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SUPERCONDUCTING TOROIDAL FIELD COIL CURRENT DENSITIES FOR THE TFCX

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ABSTRACT

A major goal of the Tokamak Fusion Core Experiment (TFCX) study was to minimize the size of the device and achieve lowest cost. Two key factors influencing the size of the device employing superconducting magnets are toroidal field (TF) winding current density and its nuclear heat load withstand capability. Lower winding current density requires larger radial build of the winding pack. Likewise, lower allowable nuclear heating in the winding requires larger shield thickness between the plasma and coil. In order to achieve a low-cost device, it is essential to maximize the winding's current density and nuclear heating withstand capability. To meet the above objective, the TFCX design specification adopted as goals a nominal winding current density of 3500 A/cm^2 with 10-T peak field at the winding and peak nuclear heat load limits of 1 mW/cm^3 for the nominal design and 50 mW/cm^3 for an advanced design. This study developed justification for these current density and nuclear heat load limits.

A value of 3500 A/cm^2 is about 75% higher than values used in past studies and is based on evaluation of a variety of approaches. The approaches include consideration of (1) use of forced-flow Nb_3Sn conductors, similar to the Westinghouse Large Coil Program (LCP) conductor; (2) epoxy impregnation of the winding, which alters the load path and permits elimination of the co-wound structural material; (3) designing to 80% of critical current instead of 67%, as previously done; (4) reacting the conductor after winding to anneal out induced strains; and (5) use of Incoloy instead of JBK-75 as the conduit. Incoloy has cooldown characteristics that closely match that of the Nb_3Sn , so the induced cooldown strain is decreased.

In the nominal design, the Nb_3Sn conductor employs JBK-75 stainless steel conduit with a thickness of 1.7 mm and a void fraction inside the conduit of 32%. Peak nuclear heat load of 1 mW/cm^3 can be removed by forced-flow helium entering at 4.2 K and exiting at 4.5 K. In the advanced design, with peak nuclear heating of 50 mW/cm^3 , the reasonable winding exit temperature is 5.5 K. To offset the degradation in critical current at the higher temperature, the void fraction is increased to 41% (from 32%) and an Incoloy conduit is used (in place of the JBK-75 conduit). The resulting conductor cross-sectional dimensions change slightly, but the winding current density remains about the same (3500 A/cm^2).

This study concludes that the specified TF winding current densities are an aggressive step forward but are realistically achievable using existing forced-flow, cable-in-conduit conductor or its variations.

1. INTRODUCTION AND SUMMARY

During the development of the Tokamak Fusion Core Experiment (TFCX) superconducting toroidal field (S/C TF) coil options, there has been strong motivation to limit the radial build of the device in the centerpost region, because it is generally agreed that, to a first approximation, the cost of the device varies with the plasma major radius R_0 to a power between 2.5 and 3.0. Methods for achieving a compact device include designing the TF coils for a high winding current density and a high nuclear heat load, the latter permitting a reduction in the inboard shield thickness. The conductor critical current is enhanced by lowering the operating temperature, whereas increased nuclear heat loads tend to lead to increased conductor temperatures. Consequently, the objectives of high current density and high nuclear heat are mutually conflicting.

The example problems considered in this study assume use of a Nb_3Sn forced-flow conductor similar in size and void fraction to the Westinghouse Large Coil Program (LCP) conductor. Most of the examples considered in this study assume use of a single grade of conductor throughout the winding. A few examples have been worked to illustrate the potential gains in average current density afforded by grading, but methods of grading forced-flow conductors are not well developed, and the practicality of such grading is considered doubtful.

With a single grade of conductor, the TFCX design specification¹ winding current density of 3500 A/cm^2 at 10 T can be met using the present Westinghouse LCP conductor (with JBK-75 conduit), provided the conductor operating temperature is suitably controlled. If the conductor is reacted before winding, the maximum acceptable conductor temperature is 4.5 K. For a TFCX application, this limit can be satisfied with a reasonably straightforward helium manifolding arrangement, if the nuclear heat load does not exceed 1 mW/cm^3 (nominal design requirement). If the conductor is reacted after winding, a peak conductor operating temperature of 5.5 K is acceptable. At 12 T, the 2300-A/cm^2 design specification can be met with this conductor only if the conductor is reacted after winding and operated at a temperature of 4.2 K or lower.

If the JBK-75 sheath is replaced by an Incoloy 903 sheath, the cooldown contraction of the conductor composite closely approximates that of Nb_3Sn , so that the degradation of the critical current due to cooldown strain is greatly reduced. The absence of significant cooldown-induced degradation has been demonstrated by the Massachusetts Institute of Technology (MIT) in short-sample tests. On this basis, the achievable current density as a function of peak conductor temperature at 10 T and at 12 T may be summarized as shown in Fig. 1. It is clear that use of Incoloy in place of JBK-75, or use of the wind-and-react technique, increases the maximum temperature at which the design specification current density can be achieved; this implies increased freedom of design with respect to allowable nuclear heat and coolant manifolding arrangement.

With respect to maximum permissible nuclear heat loads, it is found that for practical helium manifolding arrangements (e.g., winding three-in-hand, a flow path length of four turns is about 72 m), approximately 10 to 12 mW/cm^3 may be tolerated purely from a heat-removal standpoint. For more complex manifolding arrangements (e.g., cooling one turn or one-half turn in regions of peak nuclear heat), up to about 100 mW/cm^3 becomes possible. However, these nuclear heat loads entail conductor temperatures up to about 6 K,

Fig. 1. Achievable current density vs peak conductor temperature at 10 and 12 T.

which would degrade the critical current to the point at which the design specification current densities could not be met.

The assumptions, design criteria, and equations used in the supporting analysis are included in Appendix A.

2. DISCUSSION

2.1 FEASIBILITY OF GRADING FORCED-FLOW WINDINGS

If a forced-flow winding is designed to have the same current in all turns, grading requires a transition in conduit cross section when changing from one conductor grade to another. Joint designs for achieving such cross-section transitions have not yet been developed and demonstrated.

