OH} is limited by stress considerations, while B_{OH} during the flat-top should be kept relatively small to limit power dissipation. Consequently, the pulse length must be relatively short (50 s or less).

The stress levels in the AI TF coils are quite high although the maximum principal stress is well below the 36,000 psi yield strength for the CDA-102 high conductivity copper alloy. A secondary disadvantage, from the aspect of lifetime cost, is that the design point falls on the low mass side of the minimum-cost locus for the assumed power and manufacturing rates. Any unplanned increase in electricity cost moves the configuration further away from the optimum.

To overcome these drawbacks, configuration AII has been formulated with increased throat diameter, increased radial build d , and a larger M_C for approximately the same lifetime cost as AI. The primary benefit is that the required ΔB_{OH} drops to 60% of its value for AI. At the same time P_{res} is 10% lower and bending stress is 30% lower. The main disadvantage is the larger coil mass.

Because of the low current densities in the conductors, a consequence of moderate values of B_M and large cross-sectional areas, cooling in the steady state for both versions is possible with a conventional pressurized water system. Peak temperatures in both the coolant and the conductors can be readily maintained well below 100°C with only 5% of the cross-sectional area allocated to cooling passages.

8.2 INTERNAL OH COILS

Area for the flux swing can be increased even further to allow longer pulse lengths if the OH coils are placed just inside the TF coil bore. This

configuration is practical only if the TF coils are readily demountable to allow quick access to the OH coils for maintenance or removal. For the Type BI design, the available flux is sufficiently large for a pulse length of at least 500 s when $\langle T_e \rangle \approx 8$ keV. Although a significantly higher B_M is required than for AII, the stresses are no higher and the resistive losses only marginally larger. The lifetime cost is again approximately \$500M for the same fabrication and electricity rates used in the AI and AII costing. It may be noted that in contrast to the situation for the Type A configurations, the parameters range is such that an increase in d/R_M increases the lifetime cost. In fact, $d/R_M = 0.75$ is close to the optimum value.

The main disadvantages of the Type BI geometry are the relatively large B_M and the internal placement of the OH solenoid. The BII geometry eliminates the requirement of an OH solenoid by depending on the EF flux swing and a neutral-beam or radio-frequency-driven current to establish and maintain I_p . The smaller R_p that results from elimination of the OH solenoid allows a smaller B_M . Although the resulting design is the most compact and economical of the four types considered, its feasibility depends on the effectiveness of driving a plasma current by momentum transfer, which is yet to be demonstrated convincingly in tokamaks.

9. CONSIDERATIONS FOR PURE FUSION POWER REACTORS

Although this study has concentrated on tokamaks of a size smaller than appropriate for "pure fusion" electrical power generation using a relatively modest magnetic field, it is interesting to consider the question of whether resistive TF coils of optimal geometry can also find application in "pure fusion" reactors. Such a reactor can be conceived by expanding the Type BI geometry to give a sufficiently high fusion power gain Q_C and an increased neutron wall loading. The effects of different wall loadings are shown in Fig. 29. The corresponding P_{res} vs B_M curves for constant Q_C , of primary interest in connection with power-producing reactors, are shown in Fig. 30.

Resistive dissipation is essentially minimized by choosing a design point much closer to the optimum (see Fig. 20) than in the previous configurations. Table 5 compares some of the major parameters of such a reactor with those of a recently designed "pure fusion" electrical power reactor with superconducting coils²⁷ ("STARFIRE"). For a coil bore of approximately 10 m x 17 m and a B_M of 6.8 T, the reactor, with a blanket power multiplication of 1.2, achieves a Q_C of 6.3 with a wall loading of 2.5 MW/m². The resistive loss is an acceptable 360 MW, but the coil mass is a very large 21,700 tons. The plant efficiency is about the same as for STARFIRE, but the thermal and electrical power outputs are twice as large. Smaller resistive-coil pure fusion reactors can be envisaged but with degraded plant efficiencies.

Overall reactor size and efficiency that are comparable with those of currently envisaged superconducting-coil reactors can be achieved with resistive TF coils having a lifetime cost of approximately \$1,300 M, under the assumptions of Sec. 4.4. Thus, in principle, even a pure-fusion electrical power reactor is feasible based on proven copper-coil technology, and with all the advantages discussed in Sec. 1.

10. CONCLUSIONS

With major difficulties apparent in implementing superconducting TF coils for tokamak reactors, this paper has reexamined the commonly-accepted notion that resistive coils would be unacceptable because of excessive power losses and the consequent inability to operate in the steady state. It is shown that with the moderate magnetic field strengths now thought to be adequate to contain a plasma with a fusion power density of reactor interest, steady-state resistive TF coils can be designed that offer attractive performance at least for near-term test reactors and practical tokamak neutron sources.

The requirement for accessibility to all subsystems of a tokamak reactor, including internal PF coils and inboard blanket modules, necessitates the use of readily demountable TF coils. Within the current state of the art, or that realistically projected ahead for several decades, this requirement implies that the TF coils must be assembled from resistive conductors. Disadvantages of superconducting coils in addition to their lack of demountability, include their extreme sensitivity to pulsed magnetic fields and to neutron and gamma radiation, complicated refrigeration systems, necessity for on-site fabrication, excessive downtime in case of warm-up or cool-down, and in the long run the uncertain supply of helium and the possibly exorbitant cost of niobium. The need for steady-state operation of TF coils in practical reactor applications implies that resistive coils must be massive to allow easy cooling with pressurized water. Such massive coils will result in power losses that are acceptable in the sense that they are comparable with the sum of the other power losses in the reactor facility (PF coils, plasma heating systems, blanket cooling systems, pumps, HVAC). Attention in this paper has been focused on rectangular TF coil geometries which permit the greatest accessibility and the most straightforward fabrication methods. The

necessarily large conductor cross sections and the restriction of the magnetic field to less than 8.0 T at the coil windings lead to readily manageable levels of stress.

In the parametric analyses of TF coil and reactor characteristics, various optimizations have been considered for reactor configurations that incorporate OH coils in the throat of the reactor (Type A) or in the bore of the TF coils (Type B). For geometries associated with a specified neutron wall loading, either the resistive dissipation or the total lifetime cost of the TF coils can be minimized. Power losses can be reduced to the range of 200 to 300 MW in steady-state operation for reactors with fusion electrical power gain (the ratio of potential gross electrical output from the reactor to the electrical power input to the TF coils) of order 1 and neutron wall loadings of 0.5 MW/m^2 . The minimum lifetime cost of the TF coils for an assumed fabrication cost of \$35/kg and an electricity value of \$25/MW-hr is approximately \$500 M. Departures of the coil mass by less than 30% from the optimum increase the lifetime cost by only a few percent.

Several reference designs have been formulated with each TF coil divided into four segments consisting of approximately 25 plates bolted together. Each plate can be made by splicing two or three commercially available plates of 4 m length and thickness up to 6 cm. There appears to be a number of feasible configurations for demountable joints between segments. The method of coil manufacture advocated in this paper lends itself to relatively low construction costs using mass production in off-site factories with assembly on-site limited to simple operations.

Tokamak reactors with demountable resistive TF coils and major radii in the range 4.5 to 5.5 m are suitable for near-term fusion test reactors and for deployment as neutron sources for applications such as fusion-fission power

generation and isotope production. By increasing the reactor size, the obtainable fusion power output and the neutron wall loading increase much more rapidly than the TF coil power loss, so that low-field "pure fusion" reactors with large electrical power gains are feasible for major radii of about 8 meters and more.

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Table 1CANDIDATE DEMOUNTABLE JOINTS

<u>TYPE OF JOINT</u>	<u>METHOD OF APPLYING CLAMPING PRESSURE</u>	<u>EXAMPLES OF PREVIOUS USE</u>
Lap	Bolts	ISX (Outboard) PDX
Finger	Recessed pins and through-bolts	ISX (Inboard) Doublet III
Vertical Butt	Bolts and clamping ring attached to center column	See Fig. 27
Horizontal Butt	(1) Bolting together of overlays attached to butted segments	Analogous to clamping of Alcator TF coils
	(2) Clamping ring on outboard side	
Plug-In	Hydraulic clamping ring	Plug-in joints on TEXT PF coils
Lap	Hydraulic cylinders	FM-1

Table 2
TOROIDAL FIELD COIL PARAMETERS

	<u>AI</u>	<u>AII</u>	<u>BI</u>	<u>BII</u>
No. of coils	12	12	12	12
a_c (m)	3.25	2.90	3.00	2.75
b_c (m)	5.20	4.50	4.55	4.50
R_c (m)	5.25	5.40	4.80	4.35
R_M (m)	2.00	2.50	1.80	1.60
d (m)	1.00	1.25	1.35	1.20
d_v (m)	0.80	0.80	0.50	0.50
d_{OH} (m)	0.00	0.00	0.45	0.00
w (m)	1.07	1.34	1.21	0.86
d/R_M	0.50	0.50	0.75	0.75
R_M/R_c	0.38	0.46	0.38	0.37
Overall diam. (m)	19.0	19.1	18.3	16.6
Overall height (m)	12.4	11.5	11.8	11.4
Access gap (m)	3.37	3.00	2.87	2.85
B_M (T)	5.87	5.96	7.60	6.91
B_t (T)	2.42	2.98	2.86	2.70
Nl_c per coil (MA) (Mat)	4.89	6.21	5.70	4.61
Turns per coil, N	25	25	25	25
I_c (kA)	196	248	228	184
Coil mass M_c (10^6 kg)	3.08	4.22	3.82	2.45
Resistive loss P_{res} (MW)	246	221	238	207
<u>Ripple, peak-to-avg.</u>				
at $R = R_p$	0.004	0.006	0.005	0.004
at $R = R_p + a_p/2$	0.015	0.016	0.016	0.014
at $R = R_p + a_p$	0.050	0.044	0.050	0.045

Table 2 (Continued)

Forces (MN)

Centering force per coil	125	132	169	126
Horiz. separating force	39	57	51	37
Vert. separating force	52	72	87	58

Stresses (10^3 psi)

Max bending stress	25	17	15	18
Tensile stress, horiz. leg	2.6	2.5	2.3	2.6
Bearing stress in nested region	3.0	2.8	2.4	2.1

Table 3TF COIL THERMAL PARAMETERS

	<u>AI</u>	<u>BI</u>
Type of copper	CDA 102 Alloy	
Conductivity	101% IACS	
Resistivity at 50°C (Ωm)	1.9×10^{-8}	
Max current density(kA/cm^2)	0.76	0.91
Packing factor - outboard leg	0.85	0.85
- inboard leg	0.80	0.72
Conductors per coil	25	25
Cooling channels per conductor	6	10
Turns per channel	1	1
Channel length (m)	37.8	36.1
Channel hydraulic dia (cm)	1.89	1.80
Coolant velocity (m/s)	2.0	2.0
Channel pressure loss (psi)	17.4	17.4
Total pumping power (kW)	193	293
Total volume flow (m^3/s)	1.29	1.95
Coolant inlet temp (°C)	20.0	20.0
Coolant temp. rise (°C)	45.8	29.2
Max ΔT , conductor to coolant (°C)	9.0	7.9
Max ΔT , to local hot spots (°C)	11.5	16.6

Table 4PLASMA AND REACTOR PARAMETERS

	<u>AI</u>	<u>AII</u>	<u>BI</u>	<u>BII</u>
Major radius R_p (m)	4.85	5.00	4.78	4.10
Half-width a_p (m)	1.60	1.25	1.28	1.25
Half-height b_p (m)	3.20	2.50	2.55	2.50
R_p/a_p	3.03	4.00	3.75	3.28
a_v (m)	1.85	1.50	1.53	1.50
b_v (m)	4.20	3.50	3.55	3.50
q at plasma edge	3.0	3.0	3.0	3.0
I_p (MA)	7.8	5.7	5.9	6.3
Throat radius (m)	1.0	1.25	0.0	0.0
L_p (μH)	5.46	7.37	6.64	5.02
$L_p I_p$ (Vs)	42	42	39	31
ΔB_{OH} for start-up (T)	16.7	10.0	2.7	--
Loop voltage at $\langle T_e \rangle = 8$ keV	0.034	0.042	0.040	0.038
Pulse length (s)	50	~ 100	~ 500	∞
ΔB_{OH} during flat-top (T)	0.7	1.0	1.4	--
Range of B_{OH} (T)	+10.0 -7.4	+8.0 -3.0	+3.5 -0.6	-- --
β_p	1.8	2.4	2.2	1.9
$\langle \beta \rangle$	0.079	0.060	0.064	0.073
Relative $n_{TE} \propto n^2 a^2$	1.25	1.00	1.02	1.00
$\propto n_a^2 \frac{a_p^2}{P}$	1.43	1.00	1.03	1.00
Effective wall area A_w (m^2)	646	534	519	438
Fusion power P_{fus} (MW)	404	334	324	274
Q_C	0.46	0.42	0.38	0.38
Wall loading ϕ_w (MW/ m^2)	0.50	0.50	0.50	0.50

Table 5

TOKAMAK ELECTRICAL POWER REACTORS WITH BLANKET MULTIPLICATION ≈ 1.2

	Superconducting TF Coils (STARFIRE)	Resistive TF Coils (Type BI)
R_p	7.0 m	8.2 m
Coil conductor	Nb ₃ Sn + NbTi	Copper
Coil bore	10 m x 15 m	10 m x 17 m
R_c	7.8 m	8.33 m
R_M	3.42 m	3.33 m
d		2.50 m
w		2.03 m
a_p	1.94 m	2.9 m
M_C		21,700 tons
B_M	11.0 T	6.8 T
Coil Power Loss	20 MW _e (refrig)	360 MW _e
$\langle \beta \rangle$	0.067	0.085
I_p	11 MA	14 MA
$n\tau_E \propto \langle \beta \rangle B_t^2 a_p^2$	2.6	2.0
ϕ_w	3.5 MW/m ²	2.5 MW/m ²
Fusion Power	3200 MW	6740 MW
Gross Elec. Power	1600 MW _e	3350 MW _e
Total Power Consump.	450 MW _e	760 MW _e
$Q_E = \frac{\text{Gross Elec. Power}}{\text{Circulating Power}}$	3.6	4.4
Plant Efficiency	0.30	0.32

Fig. 1. Model of Tokamak Geometry. (PPPL-796359)

Fig. 2. TF Coil Resistive Dissipation for Type-A Reactors.
(PPPL-796350)

Fig. 3. Normalized Coil Mass for Type-A Reactors. (PPPL-796348)

Fig. 4. Neutron Wall Loading for Type-A Reactors. (PPPL-796344)

Fig. 5. Fusion Electrical Power Gain for Type-A Reactors.
(PPPL-796346)

Fig. 6. Ratio of Neutron Wall Loading to TF Coil Power Dissipation for Type-A Reactors. (PPPL-796345)

Fig. 7. Fusion Electrical Power Gain Normalized to Coil Mass for Type-A Reactors. (PPPL-796347)

Fig. 8. Neutron Wall Loading Efficiency Normalized to Coil Mass for Type-A Reactors. (PPPL-796342)

Fig. 9. Variation of TF Coil Power Dissipation with Maximum Field at the Conductor for Type-A Reactors Giving 0.5 MW/m^2 Neutron Wall Loading. (PPPL-796363)

Fig. 10. Variation of TF Coil Power Dissipation with Maximum Field at the Conductor for Type-A Reactors Giving 0.5 MW/m^2 Neutron Wall Loading. (PPPL-796361)

Fig. 11. Dependence of TF Coil Lifetime Costs on Power Dissipation and Coil Mass for Type-A Reactors Giving 0.5 MW/m^2 Neutron Wall Loading. (PPPL-796343)

Fig. 12. Effects of Electricity and Coil Fabrication Costs on the Optimal TF Coil Parameters for Minimum Lifetime Cost of Type-A Reactors. (PPPL-796360)

Fig. 13. TF Coil Resistive Dissipation for Type-B Reactors. (PPPL-803035)

Fig. 14. Normalized Coil Mass for Type-B Reactors. (PPPL-803030)

Fig. 15. Neutron Wall Loading for Type-B Reactors. (PPPL-803031)

Fig. 15. Fusion Electrical Power Gain for Type-B Reactors. (PPPL-803028)

Fig. 17. Ratio of Neutron Wall Loading to TF Coil Power Dissipation for Type-B Reactors. (PPPL-803032)

Fig. 18. Fusion Electrical Power Gain Normalized to Coil Mass for Type-B Reactors. (PPPL-803033)

Fig. 19. Neutron Wall Loading Efficiency Normalized to Coil Mass for Type-B Reactors. (PPPL-803029)

Fig. 20. Variation of TF Coil Power Dissipation with Maximum Field at the Conductor for Type-B Reactors Giving 0.5 MW/m^2 Neutron Wall Loading. (PPPL-803027)

Fig. 21. Variation of TF Coil Power Dissipation with Maximum Field at the Conductor for Type-B Reactors Giving 0.5 MW/m^2 Neutron Wall Loading. (PPPL-803034)

Fig. 22. Dependence of TF Coil Lifetime Costs on Power₂ Dissipation and Coil Mass for Type-B Reactors Giving 0.5 MW/m² Neutron Wall Loading. (PPPL-803026)

Fig. 23. Model of Coil Structure and Loading for Stress Considerations.
(PPPL-806676)

Fig. 24. Normalized Bending Moment Distributions for Nested Coil Model ($\nu=0$). (PPPL-803667)

ISX PARAMETERS

5" (—) & 3" (---) INNER BEAMS

Fig. 26. Normalized Bending Moment Distributions Corresponding to ISX Parameters.
(PPPL-806678)

Fig. 27. Elevation View of a Representative Reactor Configuration Illustrating Demountable TF Coils. (PPPL-803144)

Fig. 28. Schematic Layouts of Reference Tokamak Designs. (PPPL-796362)

Fig. 29. Variation of TF Coil Power Dissipation with Maximum Field at the Conductor for Type-B Reactors with Neutron Wall Loading ϕ_w .

(PPPL-806663)

Fig. 30. Variation of TF Coil Power Dissipation with Maximum Field at the Conductor for Type-B Reactors with Fusion Electrical Power Gain Q_c . (PPPL-806662)

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MASTER

RESISTIVE TOROIDAL-FIELD COILS FOR TOKAMAK REACTORS

BY

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ABSTRACT

Water-cooled resistive tokamak toroidal-field (TF) coils fabricated of copper or aluminum can be made readily demountable, thereby ensuring the maintainability of internal poloidal field coils, inboard blanket modules, and the components of a poloidal magnetic divertor. Resistive coils are unaffected by the pulsed fields of a tokamak, require less shielding against neutron irradiation than do superconducting coils, and have simple refrigeration systems. The demountable segments can be built in factories and transported disassembled to the reactor site. To achieve a satisfactory plasma pressure for various reactor applications, the magnetic field can be sufficiently low so that steady-state operation of resistive coils is quite feasible.

This paper analyzes the optimization of the geometry of resistive TF coils of rectangular bore for tokamak fusion test reactors and practical neutron generators. In examining the trade-offs between geometric parameters and magnetic field for reactors giving a specified neutron wall loading, either the resistive power loss or the lifetime coil cost can be minimized. Aspects of cooling, magnetic stress, and construction are addressed for several reference designs. Bending moment distributions in closed form have been derived for rectangular coils on the basis of the theory of rigid frames. Candidate methods of fabrication and of implementing demountable joints are summarized.

TABLE OF CONTENTS

	<u>Page</u>
1. INTRODUCTION	1
1.1 Disadvantages of Superconducting TF Coils	1
1.2 Motivation for Resistive TF Coils	3
1.3 Long-Term Prospects for Resistive Coils	4
1.4 Objectives of the Present Investigation	5
2. TOKAMAK MODEL	7
2.1 Reactor Geometry	7
2.2 Characteristic Parameters for the TF Coils	8
3. REACTOR PLASMA CONSIDERATIONS	12
4. OPTIMIZATION OF COIL PARAMETERS	15
4.1 Figures-of-Merit	15
4.2 Optimization for Neutron Wall Loading	16
4.3 Variation of Power Loss with Coil Mass	21
4.4 Minimum Lifetime Cost	23
4.5 Normalized Neutron Wall Loading Efficiency	24
5. STRESS CONSIDERATIONS	25
6. THERMAL CONSIDERATIONS	30
7. DESIGN OF CONDUCTORS AND JOINTS	33
7.1 Fabrication Considerations	33
7.2 Materials	34
7.3 Options for Joints	36
8. REFERENCE DESIGNS FOR REACTORS EMPHASIZING NEUTRON APPLICATIONS	39
8.1 External OH Coils	39
8.2 Internal OH Coils	41
9. CONSIDERATIONS FOR "PURE FUSION" POWER REACTORS	43
10. CONCLUSIONS	44
REFERENCES	53

1. INTRODUCTION

Practical tokamak fusion reactors must be characterized by long-pulse, high duty-factor operation with steady-state toroidal-field (TF) coils. The usual reasons cited against the use of normal-conducting (resistive) TF coils are excessive power dissipation and the consequent inability to operate in the steady state. However, at the values of $\langle \beta \rangle =$ (plasma pressure/magnetic-field pressure) now thought to be achievable in tokamaks (viz., $\langle \beta \rangle = 5$ to 10%), the magnetic field required for satisfactory performance of tokamak reactors in various applications can be sufficiently low so that steady-state operation of resistive coils is quite feasible.¹ In applications such as radiation test facilities, fusion-fission power production, and the generation of nuclear or chemical fuels, resistive TF coils can prove to be an advantageous alternative to superconducting coils. For very large "pure fusion" electrical power reactors (i.e., having only modest blanket energy multiplication), many reactor parameters are comparable irrespective of whether the coils are resistive or superconducting.

1.1 DISADVANTAGES OF SUPERCONDUCTING TF COILS

While a superconducting coil set has been built recently for a small tokamak (the T-7 device at the Kurchatov Institute of Atomic Energy²), its windings are highly stabilized with copper (10:1 cross-section ratio), and a thick copper shield is located between the TF coils and the plasma vessel. These characteristics are impractical for use in a tokamak reactor. Many bubble-chamber and other superconducting solenoids have been built and operated successfully, but they do not face the formidable conditions that will confront the TF coils in tokamak reactors: strangely asymmetric forces, overturning moments, rapidly pulsed magnetic fields, neutron and gamma radiation, and difficulty of access for repair of the coils.

A number of serious drawbacks of superconducting coils must be overcome before they can be utilized in tokamak reactors:

- Nondemountability - Access to internal components such as poloidal field (PF) coils, divertor hardware, and inboard blanket modules is extraordinarily difficult if not impossible unless the TF coils are readily demountable. Repair of a non-demountable coil would require the removal of an entire reactor segment. Implementation of demountable superconducting coils, however, is probably impractical (although a conceptual design exists³).
- Pulsed Fields - Superconducting coils with practical stabilizer volumes may not be able to withstand (without going normal) the large rate-of-change of vertical fields commonly found in tokamak operation during start up and during plasma disruptions.⁴ In fact, the maximum loop voltage that could be applied to start the plasma current will be limited by the maximum dB/dt tolerated by superconducting TF coils.
- Neutrons - Neutron streaming through injection and pumping ducts introduces special shielding problems for superconducting TF coils.⁵
- Construction - Superconducting coils of the size needed for tokamak test reactors must be built on site. They are far too bulky, heavy, and fragile to be moved from a specialized off-site factory where quality assurance can be much stricter.