On the other hand, if the conduit cross section is maintained throughout the winding, grading requires different current in each grade. This type of design requires separate

power supplies and cryogenic leads for each grade. This increases complexity of the winding configuration in order to bring these leads out of the conductor pack. Such a design approach represents a large increase in complexity for a next-step device, and, in any event, the additional cost resulting from the complexity of these additional leads and power supplies must be weighed against the cost benefits associated with the higher average current density. While grading is not out of the question, it is far from being a state-of-the-art design technique for forced-flow windings. Consequently, the assumption of a graded winding for a near-term device is considered premature and overly optimistic.

2.2 AVERAGE WINDING CURRENT DENSITY VERSUS CONDUCTOR CURRENT

This study assumes use of a Nb_3Sn conductor having a void fraction of 32% and a cross-sectional area inside the conduit of 2.93 cm^2 . These are the parameters associated with the Westinghouse LCP conductor (Fig. 2). Variations in the Westinghouse/Airco

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CONDUCTOR DIMENSION OUTSIDE CONDUIT	2.19 cm
CONDUIT THICKNESS	1.70 mm
CROSS SECTIONAL AREA INSIDE CONDUIT	2.93 cm^2
VOID FRACTION	32%
NUMBER OF STRANDS	1458
STRAND DIAMETER	0.70 mm
HYDRAULIC DIAMETER	0.40 mm

Fig. 2. Westinghouse LCP coil conductor.

values of conduit wall thickness, conduit corner bend radius, conduit material, and heat treatment are considered.

Figure 3 shows the average winding current density compared to conductor current for a design developing a peak field of 10 T at a radius of 1.7 m [representative of an early TFCX-S (all-superconducting TF coil) design]. Figure 4 shows a similar curve for a 12-T peak field at a radius of 2.4 m. In each of these designs, a single grade of conductor is used.

The 10-T examples use a conduit thickness of 2 mm (compared to 1.7 mm in the Westinghouse LCP) and an inside surface corner bend radius of 4 mm (versus 2.85 mm in the Westinghouse LCP), whereas the 12-T examples use a jacket thickness of 3 mm and an inside surface bend radius of 6 mm. These values were selected on the basis of a structural analysis routine described in Appendix A. This analysis routine considers the combined stress state in the conduit wall due to transverse compressive winding loads and longitudinal dilational tensile loads. A conduit wall thickness is selected to meet established structural design criteria, to withstand realistic quench pressures, and to conform to conduit thickness limitations related to manufacturing considerations.

The selection of the actual conductor operating current depends upon the critical current, which is a function of many variables:

- maximum field;
- residual compressive cooldown strain in the conductor;
- choice of jacket material and heat treatment, both of which affect the residual cooldown strain;

Fig. 3. Average current density vs conductor current at 10 T.

Fig. 4. Average current density vs conductor current at 12 T.

- strain level in conductor from being bent into the minimum radius of curvature during coil winding, which can be relieved if the conductor is reacted after winding; and
- strain level in conductor resulting from operating loads.

Determination of an appropriate operating current, and the average winding current density resulting from that choice, are discussed in Sect. 2.3.

2.3 ANALYSIS OF CRITICAL CURRENT DATA

Figure 5, which is reproduced from ref. 2, shows the critical current of 4.2 K samples of Westinghouse LCP conductor as a function of imposed tensile strain, for field levels of 8, 10, and 12 T. This conductor uses a JBK-75 conduit and is reacted for 30 h at 700°C.

At the 10 T field, the critical current is 24 kA with no imposed tensile strain and reaches 41 kA for an imposed tensile strain of 0.75%. The 0.75% imposed strain is therefore equal and opposite to the residual compressive strain due to cooldown plus winding; MIT has determined that the cooldown portion alone is -0.5%. A compressive strain of 0.75%, therefore, degrades the critical current to 59% of its value in the strain-free state (24 vs 41 kA). This fractional degradation is in reasonably good agreement with Ekin's strain scaling law given in ref. 3:

$$\frac{I}{I_0} = \sqrt{1 - a|\epsilon|^{1.7}} \left[\frac{1 - \frac{B}{22(1 - a|\epsilon|^{1.7})}}{1 - B/22} \right]^2, \quad (1)$$

Fig. 5. Critical current of the Westinghouse LCP conductor as a function of tensile strain.

where ϵ is the strain, $a = 900$ if $\epsilon < 0$, and $a = 1250$ if $\epsilon > 0$. For $\epsilon = -0.0075$ and $B = 10$ T, Eq. (1) gives $I/I_0 = 0.52$.

For a TFCX-size coil, the minimum radius of curvature imposed on the conductor during coil winding is about 1.5 m, corresponding to a bending strain in the conduit wall of about 0.7%. According to ref. 2 (vol. VI, Part 1, pp. 7-55), the strands experience about one-third the bending strain developed in the conduit, or about 0.23%. Furthermore, when the coil is energized, a dilational tensile strain of about 0.1% is developed. Thus, the net strain, including cooldown strain, is $-0.50 - 0.23 + 0.10 = -0.63\%$. Ekin's formula with $\epsilon = -0.0063$ and $B = 10$ T gives $I/I_0 = 0.64$. Under TFCX conditions, the critical current at 10 T becomes $0.64 \times 41 = 26.3$ kA at 4.2 K.

The maximum conductor temperature depends upon the amount of nuclear heat that the coil must withstand. A separate analysis has shown that for 1 mW/cm^3 (the nominal design value per ref. 1), a temperature rise of about 0.5 K occurs. Reference 4 predicts that the critical current at temperature T degrades from its 4.2 K value by the fraction

$$\frac{I_c(B, T)}{I_c(B, 4.2)} = 1 - \frac{T - 4.2}{10.7 - 0.61B} \quad (2)$$

If helium enters at 4.0 K and exits at 4.5 K, then for $B = 10$ T, Eq. (2) gives a fractional degradation of 0.935, so that the critical current at 4.5 K is 24.6 kA. If the conductor current is set at 80% of the critical current, it follows that the Westinghouse LCP conductor, without modification, would operate in a TFCX application at 19.7 kA at 10 T. This corresponds to an average current density (with a single grade) of 3500 A/cm², which just meets the design specification.