- Refrigeration - Cryogenic refrigeration requirements may exceed 50 kW at 4°K, corresponding to 20 MW at room temperature, so that the refrigerator size must be several times larger than the present state of the art. In any event, the refrigeration system would be extraordinarily complex compared with a resistive-coil water coolant system.

- Maintenance - Maintenance of the reactor components is impossible unless the TF coils are shut off, which for superconducting coils implies a down-time of at least two days even for the most trivial maintenance operation.

1.2 MOTIVATION FOR RESISTIVE TF COILS

Water-cooled resistive TF coils fabricated of copper or aluminum can be made readily demountable,⁶ thereby ensuring the feasibility of internal placement of PF coils and of the use of a poloidal magnetic divertor; they are unaffected by pulsed fields, require less shielding against neutron irradiation than do superconducting coils, can be factory-built and transported (disassembled) to the reactor site, have simple refrigeration systems, and can be activated or deactivated in a fraction of a minute, thereby allowing rapid turnaround for minor reactor maintenance.

However, resistive coils do have the following disadvantages:

- Power Dissipation - The power consumption of resistive coils will always be considerably greater than that of superconducting coils (about 20 MW for the cryogenic refrigerator for a full-sized reactor coil set). As shown in this study, however, if sufficient copper is used to allow the resistive coils to operate at steady state, then

the power dissipation can be reduced to the range of 150 to 300 MW for full-scale D-T reactors. This power level will be comparable with the sum of the other electrical power drains in the reactors.

- Volume of Material - Several thousand tons of copper will be required for a resistive TF coil set that can operate in the steady state. This mass represents about one day's production of copper in the U.S.⁷ On the other hand, cryostable superconducting coils require a mass of copper or aluminum of at least several hundred tons in addition to large masses of niobium and titanium or tin. The prices of the latter metals are reaching rather high levels, while the prices of copper and aluminum, which are relatively abundant, are expected to remain low.
- A disadvantage of present day resistive TF-coil sets is that they are pulsed at the same frequency as that of the plasma discharges, so that the coil lifetime is limited by fatigue. This fatigue problem will be eliminated in the steady-state TF coil set of a tokamak reactor.

1.3 LONG-TERM PROSPECTS FOR RESISTIVE COILS

It will be argued that in the long term, all toroidal reactors must have superconducting coils. However, even if reliable superconducting coils permitting easy reactor maintenance and having acceptable cost are developed in fifty years time, certain other factors will continue to weigh in favor of retaining the simpler resistive TF coil sets, at least for D-T reactors:

- It seems probable that methods of generating toroidal plasmas of increasing beta will be developed so that the magnetic field required for a given reactor application will be reduced.
- It is not clear that economically recoverable helium will be available in fifty years.⁸ Even if it is, there are many other applications requiring helium with which magnetic fusion reactors will have to compete. Besides its use as a general purpose coolant, helium may be required for applications such as dirigibles, superconducting transmission lines and levitated trains. What helium is available for fusion reactors should be retained for cooling blankets and for cryopumping.
- The price of niobium, like that of most relatively rare metals, could become exorbitant in fifty years.

It seems likely that superconducting coils will be relegated to use with fusion reactors using "advanced fuels" such as D-D, where the power densities are so low that the coil power dissipation must be truly minimized.

1.4 OBJECTIVES OF THE PRESENT INVESTIGATION

A practical tokamak reactor must have readily demountable TF coils to ensure ease of access for the maintenance of internal PF coils and the components of a poloidal magnetic divertor, and for the processing of inboard blanket modules. In the case of failure of a TF coil, demountability allows its replacement in an acceptable length of time. While demountable superconducting coils are probably impractical, demountable resistive TF coils

have been found to perform well in the ISX,⁶ Doublet III,⁹ and PDX¹⁰ experimental tokamaks, as well as the FM-1 spherator.¹¹ The ISX coils, in particular, lend themselves to rapid disassembly for changeout of the machine interior.

Reference 12 has reported an analysis of steady-state resistive coils for a tokamak reactor, but using nondemountable coils of circular bore. The present study parameterizes the performance of reactors with steady-state resistive TF coils of rectangular geometry designed especially for ease of demountability. Sections 2 to 4 of this paper determine tokamak and coil configurations that give optimal values of certain figures-of-merit related to near-term reactor goals. It is found that practical reactors can be realized with resistive losses of 200 to 250 MW, at the expense of copper masses between 2400 and 4400 tons. Consideration of the trade-offs between capital cost and operating cost for TF coils in reactors of specified performance shows that the total lifetime expenditures are reasonable.

Various stress and thermal aspects of the coils are examined in Sections 5 and 6, examples of TF coil joints are illustrated in Section 7, and the parameters of representative designs of tokamak reactors are given in Sections 8 and 9.

2. TOKAMAK MODEL

2.1 REACTOR GEOMETRY

The analysis is based on the rectangular coil geometry shown in Fig. 1, although qualitatively the results are equally applicable to more general elongated shapes. The plasma, of rectangular cross section $2a_p \times 2b_p$, is surrounded by a scrape-off layer 0.25 m thick. Space 1.5 m high is allocated to a single-null poloidal divertor. The vacuum vessel, blanket, and shield assembly are assumed to have a total thickness of 1.0 m. The half-width of the magnet bore is a_c and the half-height b_c . The radial build is denoted by d , and the axial build by w .

The plasma is centered at radius R_p and the coil at R_c relative to the major axis of the reactor, leaving gaps of d_{OH} inboard and d_v outboard between the shield and the coil. The space d_{OH} accommodates the ohmic heating solenoid for design options where it is topologically-linked with the TF coil system. The outer equilibrium-field coils are located in d_v , the magnitude of which is chosen in part to give an acceptable TF ripple at the plasma. The radius corresponding to the maximum magnetic field strength B_M is denoted by R_M . To allow the coils to be self-supporting against the centering magnetic forces by nesting on assembly, the inboard faces are shaved to radius R_s , arbitrarily assumed to be $R_M + d_{OH}$. Out-of-plane loads are to be taken out through shear/compression panels positioned between the coils or through radiation shielding.

Two families of tokamak layouts are proposed. For Type A, the OH coils are located in the throat of the reactor, and $d_{OH} = 0$. Type BI provides a much larger area for the OH flux swing by placing the OH coils in the bore of the TF coils. The TF coil inboard leg can then be extended to the reactor centerline, to yield electrical and structural benefit. The BI option is

practical only for a reactor with readily demountable TF coils. Based on the theoretical concept that the plasma current can be established and sustained by a combination of neutral beam injection and the EF flux swing,¹³ Type BII completely eliminates the OH coils, so that again d_{OH} can be zero.

2.2 CHARACTERISTIC PARAMETERS FOR THE TF COILS

The TF coils are characterized by their mass M_C and their resistive power loss P_{res} , parameters which represent respectively most of their capital and operating costs.

For a conductor of arbitrary shape carrying a current density j , the power dissipated is

$$P_{res} = \int \rho_e j^2 dt \quad (2-1)$$

where ρ_e is the resistivity of the material, and the integral is carried out over the volume of the conductor. For a constant resistivity, and the current density uniform across the cross section of a conductor in keeping with the steady-state assumption, the total dissipation of a composite of N conductors of cross-sections A_i constant along a common length L , each carrying a current I , is

$$P_{res} = \rho_e I^2 L \sum \frac{1}{A_i} \quad (2-2)$$

This quantity can be minimized subject to the constraint that the total cross-section be fixed to give

$$P_{res} = \rho_e (NI)^2 \frac{L}{A} \quad (2-3)$$

The minimum power dissipation for a group of N conductors constituting a cross-sectional area A and each passing the same current, is realized therefore when the conductors have equal areas, or, alternatively, when the current density is constant not only across each conductor but across the whole cross section. Then the composite structure can be treated as a single large conductor of area A carrying a uniform current density NI/A .

The characteristics of the TF coil system are calculated on the basis of the following model:

- All turns of a coil are of equal cross-sectional area and carry the same current. In particular, there are to be 12 coils of 25 turns each. The conceptual design anticipates assembly from high-conductivity copper plates with cooling channels machined in the faces at regular intervals. Calculations have assumed CDA 102 copper alloy with 101% IACS conductivity or a resistivity of $1.9 \times 10^{-8} \Omega m$ at $50^\circ C$.
- The packing factor, the fraction of the coil cross section occupied by the copper conductor, is specified for the outboard leg and varies elsewhere so as to keep unchanged the area devoted to cooling passages and insulation. Detailed calculations assume a packing factor of 0.85 for the outboard legs, with the remaining area subdivided to allow 10% for insulation and 5% for cooling passages.
- The resistive dissipation and, for consistency, the coil mass are calculated excluding the relatively small contributions of the corner regions. In an engineering design the outboard corners could be rounded in order to reduce stress levels¹⁴ while simultaneously

eliminating unnecessary mass and electrical resistance. Since the extent of rounding is restricted on the inboard side, a correction of 5% is added to the calculated values.

For Type A geometry, the resistive power loss for N_C coils is given by

$$P_{res} = \frac{4\pi\rho}{\mu_0} C_R B_M^2 \frac{1}{d/R_M} \left[\frac{2a_c + b_c}{f_o} + \frac{2b_c}{f_i (2 - d/R_M)} \right] \quad (2-4)$$

under the restriction $\pi/N_C \ll 1$, where f_o and f_i are the packing factors in the outboard and shaved portions of the coil respectively, μ_0 is the magnetic permeability, and C_R the correction factor to account for the corners (1.05 in the calculations). The mass for N_C coils can be written as

$$M_C \left[\frac{1 - R_M/R_C}{R_M/R_C} \right]^2 = 2\pi\rho a_c^2 C_R \frac{d}{R_M} \{ (2 - d/R_M) f_i + 2 f_o \} b_c + 4a_c f_o \} \quad (2-5)$$

where ρ is the density of the conductor. The relation between b_c and a_c is

$$b_c = \frac{1}{2} \frac{b}{a} [2a_c - (d_{OH} + d_v + 2.5)] + 2.0 \quad (2-6)$$

so that for a fixed plasma elongation, P_{res} and M_C are functions of a_c alone with the parameters d/R_M and R_M/R_C , having ranges between 0 and 1, representing the shape of the coils in nondimensional form.

The corresponding relations for Type B geometry are

$$\begin{aligned}
 P_{res} = \frac{4\pi\rho_e}{\mu_o} C_R B_M^2 \frac{1}{d/R_M} & \left[\frac{2a_c + b_c - d_{OH}}{f_o \left[1 + d_{OH}/a_c \frac{1 - R_M/R_c}{R_M/R_c} \right]} + \frac{2b_c}{f_i} d/R_M \right. \\
 & \left. + \frac{R_M/R_c a_c}{1 - R_M/R_c} \ln \left\{ \frac{f_o \left[1 + d_{CH}/a_c \frac{1 - R_M/R_c}{R_M/R_c} \right]}{(f_o - 1) \left[1 - d_{OH}/a_c \frac{1 - R_M/R_c}{R_M/R_c} \right] + 1} \right\} \right] \quad (2-7)
 \end{aligned}$$

$$\begin{aligned}
 M_C \left[\frac{1 - R_M/R_c}{R_M/R_c} \right]^2 = 2\pi\rho a_c^2 C_R d/R_M & \left\{ \left[2f_o \left(1 + d_{OH}/a_c \frac{1 - R_M/R_c}{R_M/R_c} \right) + \frac{f_i}{d/R_M} \right] b_c \right. \\
 & \left. + 4a_c f_o \left[1 + d_{OH}/a_c \frac{1 - R_M/R_c}{R_M/R_c} \right] - \frac{d_{OH}^2}{a_c} \right\} \quad (2-8)
 \end{aligned}$$

3. REACTOR PLASMA CONSIDERATIONS

For a deuterium-tritium plasma with equal densities of deuterons and tritons, the fusion reaction rate per unit volume is

$$R = \frac{1}{4} \frac{p^2}{k^2} \frac{\langle \sigma v \rangle}{(T_e + T_i)^2} \quad (3-1)$$

where p is the total plasma pressure, T_i the mean ion temperature, T_e the electron temperature, k the Boltzmann constant, and $\langle \sigma v \rangle$ the fusion reactivity. In terms of β , the ratio of plasma to magnetic pressure, Eq. (3-1) is rewritten as

$$R = \frac{1}{(4\mu_0 k)^2} \frac{\langle \sigma v \rangle}{(T_e + T_i)^2} \beta^2 B_t^4 \quad (3-2)$$

where B_t is the magnetic field at the center of the plasma.

For a given plasma pressure, the maximum fusion power density P_f is attained at the maximum of $\langle \sigma v \rangle / T_i^2$, which for a D-T plasma occurs near $T_i = 15$ keV. Then for $T_e = T_i$ (in MW/m³)

$$P_f = 1.3 \beta^2 B_t^4. \quad (3-3)$$

Enhancement of $\langle P_f \rangle$ by a factor of 2 to 3 is realistic with tailored plasma profiles.¹⁵ A value of 2.3 is chosen to yield a space-averaged fusion power density (in MW/m³) of

$$\langle P_f \rangle = 3.0 \langle \beta \rangle^2 B_t^4. \quad (3-4)$$

For the driven (subignited) plasmas that are most pertinent to this work, the above relations would require some modification. Nevertheless, Eq. (3-4) is used throughout this analysis.

For the rectangular geometry defined in Section 2 the total fusion power output (in MW) is

$$P_{fus} = 24.0 \pi \langle \beta \rangle^2 \frac{b_p}{a_p} \left(\frac{a_p}{R_p} \right)^2 R_p^3 B_t^4 \quad (3-5)$$

of which 80% is associated with the neutron flux. An average neutron wall loading ϕ_w , defined on the basis of a fictitious first wall placed at the 25 cm scrape-off distance all around the plasma, becomes (in MW/m²)

$$\phi_w = 2.4 \langle \beta \rangle^2 \frac{b_p}{a_p} \left(\frac{a_p}{R_p} \right)^2 \frac{R_p^2 B_t^4}{a_p \left(1 + \frac{b_p}{a_p} \right) + 0.50} \quad (3-6)$$

MHD stability considerations of tokamak equilibria place an upper bound on the average plasma pressure through the relation¹⁶

$$\max \langle \beta \rangle = 0.12 \frac{a_p}{R_p} \frac{b_p}{a_p} \quad , \quad (3-7)$$

thus limiting the achievable fusion power density and the neutron wall loading. This relation is valid for plasma elongations up to approximately 2. The present study of TF coil parameters is carried out for a single ratio $b_p/a_p = 2.0$.

The variables B_t , R_p , and a_p that refer to the plasma can be rewritten in terms of coil parameters using the expressions

$$B_t = \frac{(R_M/R_C) a_c}{(1 - R_M/R_C)} B_M \cdot \frac{1}{a_c / (1 - R_M/R_C) + (d_{OH} - d_v)/2} \quad (3-8)$$

$$R_p = \frac{a_c}{1 - R_M/R_C} + \frac{d_{OH} - d_v}{2} \quad (3-9)$$

$$a_p = a_c - 1.25 - \frac{d_{OH} + d_v}{2} \quad (3-10)$$

4. OPTIMIZATION OF COIL PARAMETERS

4.1 FIGURES-OF-MERIT

Various figures-of-merit may be defined on the basis of the fusion power output, the neutron loading on the first wall, the resistive power dissipated in the coils, and the coil mass. When tempered with engineering considerations, different optimizations provide a basis for the choice of reference designs.

Detailed calculations have been carried out for Type A configurations with $d_v = 0.80$ m and for Type B with $d_{OH} = 0.45$ m and $d_v = 0.50$ m. Figures 2, 3, 4, and 13, 14, 15 show P_{res} , M_C , and ϕ_w for the two configurations as functions of a_c , with d/R_M and R_M/R_C as parameters.

The fusion electrical power gain factor Q_C for the coils, defined as the ratio of the potential gross electrical output from the reactor to the electrical power input to the TF coils, is

$$Q_C = 0.28 \frac{P_{fus}}{P_{res}} M_{bl} \quad (4-1)$$

for a thermal-to-electrical conversion efficiency of 35%. M_{bl} is the average fusion neutron energy multiplication in the blanket modules. Q_C for $M_{bl} = 1$ is shown in Figs. 5 and 16.

The ratio of the net electrical power output P_{out} to the total recirculating power P_{circ} , representing the sum of P_{res} and the power lost to other reactor subsystems, is given by

$$\frac{P_{out}}{P_{circ}} = Q_C \frac{P_{res}}{P_{circ}} - 1 \quad (4-2)$$

If the resistive dissipation is comparable with other types of power consumption so that, for example, $P_{res}/P_{circ} = 1/2$, a fusion reactor that gives a net power output of the same magnitude as the recirculating power would require $Q_C = 4$.

For reactor applications where the neutron wall loading is the most appropriate measure of performance, an efficiency parameter analogous to Q_C is ϕ_w/P_{res} , which represents the wall loading obtainable per unit operating cost. (Figs. 6 and 17). For fixed coil shape parameters and for fixed B_M , the efficiencies Q_C and ϕ_w/P_{res} and the coil mass M_C are increasing functions of bore size a_c . Consequently, both Q_C and ϕ_w/P_{res} can be increased at the cost of a larger M_C . A measure of the effectiveness of the utilization of the conductor would be the parameters Q_C/M_C and $\phi_w/(P_{res} M_C)$ (Figs. 7, 8, and 18, 19). Both pass through maxima as functions of a_c , thus defining possible "optimal" geometries, with the maximum of $\phi_w/(P_{res} M_C)$ falling in a range that could be of practical interest. The magnitudes of these parameters are proportional to B_M^2 so that they can be increased readily by raising the magnetic field strength.

4.2 OPTIMIZATION FOR NEUTRON WALL LOADING

Instead of defining global optima, it is of greater practical relevance to examine optimizations under suitable constraints. A comparison of all the configurations that give a specified time-averaged wall loading can lead to geometries that minimize the coil dissipation or that minimize the total

lifetime cost associated with the coils. In reactors intended for fusion-neutron applications, ϕ_w should be in the range of 0.3 to 2.5 MW/m², depending on specific goals.¹⁷ The general results of this study are adapted to define reactors that achieve a spatially-averaged ϕ_w of 0.5 MW/m². The local value on the outboard wall can be expected to be about 25% higher.¹⁸ In any case, for a given reactor geometry and β the wall loading from a thermonuclear plasma can be raised as B_M^4 if higher values are required.

Under the simplifying assumptions that the displacement of the plasma centerline relative to the coil centerline is small compared with the coil major radius,

$$\frac{d_{OH} - d_v}{2 a_c} (1 - R_M/R_C) \ll 1, \quad (4-3)$$

and that the scrape-off layer thickness is small compared with the plasma half-width,

$$\frac{0.25}{a_c - \delta} \ll 1 \quad (4-4)$$

where δ is the average horizontal space between the plasma and the coil

$$\delta = 1.25 + \frac{d_{OH} + d_v}{2} \quad (4-5)$$

the condition of constant ϕ_w can be written as

$$\frac{a_c^2}{(a_c - \delta)^3} = G(\phi_w, R_M/R_C) B_M^4, \quad (4-6)$$

where

$$G(\phi_w, R_M/R_C) = \frac{0.09216}{\phi_w} (R_M/R_C)^4 (1 - R_M/R_C)^2. \quad (4-7)$$

Eq. (4-6) has a solution

$$a_c = \frac{\delta}{1 - \left(\frac{1}{2\delta G B_M^4} \right)^{1/3} \{ (1+H)^{1/3} + (1-H)^{1/3} \}} \quad (4-8)$$

where

$$H = \left(1 + \frac{4}{27 \delta G B_M^4} \right)^{1/2}. \quad (4-9)$$

Since for Type A geometry, and for Type B geometry in the limit $d_{OH}/a_c \ll 1$, P_{res} for constant d/R_M is proportional to $B_M^2 (a_c - C)$,

$$P_{\text{res}} \sim B_M^2 \left[\frac{\delta}{1 - \left(\frac{1}{2\delta G B_M^4} \right)^{1/3} \{ (1+H)^{1/3} + (1-H)^{1/3} \}} - C \right]. \quad (4-10)$$

C is a constant proportional to δ^{-1} with a weak d/R_M dependence, and arises due to the fact that the height-to-width ratio for the magnet bore differs from the corresponding ratio for the plasma. The effects of the shape parameters for a fixed δ could be essentially absorbed to give a universal function relating P_{res}/B_M^2 to $G B_M^4$. For small B_M ,

$$P_{\text{res}} \sim B_M^{-2} \quad \text{and} \quad a_c \sim B_M^{-4}$$

while for large B_M ,

$$P_{\text{res}} \sim B_M^2 \quad \text{and} \quad a_c \rightarrow \delta$$

so that P_{res} has a minimum at intermediate values of B_M corresponding to an $a_c > \delta$.

Using Eq. (4-6), the value of a_c at the minimum is readily shown to be a solution of the equation

$$a_c^{\min 2} - a_c^{\min} (4\delta - C) + 2\delta C = 0 \quad (4-11)$$

A particularly convenient form for the root is obtained by making use of the fact that in practice $(C/4\delta)^2 \ll 1$, so that to first order in C/δ

$$a_c^{\min} = 4\delta \left(1 - \frac{3}{8} \frac{C}{\delta} + \dots \right) \quad (4-12)$$

Curves of P_{res} as a function of B_M for constant ϕ_w have minima at a value of a_c that is independent of ϕ_w and R_M/R_C and only very weakly dependent on d/R_M (through C). Therefore, in a reactor configuration that gives the minimum P_{res} for the TF coils for any prescribed wall loading and any combination of shape parameters, the plasma occupies approximately 3/4 of the radial bore.

The magnitude of the minimum P_{res} does depend on the shape parameters and wall loading. For Type A reactors,

$$P_{res}^{\min} = \frac{509.8 \rho_e}{2 \mu_o} C \frac{1}{R} \frac{1}{d/R_M} \left[\frac{1}{f_o} + \frac{1}{f_1 (2 - d/R_M)} \right] \frac{\phi_w^{1/2} \delta^{1/2}}{(R_M/R_C)^2 (1 - R_M/R_C)} \left[1 - \frac{C}{4\delta} + \dots \right] \quad (4-13)$$

and for Type B,

$$P_{res}^{min} = \frac{509.8 \rho_e}{2 \mu_0} C_R \frac{1}{d/R_M} \left[\frac{1}{f_o} + \frac{d/R_M}{f_i} \right] \frac{\phi_w^{1/2} \delta^{1/2}}{(R_M/R_C)^2 (1-R_M/R_C)} \left[1 - \frac{C}{4\delta} + \dots \right].$$

(4-14)

The corresponding value of B_M in both cases is

$$B_M^{min} = 1.592 \frac{\phi_w^{1/4}}{\delta^{1/4} (R_M/R_C) (1-R_M/R_C)^{1/2}} \left[1 + \frac{3}{16} \frac{C}{\delta} + \dots \right]. \quad (4-15)$$

The exact solutions for $\phi_w = 0.5 \text{ MW/m}^2$ are shown in Figs. 9, 10, and 20, 21. A configuration of minimum resistive loss is achieved at the expense of large coil dimensions and mass but modest magnetic field strength. The fusion power output may also be too large to be of practical interest. A geometry that gives higher resistive dissipation but smaller coil mass may be preferable to achieve the same wall loading so long as the power loss is still comparable with the total electrical power consumed in the plasma heating systems, the PF coils, and the blanket coolant loops.