One way to increase the critical current of the Westinghouse LCP conductor (with the present JBK-75 sheath material) is to react the conductor after winding. This process anneals out the strain introduced during winding, although cooldown compressive strain and tensile strain due to dilation remain during coil operation.

The feasibility of winding and reacting TFCX-size TF coils has been discussed with the Westinghouse LCP team. The principal technical feasibility issue is to identify an insulator wrap material that can survive the reacting temperature so that it would not be necessary to clean oxides off the reacted conductor and then apply insulating wrap after reacting. Furnaces large enough to react such large coils are considered technically feasible.

The critical current for wound-and-reacted, JBK-75-wrapped Westinghouse LCP conductor at 4.2 K is estimated by scaling down the peak critical current of 41 kA (with zero strain) by the degradation per Eq. (1) for $B = 10$ T and $\epsilon = -0.0050$ (cooldown) + 0.001 (dilation) = -0.0040 ; the result is $I_c = 34.2$ kA. Since an operating current of 19.7 kA is needed to achieve 3500 A/cm², the critical current must not go below 24.6 kA. Therefore, a temperature degradation of $24.6/34.2 = 0.72$ is acceptable when the conductor is reacted after winding; this corresponds to a peak conductor temperature of 5.5 K.

References 5-7 describe recent work performed at MIT dealing with the effect on critical current of using different heat treatments and/or substituting Incoloy 903 for JBK-75 as the conduit material. Figure 5 summarizes critical current data gathered from refs. 2 and 5-8 and from conversations with MIT personnel. All of the data shown in Fig. 5 are for 4.2 K operation and therefore include the effects of cooldown strain.

There is a dramatic difference between the short-sample (10-cm) and the long-sample (375-cm) MIT data for JBK-75, although the long-sample results are not significantly different from the Westinghouse data. It would therefore appear that the MIT dual heat treatment in itself has a relatively minor impact on critical current and that the difference between short-sample and long-sample results for JBK-75 is due to effects related to specimen length.

One possible explanation is better transfer of heat away from the hot spot in the short samples, with an attendant increase in critical current. Another plausible explanation is that the short samples do not have adequate end restraint to simulate a plane strain condition. If the strands are not fully constrained to have the same longitudinal cooldown strain as the conduit, the short-sample results would then reflect little or, perhaps, no degradation due to cooldown strain.

At the 10-T field, the long-sample and short-sample critical currents for the dual-heat-treated specimens are in the ratio $25/36.75 = 0.68$. If it is assumed that the short-sample critical current approximates the as-reacted current, the observed degradation is equivalent to a cooldown strain of 0.0059 per Eq. (1). This strain estimate compares well with the value 0.0064 determined by analysis and reported in ref. 3. Therefore, it appears

reasonable to conclude that the dual heat treatment of the JBK-75-wrapped conductor in itself offers no significant improvement over the Westinghouse/Airco heat treatment.

The short-sample curves in Fig. 6 show that use of Incoloy 903 in place of JBK-75 offers a modest increase in critical current at fields of 10 T and higher. More importantly, however, the cooldown contraction of the Incoloy-wrapped conductor closely matches the cooldown contraction of Nb₃Sn, so that the degradation due to cooldown contraction is small.⁷ The short-sample Incoloy 903 curve in Fig. 6 was used for the calculations.

At 10 T, the critical current of an Incoloy 903-wrapped conductor (at 4.2 K with cooldown strain only) is therefore taken as 37.5 kA. In the absence of a significant cooldown strain, the degradation of critical current may be estimated using Eq. (1) with $\epsilon = 0.0023$ (bending) + 0.0010 (dilation) = +0.0033; the result is $I/I_0 = 0.835$. If, as before, an operating current of 19.7 kA is needed to achieve 3500 A/cm², the required critical current of 24.6 kA can be attained under these conditions with a temperature degradation factor as low as 0.78, corresponding to a peak conductor temperature of 5.2 K when the conductor is reacted before winding. In like manner, if the conductor is reacted after winding, the winding strain disappears and the maximum conductor temperature can be as high as 5.7 K.

Fig. 6. Cable-in-conduit (32% void) conductor current vs field.

A similar analysis of the critical current data has been made for each of the combinations of possible conditions:

- JBK-75 or Incoloy jacket (affects cooldown strain),
- react-and-wind or wind-and-react (affects winding strain), and
- 10- or 12-T peak field.

Figure 1 summarizes the attainable current density as a function of peak conductor temperature for each combination of peak field, jacket material, and manufacturing sequence. The design specification current densities are shown for comparison. The curves afford a convenient method of comparing the current densities that result from each possible combination. The higher the maximum acceptable conductor temperature, the greater the freedom of design with respect to nuclear heat and helium manifolding arrangement.

2.4 EFFECT OF CONDUCTOR GRADING ON CURRENT DENSITY

Although the practicality of grading forced-flow windings is perhaps questionable, the analytic formalism described in Appendix A includes the capability to model graded windings in which the conduit cross section is maintained and different currents are used in each grade.

Several cases were run to assess the potential increase in current density afforded by grading. No attempt was made to address how leads would be brought out of the winding.

Windings were sized to develop 12 T at a radius of 2.4 m. Up to three grades were considered, with currents set at two-thirds of the critical currents shown in Fig. 6 on the curve labeled "W LCP." Corrections for temperature and winding strain effects were not considered in this evaluation, but the relative average current densities should be nonetheless indicative of the potential benefits of grading, if indeed grading is deemed practical. The operating currents and field ratings of the various grades are summarized in Table 1.

The results for a 12-T winding pack are summarized in Table 2. Use of two grades raises the average current density about 50% above that with a single grade, whereas use of three grades about doubles it.