4.3 VARIATION OF POWER LOSS WITH COIL MASS

An examination of the trade-offs between P_{res} and M_C as the coil geometry changes but ϕ_w and B_M remain fixed leads to the matter of the lifetime cost of the coil system. An arbitrary limit of 250 MW on P_{res} for realistic geometries suggests that suitable values for B_M would be 6T for a Type A configuration (Figs. 9 and 10) and 7.75T for Type B (Figs. 20 and 21). It is

convenient to look at a sequence of curves of constant d/R_M , with R_M/R_C changing along each curve.

The condition of constant ϕ_w and B_M can be rewritten from Eq. (4-6) as

$$\left(\frac{R_M}{R_C}\right)^2 \left(1 - \frac{R_M}{R_C}\right) = F(a_C; \phi_w, B_M) \quad (4-16)$$

where

$$F(a_C; \phi_w, B_M) = \left[\frac{\phi_w}{0.09216 B_M^4} \frac{a_C^2}{(a_C - \delta)^3} \right]^{1/2} \quad (4-17)$$

F reaches a maximum permissible value when $R_M/R_C = 2/3$, so that there exists a minimum a_C compatible with the imposed conditions. Its value can be calculated from Eq. (4-9).

The three roots of Eq. (4-16) are

$$\frac{R_M}{R_C} = \frac{1}{3} + \frac{2}{3} \cos \left\{ \frac{1}{3} \left[\cos^{-1} \left(-\frac{27}{2} \left[F - \frac{2}{27} \right] \right) \right] + \frac{2m\pi}{3} \right\} \quad (4-18)$$

corresponding to $m = 0, 1, 2$. From the fact that the range of values for F is $0 < F < 4/27$, the $m = 1$ root can be shown to be always negative and thus not of interest; the remaining two roots are positive and coalesce to the value $R_M/R_C = 2/3$ when a_C reaches its minimum.

Since M_C depends explicitly on R_M/R_C and a_C , and since a_C is linearly proportional to P_{res} , M_C can be written explicitly as a function of P_{res} . For each value of P_{res} above a minimum there exist two values of M_C . The limiting behaviors of the curves are given by

$$P_{res} \sim M_C^{2/5} \text{ for } \frac{R_M}{R_C} \rightarrow 0$$

$$P_{res} \sim M_C^{1/4} \text{ for } \frac{R_M}{R_C} \gg 1$$

Portions of the exact solutions for Type A with $B_M = 6T$ appear in Fig. 11 and for Type B with $B_M = 7.75T$ in Fig. 22. The minimum P_{res} occurs at values of M_C beyond the range shown. There also exists a minimum M_C for each d/R_M , required in order to allow the curve to evolve from its behavior near the minimum P_{res} to its asymptotic form as $R_M/R_C \rightarrow 0$.

4.4 MINIMUM LIFETIME COST

The capital cost of the TF coil system is essentially proportional to M_C with a much smaller component proportional to P_{res} that accounts for power conversion and cooling equipment. The operating cost is proportional to P_{res} . In the $P_{res} - M_C$ plane, a given total lifetime cost of the coil system is represented by a straight line. As an example, the fabrication cost of a TF coil is taken as \$35/kg, a value in keeping with the large quantities of copper involved and the relatively simple design. The cost of electricity is specified as \$25/MWh. The coil lifetime is assumed to be 12 years at 60%

capacity factor. Figures 11 and 22 show that for each d/R_M there is a coil mass in the range 3,000 to 4,000 tons for Type A and 3,000 to 5,000 tons for Type B that yields a minimum lifetime cost of around \$500M. A locus of minimum lifetime cost can be defined showing its d/R_M - variation as well as that of the parameters P_{res} and M_C associated with minimum-lifetime-cost configurations.

Although the optimal geometry depends on coil manufacturing costs and the cost of electrical power, as shown in Fig. 12, with major effects on the total lifetime cost, for given rates the reactor parameters can vary over a significant range with only minimal increases in lifetime cost. For example, Fig. 11 shows that a variation in M_C by 1,000 tons around the optimum produces changes in the lifetime cost of only a few per cent.

4.5 NORMALIZED NEUTRON WALL LOADING EFFICIENCY

It is useful to compare the optimization curves relating the normalized wall loading efficiency $\phi_w/(P_{res} M_C B_M^2)$ to a_c for constant d/R_M , R_M/R_C , and B_M with those for constant d/R_M , ϕ_w , and B_M . Both are shown on Figs. 8 and 19. The point of minimum a_c which exists for fixed ϕ_w and B_M separates the corresponding curves into upper and lower branches associated with the two roots of R_M/λ_c for each a_c . The upper branch with $R_M/R_C < 2/3$ passes through a maximum and in the limit $R_M/R_C \rightarrow 0$ for which $a_c \sim (R_M/R_C)^{-4}$ shows as asymptotic decay $\phi_w/(P_{res} M_C B_M^2) \sim a_c^{-7/2}$. The lower branch for $R_M/R_C > 2/3$, which is omitted from the figures, drops with increasing a_c and in the limit $R_M/R_C \rightarrow 1$, for which $a_c \sim (1 - R_M/R_C)^{-2}$, approaches $\phi_w/(P_{res} M_C B_M^2) \sim a_c^{-5}$. For comparison, it is noted that the curves of constant R_M/R_C decay as $\phi_w/(P_{res} M_C B_M^2) \sim a_c^{-3}$ for large a_c .

5. STRESS CONSIDERATIONS

The large volume of the conductors associated with resistive TF coils designed for steady-state operation, and the moderate magnetic field strengths adequate for satisfactory reactor performance allow the coils to be self-supporting against in-plane loads. Out-of-plane stresses arising from overturning moments or from fault conditions can be taken out through shear/compression panels¹⁹ or through radiation shielding.²⁰

Due to in-plane forces, by far the largest stresses originate with the bending moments (see Table 2). Estimates can be obtained from the theory of rigid frames. With the coils wedged together to a radius R_g under the large centering forces, the coil system can be represented as a series of rectangular frames coalescing into a rigid core formed by the inner beams. There is, however, no difficulty in relaxing the built-in boundary condition and postulating a continuous elastic support along a portion of the inner beams. The more general approach is taken here to give results of wider applicability for which the case of interest represents one limit.

The model and the loadings are illustrated in Fig. 23, with the coil represented by a rectangular frame of mean dimensions $h \times l$ built up of beams with moments-of-inertia I_h , I_l , and I_i . The inner element at radius R_i is supported along a length δ by a continuous elastic bucking cylinder. For a R^{-1} toroidal magnetic field variation in the bore of the coil, the outward force per unit length along the horizontal legs is

$$F_v = \frac{\pi}{\mu_0 N_c} \frac{B_M^2 R_M^2}{R} \quad (5-1)$$

where N_c is the number of coils. Along the outer vertical leg,

$$F_H = \frac{\pi}{\mu_{ONC}} \frac{B_M^2 R_M^2}{R_i + h}, \quad (5-2)$$

while along the inside leg,

$$F_H = \frac{\pi}{\mu_{ONC}} \frac{B_M^2 R_M^2}{R_i}. \quad (5-3)$$

The moment distributions are most readily calculated by treating the results given by Kleinlogel²¹ for concentrated loads as influence functions in integrals over the load distributions. On the horizontal legs, the bending moment, taken positive for tension on the inside of the frame element, is given by

$$\begin{aligned} \frac{M_x \mu_{ONC}}{2 B_M^2 R_M^2 R_i} &= \left(1 + \frac{x h}{h R_i}\right) \ln \left(1 + \frac{x h}{h R_i}\right) \\ &+ \frac{x}{h} \left[\frac{1}{12[(\lambda+2) + \nu(2\lambda+3)]} \left(\frac{h}{R_i}\right)^2 \frac{1}{1+h/R_i} \left\{ 3(1+\nu) - \frac{3\nu(1+\lambda)}{2} \left[3 - \left(\frac{h}{R_i}\right)^2\right] \right. \right. \\ &- \left. \left. \left\{1+h/R_i\right\} \left\{1 - \left(\frac{h}{R_i}\right)^2\right\}\right\} - \frac{(\lambda+1)S - \lambda(1+\nu)T}{(\lambda+2) + \nu(2\lambda+3)} - (1+h/R_i) \ln(1+h/R_i) \right] \\ &+ \frac{1}{3[(\lambda+2) + \nu(2\lambda+3)]} \left[(2\lambda + 3) S - \lambda T \right. \\ &- \left. \frac{1}{4} \left(\frac{h}{R_i}\right)^2 \frac{1}{1+h/R_i} \left\{ 1 - \frac{\nu(2\lambda+3)}{2} \left[3 - \left(\frac{h}{R_i}\right)^2\right] - \left\{1+h/R_i\right\} \left\{1 - \left(\frac{h}{R_i}\right)^2\right\}\right\} \right] \end{aligned} \quad (5-4)$$

where x is measured from $R = R_i$, and

$$\lambda = \frac{h}{\ell} \frac{I_{\ell}}{I_h} \quad (5-5)$$

$$\nu = \frac{\ell}{h} \frac{I_h}{I_{\ell}} \quad (5-6)$$

$$S = \frac{5}{2} + \left(\frac{R_1}{h}\right)^2 \left\{ \frac{h}{R_1} \left[1 + \frac{5}{6} \left(\frac{h}{R_1}\right)^2 \right] - \left(1 + \frac{h}{R_1}\right) \left(1 + 2 \frac{h}{R_1}\right) \ln \left(1 + \frac{h}{R_1}\right) \right\} \quad (5-7)$$

$$T = \frac{1}{2} + \left(\frac{R_1}{h}\right)^2 \left\{ -\frac{h}{R_1} \left[1 - \frac{2}{3} \left(\frac{h}{R_1}\right)^2 \right] + \left[1 - \left(\frac{h}{R_1}\right)^2 \right] \ln \left(1 + \frac{h}{R_1}\right) \right\} \quad (5-8)$$

For the outer vertical leg the corresponding relation is

$$\begin{aligned} \frac{M_y \mu_o N_c}{\pi B_M^2 R_M^2 R_i} &= \frac{1}{3[(\lambda+2) + \nu(2\lambda+3)]} \left[\lambda \{ T(2 + 3\nu) - S \} \right. \\ &\quad \left. + \frac{1}{4} \left(\frac{\ell}{R_1}\right)^2 \frac{1}{1 + h/R_1} \left\{ 2 + 3\nu - \frac{\nu\lambda}{2} \left[\left\{ 3 - \left(\frac{\delta}{\ell}\right)^2 \right\} - \left\{ 1 + h/R_1 \right\} \left\{ 1 - \left(\frac{\delta}{\ell}\right)^2 \right\} \right] \right\} \right] \\ &\quad - \frac{1}{2} \left(\frac{\ell}{R_1}\right)^2 \frac{1}{1 + h/R_1} \frac{\nu}{\ell} \left(1 - \frac{\nu}{\ell} \right) \end{aligned} \quad (5-9)$$

where y is measured from one end. For the inner leg the result is

$$\begin{aligned}
\frac{M_{Y O C}}{\pi B_M^2 R_M^2 R_1} = & - \frac{1}{\lambda[(\lambda+2) + \nu(2\lambda+3)]} \left(\frac{\ell}{R_1}\right)^2 \frac{1}{1 + h/R_1} \left\{ 1 - \frac{\nu(2\lambda+3)}{2} \left[3 - \left(\frac{\delta}{\ell}\right)^2 \right] \right. \\
& \left. - \left\{ 1 + h/R_1 \right\} \left\{ 1 - \left(\frac{\delta}{\ell}\right)^2 \right\} \right\} \\
& + \frac{(2\lambda+3) S - \lambda T}{3[(\lambda+2) + \nu(2\lambda+3)]} - \frac{1}{2} \left(\frac{\ell}{R_1}\right)^2 \frac{1}{1 + h/R_1} \frac{\chi}{\ell} \left[1 - \left(1 + h/R_1\right) \frac{\chi}{\ell} \right] \\
\left\{ \begin{array}{ll} 0 & \text{for } 0 < \frac{\chi}{\ell} < \frac{1}{2} \left(1 - \frac{\delta}{\ell}\right) \\ - \frac{1}{2} \frac{\delta}{\ell} \left(\frac{\ell}{R_1}\right)^2 \frac{h/R_1}{1 + h/R_1} \left[\frac{\chi}{\ell} - \frac{1}{2} \left(1 - \frac{\delta}{\ell}\right) \right]^2 & \text{for } \frac{1}{2} \left(1 - \frac{\delta}{\ell}\right) < \frac{\chi}{\ell} < \frac{1}{2} \left(1 + \frac{\delta}{\ell}\right) \\ \left(\frac{\ell}{R_1}\right)^2 \frac{h/R_1}{1 + h/R_1} \left[\frac{\chi}{\ell} - \frac{1}{2} \right] & \text{for } \frac{1}{2} \left(1 + \frac{\delta}{\ell}\right) < \frac{\chi}{\ell} < 1 \end{array} \right. \quad (5-10)
\end{aligned}$$

Nesting increases the effective stiffness of the inner leg of an individual frame. In the limit $\nu \rightarrow 0$, the bending moment distributions on the horizontal and the outer vertical elements are those for a frame with built-in horizontal legs; the moment distribution on the inner leg is superfluous. Fig. 24 shows the normalized bending moments for a representative range of values of the parameters h/R_1 , h/ℓ , and λ for a nested coil system ($\nu = 0$) with R_1 taken to be R_g . These examples cover the reference cases defined and examined in detail in Section 8. The effects of a finite stiffness of the inner leg and the effects of changes in length of the bucking cylinder are illustrated in Fig. 25.

A particular case, using parameters corresponding approximately to the TF coils of ISX, for which finite element solutions exist based on exact force

calculations,²² is shown in Fig. 26 to demonstrate the accuracy of the rigid frame approach. The compound shape of the inner beam, where approximately the central half has a radial build of 3 inches and the outer sections 5 inches, is best modelled by taking the build to be 3 inches over the whole length with the increased stiffness of the outer portions partly taken into account by a fictitiously-lengthened bucking cylinder.

The highest stresses are found at the extremities and at roughly the mid-spans of the horizontal and vertical legs, with the maximum usually occurring at the outboard corners. Regions of low bending stress and consequently almost pure tension suggest advantageous locations for the joints, as in the ISX TF coils, although even at the corners of the reference designs the stress levels are acceptable. For coils bearing on a bucking cylinder, the solutions depend on the choice of the length of the support as well as on the characteristics of the frame itself. By going from a nested system to one with a bucking cylinder, the bending stresses at the outer corners are affected only slightly. A proper choice of δ/l , however, can significantly reduce the stresses near the inner corners where joints are to be located, and maintain them small along the entire inner beams.

6. THERMAL CONSIDERATIONS

Whereas in the pulsed TF coils of present-day tokamaks the heating and cooling take place on different time scales, in steady-state operation there is an equilibrium at all times between the internal heat generated and that convected away by the flowing coolant. Even coils designed for pulsed scenarios can be operated at steady state (as in fact they are for discharge cleaning) at a fraction of their design current, determined either by temperature limitations or by the performance of the external heat rejection system.

The TF coils conceived in this study are assembled from sets of high-conductivity copper plates incorporating surface cooling channels. After passing through the coils, the cooling water is circulated through an external heat rejection system of adequate capacity. Even though the coils are not subjected to the pulsed loadings conducive to metal fatigue and therefore in principle can be operated at higher conductor temperatures with larger temperature gradients, in the interests of system integrity and reliability the conductor, insulator, and coolant temperatures can be maintained well below 100°C for room temperature coolant at the inlet with only modest cooling system performance. An area equal to 5% of the cross section of the outboard leg is adequate for cooling passages.

Estimates of the temperature distributions follow from one-dimensional hydraulics and heat conduction considerations (e.g., Montgomery,²³ Luton²⁴). The bulk temperature of the coolant is a monotonically increasing function of distance from the inlet, deviating from linearity primarily due to changes in cross-sectional area. A global energy balance gives the overall bulk temperature rise. Based on mean local values of resistive dissipation, temperature drops across the boundary layer can be calculated using empirical

relations for the turbulent flow heat transfer coefficient. The local temperature difference in the conductor between the channel wall and any interior point is estimated from a unidirectional conduction model. The nested regions of the coils yield the highest temperature differences so that the temperature profile along the conductor can be non-monotonic. If heat conduction along the conductor were incorporated in the model, the net effect would be a smoothing of the temperature distribution.

Without reference to the locations of the cooling passage inlets and outlets, and with the assumption of a channel length of one turn, the calculated temperature differences for the reference designs are such that even in the worst combination they give peak temperatures that are well below critical values. The thermal stresses should not be an area of concern. The coolant velocity, the pressure loss per channel, and the total pumping power are all reasonable. Specific results of the calculations are given in Section 8 and in Table 3.

A parameter providing an approximate measure of the feasibility of operating the coils at steady state can be defined. The increase in resistive dissipation due to nesting is ignored in the interests of simplicity, and the cross section allocated to cooling passages is taken as one third of the non-conducting area. A balance between the heat generated by resistive dissipation and the heat absorbed by the coolant flowing at velocity V along a channel of length L as its bulk temperature rises by ΔT_b gives

$$\frac{d^2 f_o (1 - f_o)}{B_M^2 L} = \frac{3 \rho_e}{\mu_o^2 \rho_w c_{pw}} \frac{1}{\Delta T_b V} \quad (6-1)$$

where ρ_w is the density of the coolant and C_{pw} is its specific heat. Since there are practical limits that must be imposed on the magnitudes of ΔT_b and V , the value of the parameter $d^2 f_o(1-f_o)/B_M^2 L$ is bounded from below.

The temperature differences across the boundary layers and within the conductor itself generally are considerably lower than ΔT_b and can be taken into account in the choice of ΔT_b . Assuming water as coolant and limiting ΔT_b to 45°C and V to 2 m/sec, Eq. (6-1) can be rewritten to give

$$\frac{d^2 f_o(1-f_o)}{B_M^2 L} > 9.6 \times 10^{-5} \quad (\text{MKS}) \quad (6-2)$$

The factor $f_o(1-f_o)$, with a maximum at $f_o = 0.5$, represents a particular case of the well-known trade-off between areas allocated to current conduction and to cooling so as to give the minimum temperature increase per unit length.

For large coils with moderate values of B_M , coils of the type treated in this study, the current densities in the conductors are sufficiently low that the condition of Eq. (6-2) is readily satisfied for channel lengths of one turn.

7. DESIGN OF CONDUCTORS AND JOINTS

7.1 FABRICATION CONSIDERATIONS

There are three methods of TF-coil fabrication: (1) tape winding, (2) assembly from Bitter plates, and (3) riveting or bolting of plate segments or bars. Type (2) is appropriate for the highest fields, while type (3) can be used when relatively small fields are satisfactory. The first two methods, however, do not lend themselves to demountability. The present conceptual designs make use of type (3) construction with the TF coils assembled from high-conductivity alloy plates.

The components of demountable resistive coils of this type can be prefabricated under strict conditions of quality assurance at specialized factories, transported to the reactor site by existing rail or truck systems, and assembled with minimal work.

Construction itself will be relatively simple: There is no winding process and the milling requirements are straightforward. The large number of identical sections (e.g., several hundred inboard and outboard segments for each reactor) allow essentially mass production with automated machining procedures. Defects can be readily detected by proof-testing, and the segment discarded or refabricated. Hence no defects should be present in the segments shipped to the reactor site.

The insulating material could be fabricated in the form of plates to be compressed between adjacent conductor turns upon coil assembly. Alternatively, insulating material could be bonded to each conductor segment.

Each segment is sufficiently short (12 m or less) and light (10 tons or less) so that it can be transported essentially anywhere with existing rail flatcars or flatbed trucks. The only on-site construction operations required

for the TF coils are joining of the segments. Mass production of segments in a specialized factory and minimization of field assembly work will reduce the total magnet cost, reduce the time needed for reactor fabrication and installation, and maximize the reliability of the completed coil set.

7.2 MATERIALS

7.2.1 Conductors

The price of raw copper is just under \$1 per pound, while the price of machinable copper stock (in large quantities) is about \$2 per pound. The dominant cost will be that of fabrication, however, which at the present time would be in the range of \$12 to \$16 per pound for the relatively simple segments envisaged here. Hence a 4000-ton TF coil assembly would cost \$96 to \$128 million dollars to fabricate. These costs are expected not to rise more rapidly than the general rate of inflation because of the large copper resources in the U.S. and abroad, and because of the increasing displacement of copper by more efficient materials in communications and transportation.

Manufacturers can roll high-conductivity copper plate in thicknesses up to 6 cm for sizes up to 4 m in length and 1 m in width, larger dimensions being prohibited by equipment limitations at both copper refineries and fabricating plants. In any case, the desirability of larger lengths or widths must be weighed against the increasing probability of serious metallurgical flaws for the high-conductivity alloys. With a typical alloy such as CDA 102, the final product is at least half-hard.

The TF coil segments envisaged in this paper would be constructed by joining two or three of the maximum-sized plates by soft soldering or lap riveting. With either technique the strength of the plates would be retained.

Aluminum is the only practical alternative to copper as the TF coil conducting material. Aluminum structural alloy has a conductivity which is only 0.6 times that of OFHC copper, but the density of aluminum is just 0.30 times that of copper, and the two metals have similar costs per pound. Assuming that the fabrication costs per pound are similar, the cost of an aluminum coil would be only one-half that of a copper coil of the same P_{res} . However, copper would still be used for the inboard coil segments where space is at a premium, so that the cost saving would be no more than 35%.

The use of aluminum as the conductor must be evaluated on the basis of the following advantages and disadvantages:

Advantages of Aluminum

- (1) Cost per segment about one-half that of copper.
- (2) Substantially less long-term activation by neutron irradiation than copper.

Disadvantages of Aluminum

- (1) Strong tendency to oxidize, which may cause excessive electrical resistance at the TF coil joints.
- (2) Lower strength than copper, thus placing a more severe limit on the allowed B_M .
- (3) The TF coil joints tend to gall if many assembly and disassembly operations are made.
- (4) Smaller access width between adjacent TF coils.

In view of the disadvantages of aluminum, and because the inboard segments would have to be fabricated of copper in any event, it seems preferable to specify the use of copper for all the coil segments.

7.2.2 Insulators

While organic insulation may be more reliable mechanically, the plate construction advocated here lends itself to the use of inorganic insulation, which may be desirable in order to extend the lifetime of the coil assembly against irradiation. The insulators separating the copper turns can be thin plates (~0.4 cm) of G-10 organic insulation or ceramics such as alumina or magnesia.

7.3 OPTIONS FOR JOINTS

While at least four toroidal confinement devices have been constructed with demountable TF coils, only the FM-1 and ISX devices had coils that were readily demountable. Additional demands on the successful engineering of the joints for the TF coils of a reactor are imposed by the requirement of demountability by means of remote machinery, or at least by semi-contact techniques.

The most probable failure in a TF coil would be associated with the joints; potential problems include an increase in contact resistance, coolant leakage, and bolt fatigue. Thus the greatest attention must be paid to the design of reliable joints, or at least joints that can be repaired easily by remote techniques.

A contact pressure of at least 2000 psi is necessary between two plates being joined, in order that the electrical resistance of the joint be negligible. Exertion of this pressure requires either bolting or hydraulic techniques. In certain designs a coil case may be necessary to take up the shear stresses.