Table 1. Conducting operating currents and field ratings

	Grade 1	Grade 2	Grade 3
Operating current, kA	28.7	16.7	7.7
Maximum field, T	8.0	10.0	12.0

Table 2. Effect on current density of 12-T winding pack consisting of multiple conductor grades

	Number of grades			
	1	2	2	3
Grades used ^a	3	1,3	2,3	1,2,3
Number of layers of grade 1 conductor		7		7
Number of layers of grade 2 conductor			17	4
Number of layers of grade 3 conductor	5 ^a	24	11	11
Fraction of total amp-turns in grade 1		0.46		0.54
Fraction of total amp-turns in grade 2			0.73	0.19
Fraction of total amp-turns in grade 3	1.00	0.54	0.27	0.27
Current density—grade 1, A/cm ²		4260		4290
Current density—grade 2, A/cm ²			2450	2480
Current density—grade 3, A/cm ²	1150	1150	1130	1150
Average current density, A/cm ²	1150	1725	1870	2280

^aSee Table 1.

2.5 PERMISSIBLE NUCLEAR HEAT VERSUS COOLING SYSTEM PARAMETERS

As the nuclear heat load rises, more and more helium must be circulated. Not only does this increasing mass flow increase pressure drops, but also the associated frictional heating increases the heat load further. In conventional piping system designs, these frictional heat terms are negligible, but in cryogenic systems, they are of the same order of magnitude as the imposed heat load. These heat loads limit the length of the coolant path and therefore affect the required number of coolant lines.

The determination of permissible nuclear heat loads was performed some time ago, using a representative TF coil design that differs somewhat from the cases discussed in the previous section. The TF coil design studied here develops a peak field of 10 T using a single grade of conductor and operates at a winding current density of about 2850 A/cm². The conductor is a forced-flow design with 32% void fraction and a cross-sectional area inside the conduit of 5.2 cm² (versus 2.93 cm² in the Westinghouse LCP). The conduit thickness is 3 mm, and the inside corner bend radius is 6 mm. The radial build of the TF winding is 12 turns.

Several coolant path lengths, corresponding to different manifolding arrangements, were considered. For each selected path length, the assumed nuclear heat load was gradually increased until it became impossible to select inlet helium conditions that would result in acceptable pressure drops and helium exit temperatures of 6 K or less.

Helium outlet conditions were calculated using a computer program that accounts for spatial variation of the nuclear heat load around the perimeter of the TF coil and also

accounts for attenuation of the nuclear heat load turn-to-turn. Pressure drops and frictional heat are calculated using the friction factor correlation in ref 2. The program contains thermodynamic properties of helium as defined in ref. 9.

A mean TF coil perimeter of 18 m was assumed, and coolant path lengths of 1/2, 1, and 4 turns were considered. The TF coil configuration has a radial build of 12 turns, and if it is assumed that the coil is wound three-in-hand, a path length of 4 turns is considered about as simple a manifolding scheme as is reasonably achievable. The shorter path lengths represent more complex manifolding but offer the potential for higher incident nuclear heat.

All of the cooling system configurations considered here assume steady-state operation of the coil. Larger incident nuclear heat loads are perhaps feasible in pulsed machines by using a buffer helium supply that is allowed to boil during coil operation and is relieved between pulses.

Table 3 summarizes the maximum nuclear heat capability for three cooling system configurations (denoted A, B, and C) that differ in flow path length and in helium inlet and outlet conditions. A nuclear heat level of 10 to 12 mW/cm³ appears feasible from a heat-removal standpoint for a practical manifolding arrangement, whereas up to about 100 mW/cm³ can be accommodated with many short helium flow paths. The nuclear heat load may be limited to lower values, however, to avoid unacceptable degradation of the critical current.

Table 3. Nuclear heat capability for several cooling system configurations

	Configuration		
	A	B	C
Number of turns in coolant path	4	1	1/2
Coolant path length, m	72	18	9
Helium inlet temperature, K	4.2	4.2	4.2
Helium outlet temperature, K	5.8	6.0	5.9
Helium inlet pressure, atm	9.0	8.0	10.0
Helium outlet pressure, atm	2.7	2.3	2.6
Helium flow rate per channel, g/s	15	30	50
Maximum nuclear heat, mW/cm ³	12	75	100

3. CONCLUSIONS

Maximum achievable current densities have been developed using a Nb₃Sn cable-in-conduit type of conductor. The recommended current densities were established by including effects of peak conductor temperature, cooling path length, conductor strain, conduit load-carrying capability, and peak nuclear heating. With a single grade of conductor, the TFCX design specification winding current density of 3500 A/cm² at 10 T can be met using the Westinghouse LCP conductor with JBK-75 conduit, provided that conductor temperature is maintained below 4.5 K.

With respect to maximum permissible nuclear heat loads, it is found that for practical helium manifolding arrangements (e.g., winding three-in-hand, a flow path length of four turns is about 72 m), approximately 10 to 12 mW/cm³ may be tolerated purely from a heat-removal standpoint. For more complex manifolding arrangements (e.g., cooling one turn or one-half turn in regions of peak nuclear heat), up to about 100 mW/cm³ becomes possible. However, these nuclear heat loads require conductor temperatures up to about 6 K, which would degrade the critical current to the point where the design specification current densities could not be met.

APPENDIX A

MAGNETICS AND STRUCTURAL ANALYSIS FORMALISM

INTRODUCTION

This appendix describes the supporting magnetics and structural analysis for the present superconducting TF coil current density study.

The second section discusses the assumptions made in the magnetics and structural analysis and describes the design criteria selected. The third section documents the equations used to size the winding cavity, evaluate the design-limiting stresses in the winding pack, and evaluate the maximum allowable quench pressure.

The magnetics and structural analysis routine has been programmed for an IBM-PC (see Appendix B).

ASSUMPTIONS AND DESIGN CRITERIA

Choice of Conductor

The example problems considered in this study assume use of a forced-flow, cable-in-conduit conductor. This type of conductor offers the best potential for high current density; pool-boiling conductors, as a group, typically operate at much lower current densities than do forced-flow conductors.