Table 1 lists the various types of joints that have been examined. Several of these have been studied in detail, and the conclusions are reported in Ref. 26. A butt joint probably takes up the least amount of space, but it requires very careful alignment and a fairly elaborate clamping system to exert the required compressive stress. A lap joint, on the other hand, offers the simplest overall joint assembly, but requires the most space and is unsuitable for the inboard joints. The pins used in finger joints must also take up shear stress.

7.3.1 Bolted Joints

Figure 27 shows a TF coil with bolted lap joints at positions of low bending stress on the outboard legs and bolted butt joints on the inboard side. The design of the joints between the upper or lower segments and the outboard segment is straightforward. The joint can be placed in the region of lowest stress, in the range of 4,000 to 8,000 psi when $B_M = 7.5$ T, and there is no access problem. The most suitable joint for the outboard segment is a lap-type joint such as that implemented in the ISX machine.

The design of the joints between the upper and lower segment and the inboard segment, on the other hand, presents extreme difficulties: first because of the very large stresses (perhaps 20,000 to 25,000 psi) in the most desirable regions, namely at the corners (thus allowing the greatest access to the coil bore); and second because of the severe restriction on space and accessibility for the joint, especially if the TF coils are wedged. These problems could be alleviated markedly by displacing the joint outward by several tens of centimeters.²⁶ In that case, however, the OH solenoid cannot be removed without prior disassembly. In Fig. 27, the inboard corners are also held in place by hydraulically clamped stainless steel plates keyed into the center column. The large stresses in the bolts would require the use of

Inconel or an equivalent alloy. To minimize fatigue problems, the bolts would be replaced every time the top (or bottom) segment is disassembled.

A modification of this scheme involves moving the inboard bottom joint to a much larger major radius where it would be readily accessible and could be made into a lap joint. The inboard segment could then be removed from the bottom.

7.3.2 Hydraulic Joints

A great advantage of purely hydraulic clamping (i.e., without bolts) when used with an external shear restraint in the joint area, is that the joint can literally be dismantled by simply releasing the oil pressure to the hydraulic jacks. A stainless steel casing would be necessary to take up shear stress. More discussion can be found in Ref. 26.

8. REFERENCE DESIGNS FOR REACTORS EMPHASIZING NEUTRON APPLICATIONS

Several reference designs have been selected on the basis of the foregoing analysis, tempered with engineering considerations. For fusion neutron applications with wall loadings of 0.5 MW/m^2 , two designs are of Type A in which the OH coils sit in the throat of the reactor, while two are based on an optimization of Type B geometry where the OH coils are placed in the bore of the TF coils (BI) or are omitted altogether (BII). The basic geometries are illustrated in Fig. 28, and the important parameters are listed in Tables 2, 3 and 4. The locations of the design points for AI and AII are shown in Figs. 2 to 11 and for BI in Figs. 13 to 22. The four illustrative designs have the following features in common:

- (1) $P_{\text{res}} = 200 \text{ to } 250 \text{ MW}$
- (2) $M_c = 3100 \text{ to } 4200 \text{ metric tons}$
- (3) $n\tau_E \sim 3 \times 10^{13} \text{ cm}^{-3}\text{s}$
- (4) $P_{\text{fusion}} = 320 \text{ to } 410 \text{ MW}$
- (5) $\langle \phi_w \rangle = 0.50 \text{ MW/m}^2$.

Other parameters have considerable variation.

8.1 EXTERNAL OH COILS

Configuration AI gives nearly the optimal value of $\phi_w/(P_{\text{res}}M_c)$ associated with reactors of wall loading 0.5 MW/m^2 while the trade-off between P_{res} and M_c results in a lifetime cost of the TF coils only slightly higher than the minimum possible with $d/R_M = 0.50$, approximately \$500M. This is probably comparable to the cost of an equivalent superconducting coil set considering

the latter's higher capital cost, expensive liquid helium refrigeration system, and the refrigeration power requirement. A lower lifetime cost could be obtained with a higher value of d/R_M at the cost of increased coil mass.

For a manufacturing unit cost sufficiently high or an electrical power cost sufficiently low, there is an optimum d/R_M for minimizing total lifetime cost, although its value may be too high to be of practical value. For the rates assumed in Fig. 11, however, this optimum does not occur before d/R_M reaches its physical limit of 1.

The coil mass for Type AI is lower than that of any of the other reference designs requiring an OH solenoid. Radial access is large for ease of installation of test modules, beam lines, and diagnostics. The space between the blanket and the outer leg of the TF coil, defined on the basis of toroidal-field ripple, can accommodate substantial shielding. The ripple values from an exact calculation for rectangular coils are substantially lower than the values in Table 2 which are obtained from an empirical relation derived for circular coils,²⁵ and in fact are sufficiently low to be acceptable for ignited fusion plasmas.

The equilibrium field coils can supply at least one-third of the total flux swing (resistive + inductive) needed to start the plasma current I_p . Since the resistive component is no more than half of the inductive component $L_p I_p$, and since the additional flux swing required to maintain I_p for a flat-top of 100 s is a relatively small 4 V, for $\langle T_e \rangle = 8$ keV, the OH transformer must supply only an amount equivalent to $L_p I_p$. This requirement determines the change in the magnetic field that must be generated by the OH solenoid in the throat of the tokamak.

The main disadvantage of the AI configuration is that the throat diameter is at the very minimum needed to accommodate the required $L_p I_p$ with a

realistic range for B_{OH} . The maximum absolute value B_{OH} is limited by stress considerations, while B_{OH} during the flat-top should be kept relatively small to limit power dissipation. Consequently, the pulse length must be relatively short (50 s or less).

The stress levels in the AI TF coils are quite high although the maximum principal stress is well below the 36,000 psi yield strength for the CDA-102 high conductivity copper alloy. A secondary disadvantage, from the aspect of lifetime cost, is that the design point falls on the low mass side of the minimum-cost locus for the assumed power and manufacturing rates. Any unplanned increase in electricity cost moves the configuration further away from the optimum.

To overcome these drawbacks, configuration AII has been formulated with increased throat diameter, increased radial build d , and a larger M_C for approximately the same lifetime cost as AI. The primary benefit is that the required ΔB_{OH} drops to 60% of its value for AI. At the same time P_{res} is 10% lower and bending stress is 30% lower. The main disadvantage is the larger coil mass.

Because of the low current densities in the conductors, a consequence of moderate values of B_M and large cross-sectional areas, cooling in the steady state for both versions is possible with a conventional pressurized water system. Peak temperatures in both the coolant and the conductors can be readily maintained well below 100°C with only 5% of the cross-sectional area allocated to cooling passages.

8.2 INTERNAL OH COILS

Area for the flux swing can be increased even further to allow longer pulse lengths if the OH coils are placed just inside the TF coil bore. This

configuration is practical only if the TF coils are readily demountable to allow quick access to the OH coils for maintenance or removal. For the Type BI design, the available flux is sufficiently large for a pulse length of at least 300 s when $\langle T_e \rangle \approx 8$ keV. Although a significantly higher B_M is required than for AII, the stresses are no higher and the resistive losses only marginally larger. The lifetime cost is again approximately \$500M for the same fabrication and electricity rates used in the AI and AII costing. It may be noted that in contrast to the situation for the Type A configurations, the parameters range is such that an increase in d/R_M increases the lifetime cost. In fact, $d/R_M = 0.75$ is close to the optimum value.

The main disadvantages of the Type BI geometry are the relatively large B_M and the internal placement of the OH solenoid. The BII geometry eliminates the requirement of an OH solenoid by depending on the EF flux swing and a neutral-beam or radio-frequency-driven current to establish and maintain I_p . The smaller R_p that results from elimination of the OH solenoid allows a smaller B_M . Although the resulting design is the most compact and economical of the four types considered, its feasibility depends on the effectiveness of driving a plasma current by momentum transfer, which is yet to be demonstrated convincingly in tokamaks.

9. CONSIDERATIONS FOR PURE FUSION POWER REACTORS

Although this study has concentrated on tokamaks of a size smaller than appropriate for "pure fusion" electrical power generation using a relatively modest magnetic field, it is interesting to consider the question of whether resistive TF coils of optimal geometry can also find application in "pure fusion" reactors. Such a reactor can be conceived by expanding the Type BI geometry to give a sufficiently high fusion power gain Q_C and an increased neutron wall loading. The effects of different wall loadings are shown in Fig. 29. The corresponding P_{res} vs B_M curves for constant Q_C , of primary interest in connection with power-producing reactors, are shown in Fig. 30.

Resistive dissipation is essentially minimized by choosing a design point much closer to the optimum (see Fig. 20) than in the previous configurations. Table 5 compares some of the major parameters of such a reactor with those of a recently designed "pure fusion" electrical power reactor with superconducting coils²⁷ ("STARFIRE"). For a coil bore of approximately 10 m x 17 m and a B_M of 6.8 T, the reactor, with a blanket power multiplication of 1.2, achieves a Q_C of 6.3 with a wall loading of 2.5 MW/m². The resistive loss is an acceptable 360 MW, but the coil mass is a very large 21,700 tons. The plant efficiency is about the same as for STARFIRE, but the thermal and electrical power outputs are twice as large. Smaller resistive-coil pure fusion reactors can be envisaged but with degraded plant efficiencies.

Overall reactor size and efficiency that are comparable with those of currently envisaged superconducting-coil reactors can be achieved with resistive TF coils having a lifetime cost of approximately \$1,300 M, under the assumptions of Sec. 4.4. Thus, in principle, even a pure-fusion electrical power reactor is feasible based on proven copper-coil technology, and with all the advantages discussed in Sec. 1.

10. CONCLUSIONS

With major difficulties apparent in implementing superconducting TF coils for tokamak reactors, this paper has reexamined the commonly-accepted notion that resistive coils would be unacceptable because of excessive power losses and the consequent inability to operate in the steady state. It is shown that with the moderate magnetic field strengths now thought to be adequate to contain a plasma with a fusion power density of reactor interest, steady-state resistive TF coils can be designed that offer attractive performance at least for near-term test reactors and practical tokamak neutron sources.

The requirement for accessibility to all subsystems of a tokamak reactor, including internal PF coils and inboard blanket modules, necessitates the use of readily demountable TF coils. Within the current state of the art, or that *realistically projected ahead for several decades*, this requirement implies that the TF coils must be assembled from resistive conductors. Disadvantages of superconducting coils in addition to their lack of demountability, include their extreme sensitivity to pulsed magnetic fields and to neutron and gamma radiation, complicated refrigeration systems, necessity for on-site fabrication, excessive downtime in case of warm-up or cool-down, and in the long run the uncertain supply of helium and the possibly exorbitant cost of niobium. The need for steady-state operation of TF coils in practical reactor applications implies that resistive coils must be massive to allow easy cooling with pressurized water. Such massive coils will result in power losses that are acceptable in the sense that they are comparable with the sum of the other power losses in the reactor facility (PF coils, plasma heating systems, blanket cooling systems, pumps, HVAC). Attention in this paper has been focused on rectangular TF coil geometries which permit the greatest accessibility and the most straightforward fabrication methods. The

necessarily large conductor cross sections and the restriction of the magnetic field to less than 8.0 T at the coil windings lead to readily manageable levels of stress.

In the parametric analyses of TF coil and reactor characteristics, various optimizations have been considered for reactor configurations that incorporate OH coils in the throat of the reactor (Type A) or in the bore of the TF coils (Type B). For geometries associated with a specified neutron wall loading, either the resistive dissipation or the total lifetime cost of the TF coils can be minimized. Power losses can be reduced to the range of 200 to 300 MW in steady-state operation for reactors with fusion electrical power gain (the ratio of potential gross electrical output from the reactor to the electrical power input to the TF coils) of order 1 and neutron wall loadings of 0.5 MW/m^2 . The minimum lifetime cost of the TF coils for an assumed fabrication cost of \$35/kg and an electricity value of \$25/MW-hr is approximately \$500 M. Departures of the coil mass by less than 30% from the optimum increase the lifetime cost by only a few percent.