Most of the examples in this study assume that a single grade of conductor is used throughout the winding. Possible methods of grading forced-flow windings were considered, but it was concluded that several unresolved feasibility issues rendered grading of forced-flow windings an overly optimistic assumption for a near-term device such as TFCX-S.

While the practicality of graded forced-flow windings is debatable, the analysis routine described in the next section includes the capability to model graded windings in which the conductor conduit cross section is maintained in all grades but different currents are used in each grade.

Conductor Current

The conductor current is supplied as input data to the magnetics sizing routine. The conductor current is typically two-thirds to four-fifths of the critical current, with the critical current adjusted to include the effect of field level, strain level, operating temperature, and cooldown contraction properties of the conductor composite.

Structural Considerations

One of the factors that limits the current density is the need to provide enough steel in the winding to withstand the combination of the compressive winding load that propagates from turn to turn, plus the orthogonal tensile load resulting from dilation of the coil. This structural requirement can be design limiting, particularly at high fields (≥ 10 T). Because of this requirement, several previous forced-flow TF and poloidal field (PF) designs done by the Fusion Engineering Design Center (FEDC) (FED-Baseline, forced-flow option of

TFCX-S) have used a U-channel co-wound with the conductor. Use of any type of co-wound structure lowers the average winding pack current density.

Tests performed by MIT on arrays of conduits (without a co-wound U-channel) loaded in transverse compression indicate that epoxy impregnation of the array greatly increases the tolerable transverse compressive load. Without the potting, the turn-to-turn load must be borne by the conduit in bending the corner elbow. The MIT test results indicate that potting material in the interstices between adjacent conduits acts much as a fluid and transmits the turn-to-turn load in a direct compressive load path through the conduit walls. At the time of this writing, MIT was performing a finite-element analysis (using two-dimensional continuum elements) of the winding cross section to corroborate the test results. This more favorable load path offers the potential opportunity to eliminate the co-wound U-channel, with an attendant increase in current density.

In view of the MIT test results, this study assumes use of an epoxy-impregnated array of cable-in-conduit conductors without co-wound U-channels. Although perhaps not obvious at the outset, the limiting loading condition on the conduit is found to be the combination of turn-to-turn compressive loading in the wall of the conduit plus the dilational tensile load. The structural analysis of the conduit also considers the quench pressure load, although this loading condition turns out not to be a design driver in this case.

The structural analysis of the conduit is in accordance with the established FEDC Magnet System structural criteria. In the present application, these criteria limit the *algebraic difference* of the dilational tensile stress and the transverse compressive stress in the conduit wall to two-thirds of yield strength or one-third of ultimate strength, whichever is less. Since the two stress components are of opposite sign, the stress limit is applied to the *sum of the absolute values* of the components. Application of stress limits to the *maximum stress intensity* (i.e., the maximum algebraic difference between principal stresses in mutually orthogonal directions) is a standard structural design technique that follows from the well-known Tresca failure criterion for multiaxial stress states. Application of stress limits to stress intensities can be very different from application of the same limits to individual stress components.

Conductor Conduit Fabrication

The required conduit thickness is determined by the need to withstand loads that arise during quench and during normal magnet operation. On the other hand, manufacturing operations that must be carried out in the course of fabricating the conduit place a practical limit on the conduit thickness. Airco considers that 3 mm is the maximum jacket thickness that is practical with existing forming equipment. Furthermore, in view of Airco's experience in forming the 1.7-mm-thick jacket for the Westinghouse LCP conductor, members of the Westinghouse LCP team expressed the opinion that jacket thicknesses of much more than 1.7 mm would present a serious manufacturing problem.

Double-jacketing has been proposed as a possible alternative to thick conduit walls. However, this concept requires precise fit-up between the inner and outer jacket, and it has not yet been demonstrated that this fit-up can be achieved.

Consequently, conduit thicknesses have been limited to 3.0 mm. Furthermore, in most examples the inside corner bend radius was set at twice the conduit wall thickness in order to limit bending strains during conduit-forming operations to 20%.

MAGNETICS SIZING ROUTINE

The magnetics sizing routine can model multiple grades of forced-flow conductor. The routine assumes the cross-sectional dimensions of the conductor and conduit are the same in all grades. The operating current and maximum allowable field for each grade are supplied as input.

The radius R_N at which the peak field B_N must be developed is also specified. The amp-turns (AT) required to develop the peak field are

$$AT = \frac{B_N R_N}{\mu_0 / 2\pi} \quad (A.1)$$

The total area A inside the conduit, as well as the conduit wall thickness t_w , inside surface corner bend radius r_b , and insulation wrap thickness t_i are specified (Fig. 7). The transverse linear dimensions L_i and L_o are then

$$L_i = \sqrt{A + (4 - \pi)r_b^2} \quad (A.2)$$

and

$$L_o = L_i + 2t_w + 2t_i \quad (A.3)$$

The TF coil case sidewall thickness t_s and the bobbin ring and outer wall thickness t_b are specified, as well as the number of TF coils N_c .

The region of the TF coil winding assigned to conductor grade j , $j = 1, \dots, N$ is modeled as an annulus of outer radius R_j and inner radius R_{j-1} . R_N is specified, whereas $R_{N-1}, \dots, R_o$ are to be determined. The combined winding area in all N_c coils that is assigned to conductor grade j is

$$A_j = \pi(R_j^2 - R_{j-1}^2) - 2N_c t_s (R_j - R_{j-1}) \quad (A.4)$$

Starting with $j = N$ (high-field grade), R_{j-1} is initially set equal to $R_j - L_o$. The area A_j is computed using Eq. (A.4). The number of conductor turns that will fit inside the area A_j is taken as

$$N_j = N_c \left\lfloor \frac{f A_j / N_c}{L_o^2} \right\rfloor \quad (A.5)$$

where the parentheses indicate that the quantity therein is truncated to an integer. The term f indicates the fraction of the total area A_N that is actually filled by conductor. In

Fig. 7. Cross-sectional drawings illustrating the total area inside the conduit and the TF coil case sidewall thickness.

lieu of doing a detailed winding layout, f is supplied as input data, with $f = 0.9$ built in as a representative default value.