Several reference designs have been formulated with each TF coil divided into four segments consisting of approximately 25 plates bolted together. Each plate can be made by splicing two or three commercially available plates of 4 m length and thickness up to 6 cm. There appears to be a number of feasible configurations for demountable joints between segments. The method of coil manufacture advocated in this paper lends itself to relatively low construction costs using mass production in off-site factories with assembly on-site limited to simple operations.

Tokamak reactors with demountable resistive TF coils and major radii in the range 4.5 to 5.5 m are suitable for near-term fusion test reactors and for deployment as neutron sources for applications such as fusion-fission power

generation and isotope production. By increasing the reactor size, the obtainable fusion power output and the neutron wall loading increase much more rapidly than the TF coil power loss, so that low-field "pure fusion" reactors with large electrical power gains are feasible for major radii of about 8 meters and more.

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Table 1CANDIDATE DEMOUNTABLE JOINTS

<u>TYPE OF JOINT</u>	<u>METHOD OF APPLYING CLAMPING PRESSURE</u>	<u>EXAMPLES OF PREVIOUS USE</u>
Lap	Bolts	ISX (Outboard) PDX
Finger	Recessed pins and through-bolts	ISX (Inboard) Doublet III
Vertical Butt	Bolts and clamping ring attached to center column	See Fig. 27
Horizontal Butt	(1) Bolting together of overlays attached to butted segments (2) Clamping ring on outboard side	Analogous to clamping of Alcator TF coils
Plug-In	Hydraulic clamping ring	Plug-in joints on TEXT PF coils
Lap	Hydraulic cylinders	FM-1

Table 2
TOROIDAL FIELD COIL PARAMETERS

	<u>AI</u>	<u>AII</u>	<u>BI</u>	<u>BII</u>
No. of coils	12	12	12	12
a_C (m)	3.25	2.90	3.00	2.75
b_C (m)	5.20	4.50	4.55	4.50
R_C (m)	5.25	5.40	4.80	4.35
R_M (m)	2.00	2.50	1.80	1.60
d (m)	1.00	1.25	1.35	1.20
d_V (m)	0.80	0.80	0.50	0.50
d_{OH} (m)	0.00	0.00	0.45	0.00
w (m)	1.07	1.34	1.21	0.86
d/R_M	0.50	0.50	0.75	0.75
R_M/R_C	0.38	0.46	0.38	0.37
Overall diam. (m)	19.0	19.1	18.3	16.6
Overall height (m)	12.4	11.5	11.8	11.4
Access gap (m)	3.37	3.00	2.87	2.85
B_M (T)	5.87	5.96	7.60	6.91
B_t (T)	2.42	2.98	2.86	2.70
N_{tC} per coil (MA _t)	4.89	6.21	5.70	4.61
Turns per coil, N	25	25	25	25
I_C (kA)	196	248	228	184
Coil mass M_C (10^6 kg)	3.08	4.22	3.82	2.45
Resistive loss P_{res} (MW)	246	221	238	207
<u>Ripple, peak-to-avg.</u>				
at $R = R_p$	0.004	0.006	0.005	0.004
at $R = R_p + a_p/2$	0.015	0.016	0.016	0.014
at $R = R_p + a_p$	0.050	0.044	0.050	0.045

Table 2 (Continued)

Forces (MN)

Centering force per coil	125	132	169	126
Horiz. separating force	39	57	51	37
Vert. separating force	52	72	87	58

Stresses (10^3 psi)

Max bending stress	25	17	15	18
Tensile stress, horiz. leg	2.6	2.5	2.3	2.6
Bearing stress in nested region	3.0	2.8	2.4	2.1

Table 3TF COIL THERMAL PARAMETERS

	<u>AI</u>	<u>BI</u>
Type of copper	CDA 102 Alloy	
Conductivity	101% IACS	
Resistivity at 50°C (Ωm)	1.9×10^{-8}	
Max current density (kA/cm^2)	0.76	0.91
Packing factor - outboard leg	0.85	0.85
- inboard leg	0.80	0.72
Conductors per coil	25	25
Cooling channels per conductor	6	10
Turns per channel	1	1
Channel length (m)	37.8	36.1
Channel hydraulic dia (cm)	1.89	1.80
Coolant velocity (m/s)	2.0	2.0
Channel pressure loss (psi)	17.4	17.4
Total pumping power (kW)	193	293
Total volume flow (m^3/s)	1.29	1.95
Coolant inlet temp (°C)	20.0	20.0
Coolant temp. rise (°C)	45.8	29.2
Max ΔT , conductor to coolant (°C)	9.0	7.9
Max ΔT , to local hot spots (°C)	11.5	16.6

Table 4
PLASMA AND REACTOR PARAMETERS

	<u>AI</u>	<u>AII</u>	<u>BI</u>	<u>BII</u>
Major radius R_p (m)	4.85	5.00	4.78	4.10
Half-width a_p (m)	1.60	1.25	1.28	1.25
Half-height b_p (m)	3.20	2.50	2.55	2.50
R_p/a_p	3.03	4.00	3.75	3.28
a_v (m)	1.85	1.50	1.53	1.50
b_v (m)	4.20	3.50	3.55	3.50
q at plasma edge	3.0	3.0	3.0	3.0
I_p (MA)	7.8	5.7	5.9	6.3
Throat radius (m)	1.0	1.25	0.0	0.0
L_p (μH)	5.46	7.37	6.64	5.02
L_p^{1p} (Vs)	42	42	39	31
ΔB_{OH} for start-up (T)	16.7	10.0	2.7	--
Loop voltage at $\langle T_e \rangle = 8$ keV	0.034	0.042	0.040	0.038
Pulse length (s)	50	~ 100	~ 500	∞
ΔB_{OH} during flat-top (T)	0.7	1.0	1.4	--
Range of B_{OH} (T)	+10.0 -7.4	+8.0 -3.0	+3.5 -0.6	-- --
β_p	1.8	2.4	2.2	1.9
$\langle \beta \rangle$	0.079	0.060	0.064	0.073
Relative $n r_E \propto n^2 a^2$	1.25	1.00	1.02	1.00
$\propto n a_p^2$	1.43	1.00	1.03	1.00
Effective wall area A_w (m^2)	646	534	519	438
Fusion power P_{fus} (MW)	404	334	324	274
Q_C	0.46	0.42	0.38	0.38
Wall loading ϕ_w (MW/ m^2)	0.50	0.50	0.50	0.50

Table 5

TOKAMAK ELECTRICAL POWER REACTORS WITH BLANKET MULTIPLICATION ≈ 1.2

	Superconducting TF Coils (STARFIRE)	Resistive TF Coils (Type BI)
R_p	7.0 m	8.2 m
Coil conductor	Nb ₃ Sn + NbTi	Copper
Coil bore	10 m x 15 m	10 m x 17 m
R_c	7.8 m	8.33 m
R_M	3.42 m	3.33 m
d		2.50 m
w		2.03 m
a_p	1.94 m	2.9 m
M_c		21,700 tons
B_M	11.0 T	6.8 T
Coil Power Loss	20 MW _e (refrig)	360 MW _e
$\langle \beta \rangle$	0.067	0.085
I_p	11 MA	14 MA
$n r_E \propto \langle \beta \rangle B_t^2 a_F^2$	2.6	2.0
ϕ_w	3.5 MW/m ²	2.5 MW/m ²
Fusion Power	3200 MW	6740 MW
Gross Elec. Power	1600 MW _e	3350 MW _e
Total Power Consump.	450 MW _e	760 MW _e
$Q_E = \frac{\text{Gross Elec. Power}}{\text{Circulating Power}}$	3.6	4.4
Plant Efficiency	0.30	0.32

Fig. 1. Model of Tokamak Geometry. (PPPL-796359)

Fig. 2. TF Coil Resistive Dissipation for Type-A Reactors.
(PPPL-796350)

Fig. 3. Normalized Coil Mass for Type-A Reactors. (PPPL-796348)

Fig. 4. Neutron Wall Loading for Type-A Reactors. (PPPL-796344)

Fig. 5. Fusion Electrical Power Gain for Type-A Reactors.
(PPPL-796346)

Fig. 6. Ratio of Neutron Wall Loading to TF Coil Power Dissipation for Type-A Reactors. (PPPL-796345)

Fig. 7. Fusion Electrical Power Gain Normalized to Coil Mass for Type-A Reactors. (PPPL-796347)

Fig. 8. Neutron Wall Loading Efficiency Normalized to Coil Mass for Type-A Reactors. (PPPL-796342).

Fig. 9. Variation of TF Coil Power Dissipation with Maximum Field at the Conductor for Type-A Reactors Giving 0.5 MW/m^2 Neutron Wall Loading. (PPPL-796363)

Fig. 10. Variation of TF Coil Power Dissipation with Maximum Field at the Conductor for Type-A Reactors Giving 0.5 MW/m^2 Neutron Wall Loading. (PPPL-796361)

Fig. 11. Dependence of TF Coil Lifetime Costs on Power Dissipation and Coil Mass for Type-A Reactors Giving 0.5 MW/m^2 Neutron Wall Loading. (PPPL-796343)

Fig. 12. Effects of Electricity and Coil Fabrication Costs on the Optimal TF Coil Parameters for Minimum Lifetime Cost of Type-A Reactors. (PPPL-796360)

Fig. 13. TF Coil Resistive Dissipation for Type-B Reactors. (PPPL-803035)

Fig. 14. Normalized Coil Mass for Type-B Reactors. (PPPL-803030)

Fig. 15. Neutron Wall Loading for Type-B Reactors. (PPPL-803031)

Fig. 16. Fusion Electrical Power Gain for Type-B Reactors. (PPPL-803023)

Fig. 17. Ratio of Neutron Wall Loading to TF Coil Power Dissipation for Type-B Reactors. (PPPL-803032)

Fig. 18. Fusion Electrical Power Gain Normalized to Coil Mass for Type-B Reactors. (FPPL-803033)

Fig. 19. Neutron Wall Loading Efficiency Normalized to Coil Mass for Type-B Reactors. (PPPL-803J29)

Fig. 20. Variation of TF Coil Power Dissipation with Maximum Field at the Conductor for Type-B Reactors Giving 0.5 MW/m^2 Neutron Wall Loading. (PPPL-803627)

Fig. 21. Variation of TF Coil Power Dissipation with Maximum Field at the Conductor for Type-B Reactors Giving 0.5 MW/m^2 Neutron Wall Loading. (PPPL-803034)

Fig. 22. Dependence of TF Coil Lifetime Costs on Power Dissipation and Coil Mass for Type-B Reactors Giving 0.5 MW/m^2 Neutron Wall Loading. (PPPL-803026)

Fig. 23. Model of Coil Structure and Loading for Stress Considerations.
(PPPL-806676)

Fig. 24. Normalized Bending Moment Distributions for Hested Coil Model ($\nu=0$). (PPPL-803667)

Fig. 25. The Effects of Inner-Leg Stiffness and Bucking Cylinder Length on Normalized Bending Moment Distributions. (PPPL-806677)

ISX PARAMETERS
5' (---) 8.3' (---) INNER BEAMS

Fig. 20. Variation of bending moment distribution for ISX parameters.

Fig. 27. Elevation View of a Representative Reactor Configuration Illustrating Demountable TF Coils. (PPPL-803144)

Fig. 29. Schematic Layouts of Reference Tokamak Designs. (PPPL-796352)

Fig. 29. Variation of TF Coil Power Dissipation with Maximum Field at the Conductor for Type-B Reactors with Neutron Wall Loading ϕ_w .

(PPPL-806663)

Variation of TF Coil Power Dissipation with Maximum Field at the
 Torus for Type-B Reactors with Fusion Electrical Power Gain Q_c .
 (PPPL-80E662)