The amp-turns in layer j is

$$(AT)_j = N_j I_j . \quad (A.6)$$

If $j > 1$, the field at R_{j-1} is computed as

$$B_{j-1} = \left(\frac{\mu_o}{2\pi} \right) \frac{AT - (AT)_N - \dots - (AT)_j}{R_{j-1}} . \quad (A.7)$$

If B_{j-1} is greater than B_{j-1}^* (the maximum permissible field for grade $j - 1$), R_{j-1} is decremented by L_o and Eqs. (A.4)–(A.7) are repeated until $B_{j-1} \leq B_{j-1}^*$, at which point the sizing of grade j is completed. The index j is then decremented by 1, and Eqs. (A.4)–(A.7) are repeated as many times as necessary to size the next grade in similar fashion.

For the lowest field grade ($j = 1$), instead of comparing B_{j-1} to B_{j-1}^* , successive layers of conductor are added until

$$(AT)_1 + \dots + (AT)_N \geq AT . \quad (A.8)$$

If the left side of Eq. (A.8) exceeds AT , the maximum field will be exceeded at R_N ; therefore, the conductor currents in all grades are scaled down by the factor

$$S = AT / [(AT)_1 + \dots + (AT)_N] . \quad (A.9)$$

After scaling by the factor S , the maximum permissible field may possibly be exceeded at one or more of the points $R_{N-1}, \dots, R$. If such is the case, the permissible field at each of the nonconforming points is decreased by 1% and the entire sizing procedure repeated until there are no longer any nonconforming points.

The average winding pack current density is then

$$\bar{j} = AT / (A_1 + \dots + A_N) . \quad (A.10)$$

STRUCTURAL ANALYSIS ROUTINE

The centering force (N/m) on each TF coil is

$$F_c = \sum_{j=1}^N \frac{(B_j + B_{j-1})}{2} \frac{(AT)_j}{N_c} . \quad (A.11)$$

The transverse compressive stress in the conduit wall is

$$\sigma_c = \frac{F_c}{(2\pi R_o)/N_c} \frac{L_o}{2t_w} . \quad (A.12)$$

The dilational force is estimated using the analytic expression for a constant tension shape (Fig. 8):

$$F = F_1 + F_2 = \frac{B_N(AT)R_N}{2N_c} \ln \frac{R_{out}}{R_{in}} . \quad (A.13)$$

Fig. 8. Dilational forces for constant tension shape of the TF coil.

To allow for departure from constant tension shapes, the tensile force at the inside section is taken as

$$F_1 = 0.55F \quad . \quad (\text{A.14})$$

The dilational tensile stress is assumed to be shared between the coil case material and the conduit wall material in proportion to their respective cross-sectional areas; this further assumes that both are made of the same material. The tensile stress in the conduit wall is:

$$\sigma_t = F_1 / (A_{\text{case}} + A_{\text{conduit}}) \quad , \quad (\text{A.15})$$

$$A_{\text{case}} = \frac{2\pi t_b(R_N + R_o)}{N_c} + 2t_s(R_N - R_o) \quad , \quad (\text{A.16})$$

and

$$A_{\text{conduit}} = A_j - \frac{N_1 + \dots + N_N}{N_c} \quad , \quad (\text{A.17})$$

where A_j is the cross-sectional area of a single conduit (Fig. 9):

$$A_j = t_w \left[4h + 2\pi \left(r_b + \frac{t_w}{2} \right) \right] \quad (\text{A.18})$$

$$= t_w (4h + 2\pi r_b + \pi t_w) , \quad (\text{A.19})$$

and

$$h = L_i - 2r_b . \quad (\text{A.20})$$

Since σ_c [Eq. (A.12)] is compressive and σ_t [Eq. (A.15)] is tensile, the Tresca failure stress σ_e is the sum of the absolute values:

$$\sigma_e = \sigma_c + \sigma_t . \quad (\text{A.21})$$

According to established structural criteria, σ_e is limited to two-thirds of yield strength or one-third of ultimate strength, whichever is less.

The maximum allowable quench pressure is found by modeling the conduit as a square tube with rounded corners, subjected to a uniform internal pressure p . The pressure loading causes the square cross section to deform into a more nearly circular shape. The

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Fig. 9. Cross-sectional drawing illustrating the area of a single conduit.

membrane and bending stresses both assume their peak values at the corner bend of the conduit. The membrane stress may be calculated by inspection, whereas the bending stress is calculated per ref. 10.

The primary membrane stress is given by

$$\sigma_m = p \frac{[\sqrt{2}L_i - 2(\sqrt{2} - 1)r_b]}{2t_w} \quad (\text{A.22})$$

The bending moment in the corner per ref. 10 is

$$M = Cp(L_i + t_w)^2, \quad (\text{A.23})$$

where C is a dimensionless coefficient that is a function of

$$x = \frac{\left(r_b + \frac{t_w}{2}\right)}{(L_i + t_w)} \quad (\text{A.24})$$

and is found by linear interpolation between the Table 4 values.

The bending stress (tensile at the inside surface) is found in terms of M using conventional curved-beam theory as applied to a uniform rectangular cross section.

The distance that the neutral axis is displaced from the centroidal axis toward the center of curvature is

$$\bar{y} = \frac{r_b + (t_w/2) - t_w}{\ln(1 + t_w/r_b)} \quad (\text{A.25})$$

Table 4. Coefficient C vs x

x	C
0	0.0833
0.1	0.0594
0.2	0.0389
0.3	0.0220
0.4	0.0091
0.5	0.0

The distance from the neutral axis to the inside surface is

$$h_i = \frac{t_w}{2 - \bar{y}} \quad (\text{A.26})$$

The bending stress at the inside surface is

$$\sigma_b = \frac{M h_i}{t_w \bar{y} r_b} = \frac{C p (L_i + t_w)^2 h_i}{t_w \bar{y} r_b} \quad (\text{A.27})$$

In order to limit the membrane stress to the allowable σ_m^* ($2/3 S_y$ or $1/3 S_u$, whichever is less), p must not exceed p_1 , given by

$$p_1 = \frac{2 \sigma_m^* t_w}{[\sqrt{2} (L_i - 2r_b) + 2r_b]} \quad (\text{A.28})$$

In order to limit the sum of membrane-plus-bending stress to the allowable σ_{mpb}^* (S_y or $1/2 S_u$, whichever is less), p must not exceed p_2 , given by

$$p_2 = \frac{\sigma_{mpb}^*}{\left[\frac{\sqrt{2} (L_i - 2r_b) + 2r_b}{2t_w} + \frac{C (L_i + t_w)^2 h_i}{t_w \bar{y} r_b} \right]} \quad (\text{A.29})$$

For most practical conduit-bend radii, p_2 is more restrictive than p_1 .

APPENDIX B

MAGNETICS AND STRUCTURAL ANALYSIS ROUTINE

```

100 REM      CD.MAX      R. J. Hooper      FEDC / GE
110 REM
120 REM      Program to determine radial build of S/C TF coils
130 REM      Analysis procedure described in App. A of FEDC-M-84-E/M-031,
140 REM      "Current Densities in Superconducting Toroidal Field Coils"
150 REM
160 REM      Outer radius and field at outer radius specified,
170 REM      inner radius to be determined.
180 REM
190 REM      GLOSSARY OF TERMINOLOGY
200 REM NC      number of TF coils
210 REM NGZ      number of conductor grades
220 REM AT      total amp-turns to develop maximum field at outer radius
230 REM I(K)     conductor current in grade k
240 REM J(K)     average winding current density in grade k
250 REM BMAX(K)  maximum field in grade k
260 REM R(K)     radius to outermost point in grade k
270 REM NT(K)    number of turns in grade k (total for all coils)
280 REM AREA(K)  winding area in grade k
290 REM AW      total winding area
300 REM F        fraction of winding area filled by conductor
310 REM JAV      average winding current density
320 REM SCALE    factor by which currents in all grades are scaled down to
330 REM          satisfy field requirement at outside surface
340 REM RATIO    ratio of mid-surface TF coil radii at equator (outer-to-inner)
350 REM TS       thickness of TF coil case sidewall
360 REM TB       thickness of TF coil case bobbin ring and outer wall
370 REM A        cross-sectional area inside conductor conduit
380 REM TW       thickness of conduit wall
390 REM RB       conduit corner bend radius (inside surface)
400 REM TI       thickness of insulation wrap
410 REM LI       distance across conduit flats, inside surface
420 REM LO       distance across conduit flats, outside surface
430 REM          (including insulation wrap)
440 REM C5       coefficient used to find conduit corner bending moment
450 REM X        ratio of mid-surface corner bend radius to
460 REM          mid-surface distance across flats of conduit
470 REM YBAR     shift of N/A from centroidal axis towards center of curvature
480 REM HI       distance from N/A to inside surface
490 REM ALM      allowable membrane stress in conduit wall
500 REM ALMPB    allowable membrane-plus-bending stress in conduit wall
510 REM P1       limiting quench pressure per membrane allowable
520 REM P2       limiting quench pressure per membrane-plus-bending allowable
530 REM FT       dilational tensile force in inboard leg of TF coil
540 REM AC       combined cross-sectional area of all conduits in one TF coil

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550 REM ACASE    cross-sectional area in inboard leg of one TF coil
560 REM
570 PI = 3.14159
580 DEFINIT K
590 DIM I(3),J(3),BMAX(3),BB(3),R(3),NI(3),NT(3),AREA(3),C(6),XX(6)
600 PRINT
610 PRINT "CONDUCTOR GRADES SPECIFIED IN ORDER OF INCREASING FIELD"
620 PRINT
630 PRINT "Number of conductor grades (1, 2 or 3) ";
640 INPUT NGZ
650 FOR K=1 TO NGZ
660 PRINT "Maximum field for grade ";K;" T ";
670 INPUT BMAX(K)
680 PRINT "Conductor current, A ";
690 INPUT I(K)
700 NEXT K
710 PRINT
720 PRINT "Number of TF coils ";
730 INPUT NC
740 PRINT "Outside TF coil radius, cm ";
750 INPUT R(NGZ)
760 R(NGZ) = .01 * R(NGZ)
770 AT = BMAX(NGZ) * R(NGZ) / .0000002
780 PRINT "TF coil sidewall thickness, cm ";
790 INPUT TS
800 TS = .01 * TS
810 PRINT "TF coil bobbin ring, outer wall thickness, cm ";
820 INPUT TB
830 TB = .01 * TB
840 PRINT "TF coil radius ratio, outer-to-inner legs ";
850 INPUT RATIO
860 PRINT "Area inside conduit, cm2 ";
870 INPUT A
880 A = .0001 * A
890 PRINT "Conduit wall thickness, cm ";
900 INPUT TW
910 TW = .01 * TW
920 PRINT "Inside conduit corner bend radius, cm ";
930 INPUT RB
940 RB = .01 * RB
950 PRINT "Insulation thickness, cm ";
960 INPUT TI
970 TI = .01 * TI
980 LI = SQF(A + .8584*RB^2)
990 LO= LI + 2!*(TW +TI)
1000 F = .9
1010 PRINT "TF coil packing factor - default is 0.9 ";
1020 INPUT F1
1030 IF F1 <> 0! THEN F = F1
1040 PRINT "Allowable membrane stress intensity, ksi ";
1050 INPUT ALM

```

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1060 ALM = 6895000! * ALM
1070 PRINT "Allowable membrane-plus-bending stress intensity, ksi ";
1080 INPUT ALMPB
1090 ALMPB = 6895000! * ALMPB
1100 FOR K = NGZ TO 1 STEP -1
1110 R(K-1) = R(K)
1120 R(K-1) = R(K-1) - L0
1130 AREA(K) = PI*(R(K)^2-R(K-1)^2) - 2!*NC*TS*(R(K)-R(K-1))
1140 NT(K) = NC*INT(F*AREA(K) / (NC*L0^2))
1150 NI(K) = NT(K)*I(K)
1160 J(K) = NI(K) / AREA(K)
1170 IF K=1 THEN GOTO 1230
1180 IF K=3 THEN B = .0000002 * (AT - NI(3)) / R(K-1)
1190 IF (NGZ=3) AND (K=2) THEN B = .0000002 * (AT - NI(3) - NI(2)) / R(K-1)
1200 IF (NGZ=2) AND (K=2) THEN B = .0000002 * (AT - NI(2)) / R(K-1)
1210 IF B < BMAX(K-1) THEN GOTO 1280
1220 GOTO 1120
1230 AT1 = 0!
1240 FOR KK = 1 TO NGZ
1250 AT1 = AT1 + NI(KK)
1260 NEXT KK
1270 IF AT1 < AT THEN GOTO 1120
1280 NEXT K
1290 SCALE = AT / AT1
1300 FOR K=1 TO NGZ
1310 J(K) = SCALE * J(K)
1320 NI(K) = SCALE * NI(K)
1330 NEXT K
1340 BB(1) = .0000002 * NI(1) / R(1)
1350 IF NGZ>1 THEN BB(2) = .0000002 * (NI(1)+NI(2)) / R(2)
1360 IF NGZ=3 THEN BB(3) = .0000002 * (NI(1)+NI(2)+NI(3)) / R(3)
1370 KJUMP = 0
1380 FOR K = 1 TO NGZ-1
1390 IF BB(K) <= BMAX(K) THEN GOTO 1420
1400 BMAX(K) = .99 * BMAX(K)
1410 KJUMP = 1
1420 NEXT K
1430 IF KJUMP = 1 THEN GOTO 1100
1440 BMAX(1) = .0000002 * NI(1) / R(1)
1450 IF NGZ>1 THEN BMAX(2) = .0000002 * (NI(1)+NI(2)) / R(2)
1460 IF NGZ=3 THEN BMAX(3) = .0000002 * (NI(1)+NI(2)+NI(3)) / R(3)
1470 PRINT
1480 PRINT "      Grade      Bmax      Rmax,m      Rmin,m      NI,MAT      i.A/cm2"
1490 PRINT
1500 FOR K=1 TO NGZ
1510 PRINT USING"#####.###";K,BMAX(K),R(K),R(K-1),NI(K)*(.000001),J(K)*(.0001)
1520 NEXT K
1530 AU = 0!
1540 AT = 0!
1550 FOR K=1 TO NGZ
1560 AT = AT + NI(K)

```

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1570 AU = AU + AREA(K)
1580 NEXT K
1590 JAV = .0001*AT / AU
1600 PRINT
1610 PRINT "Average current density (A/cm2) = ";JAV
1620 PRINT "SCALE = ";SCALE
1630 FC = 0!
1640 FOR K = 1 TO NGZ
1650 FC = FC + .5*(BMAX(K)+BMAX(K-1))*NI(K)
1660 NEXT K
1670 SIGC = FC * LO / (4!*PI*R(0)*TW)
1680 FT = .5*BMAX(NGZ)*R(NGZ)*AT*LOG(RATIO)/NC
1690 FT = .55 * FT
1700 H = LI - 2!*RB
1710 AJ = TW * (4!*H + 2!*PI*RB + PI*TW)
1720 IF NGZ=3 THEN AC = AJ * (NT(1)+NT(2)+NT(3)) / NC
1730 IF NGZ=2 THEN AC = AJ * (NT(1)+NT(2)) / NC
1740 IF NGZ=1 THEN AC = AJ * NT(1) / NC
1750 ACASE = 2!*PI*TB*(R(NGZ)+R(0))/NC + 2!*TS*(R(NGZ)-R(0))
1760 SIGT = FT / (AC + ACASE)
1770 SIGE = SIGC + SIGT
1780 PRINT
1790 PRINT "
1800 PRINT "Transverse compressive stress in conduit wall "
1810 PRINT "Dilational tensile stress in conduit wall "
1820 PRINT "Stress intensity in conduit wall "
1830 IF SIGE <= ALM THEN GOTO 1910
1840 TB = NC*(FT/(ALM-SIGC) - 2!*R(NGZ)-R(0))*TS - AC) / (2!*PI*(R(NGZ)+R(0)))
1850 PRINT
1860 PRINT "Stress intensity in conduit wall exceeds allowable."
1870 PRINT "If conduit wall thickness and TF coil case sidewall thickness
1880 PRINT " are maintained, bobbin ring and outer wall thickness must be
1890 PRINT " increased to "; 100!*TB : " cm. ( ) limit stress intensity
1900 PRINT " to the stated allowable."
1910 FOR K = 1 TO 6
1920 READ XX(K), C(K)
1930 NEXT K
1940 DATA 0...0833,.1,.0594,.2,.0389,.3,.0220,.4,.0091,.5,0.
1950 X = (RB + TW/2!) / (LI + TW)
1960 FOR K = 2 TO 6
1970 IF X < XX(K) THEN GOTO 1990
1980 NEXT K
1990 C5 = C(K-1) + (C(K)-C(K-1)) * (X-XX(K-1)) / (XX(K)-XX(K-1))
2000 YBAR = RB + TW/2! - TW / LOG(1! + TW/RB)
2010 HI = TW/2! - YBAR
2020 P2 = ALNFB / ( (1.4142*(LI - 2!*RB) + 2!*RB) / (2!*TW) +
C5*(LI+TW)^2*HI / (TW*YBAR*RB) )
2030 P1 = 2!*TW*ALM / (1.4142*(LI - 2!*RB) + 2!*RB)

```

```
2040 PRINT
2050 IF P1 < P2 THEN GOTO 2090
2060 PRINT "Allowable quench pressure, atm. "; P2 / 101300!
2070 PRINT "(limited by membrane-plus-bending stress at inside surface)"
2080 GOTO 2110
2090 PRINT "Allowable quench pressure, atm. "; P1 / 101300!
2100 PRINT "(limited by membrane stress in corner)"
2110 END
```

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