

4. CONCEPTUAL DESIGN OF SUPERCONDUCTING MAGNET COILS

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4. CONCEPTUAL DESIGN OF SUPERCONDUCTING MAGNET COILS

4.1. INTRODUCTION

Stellarators are steady-state magnetic confinement fusion devices. Unlike tokamaks, which utilize a combination of ac and dc magnetic fields, stellarators use only dc magnets since poloidal-field modulation is not required during plasma start-up or for position control. From a magnet design perspective, the advantages inherent to the stellarator are the suitability of conventional voltage comparison techniques for quench detection and device operation without superconductor ac losses.

In this work, a conceptual magnet internal design is developed to meet the operating requirements representative of stellarator power plants. These requirements, established by the Stellarator Power Plant Study (SPPS) for the modular Helias-like heliac (MHH), are presented in Table 4.1-I. Key design drivers are the 14.5 T peak magnetic field intensity and 34.7 MA/m² effective coil current density, along with a maximum nuclear heat input to the coil of roughly 800 W/m³. This combination of requirements provides an unprecedented cooling design challenge. It implies magnet design feasibility is strongly coupled to mitigation of hot spot temperature rise for this type of application.

Also noteworthy, as illustrated in Fig. 4.1-1, is the highly non-planar, non-symmetric geometry of the magnet coils as they are presently envisioned for stellarator-based power plants. The most significant geometric characteristic of these coils is the presence of at least one, small radius, out-of-plane bend. Since the minimum bend radii are extremely small fractions of the gross packaging dimensions, magnet internal design is impacted significantly by stellarator topology. This impact manifests itself in the necessary design tradeoff between quench voltage and quench temperature rise. As bend radius decreases, allowable conductor cross-section area and current capacity decrease, causing an increase in total turn count to maintain a constant ampere-turns values. To achieve acceptable quench temperature rise, small time constants for field collapse are desirable, so dump resistance increases with coil inductance according to the square of the total number of conductor turns. The resulting increase in peak quench voltage must be reconciled against the voltage standoff capability of the winding pack insulation design.

Table 4.1-I.
SPPS Magnet Design Requirements

Number of magnets	32
Average coil radius	4.0
Radial coil height (m)	0.78
Lateral coil width (m)	0.51
Minimum coil curvature radius (m)	0.34
Minimum distance between coils (m)	0.12
Plasma average major radius (m)	14.0
Total coil current (MA)	13.8
Total inductance (one-turn) (μH)	816
Stored magnetic energy (GJ)	78
Max magnetic field at coil (T)	14.5
Magnetic field at plasma axis (T)	5.0
Peak coil nuclear heat load (W/cm^3)	807

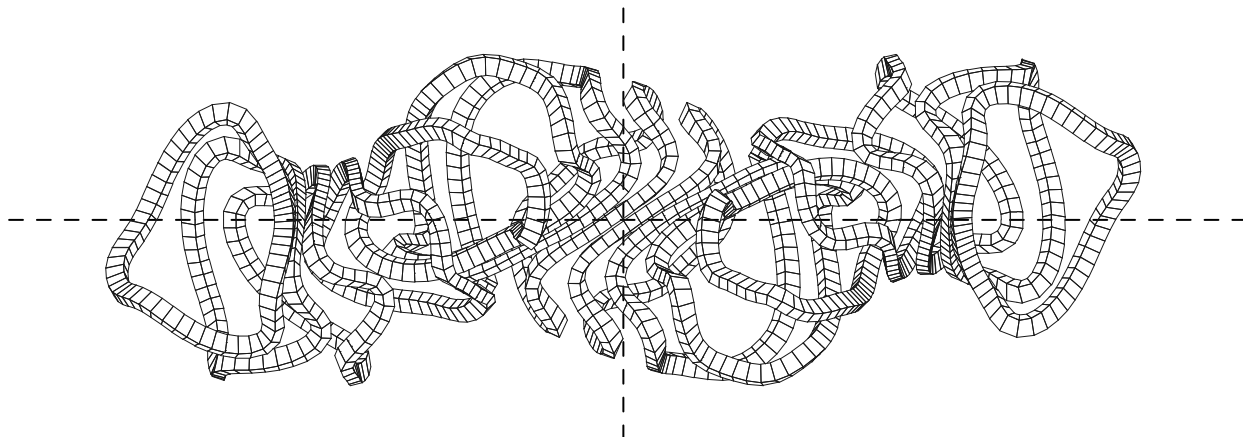


Figure 4.1-1. Elevation view of an 8 magnet quadrant as viewed from the central axis.

The coil concept development philosophy underlying this study was to extrapolate and adapt the superconductor designs for the International Thermonuclear Experimental Reactor (ITER) and Toroidal Plasma experiment (TPX) to the performance regime defined by the stellarator power plant magnet requirements. This philosophy assumed a design baseline consisting of an Incoloy-908-jacketed Nb₃Sn cable-in-conduit conductor (CICC), with forced convection heat removal using sub-cooled supercritical helium, and a wind/react/vacuum-pressure-impregnation (VPI) coil manufacturing sequence. From this baseline, a conceptual point design for the SPPS magnet winding pack was postulated and a theoretically-feasible preliminary design implementation was developed.

Section 4.2 summarizes the design effort applied to the SPPS magnet internal arrangement and presents a description of the present embodiment of the winding pack design concept as it has evolved to date. Section 4.3 presents the results of design analyses which support concept feasibility. The scope of these analyses was limited to coil grading, steady-state thermal-hydraulic simulation, quench performance, and conductor operating margins.

4.2. DESIGN OF MAGNET INTERNALS

4.2.1. Description

The proposed magnet architecture is based upon a graded winding configuration, as depicted in Fig. 4.2-1. Grading is achieved in this context by partitioning the winding window into two regions such that a different ampere-turn coil resides within each region. This partitioning consist of a low ampere-turn inner coil (aka insert) nested within a high ampere-turn outer coil (aka outsert). They are energized by separate power supplies injecting different amounts of electrical current. A theoretically-feasible design implementation of this graded winding concept is depicted by the dimensioned layout illustration shown in Fig. 4.2-2. The following discussion refers to this figure.

The outsert portion of the winding pack is further subdivided into two coils, designated as outsert coils A and B. Both outer coils are layer-wound with 144 turns in 8 nested layers (*i.e.*, 18 turns per layer) for a total current of 6.55 MA per coil, or 45.5 kA per turn. Each outsert coil is center-tapped and slaved to its own dedicated resistive dump circuit and charging power supply. Internal heating resulting from neutron attenuation is dissipated convectively by ventilation of the conductors with supercritical helium flowing at a rate of 3.5 g/s per layer at inlet temperature and pressure conditions of 3 K and 1.25 MPa, respectively.

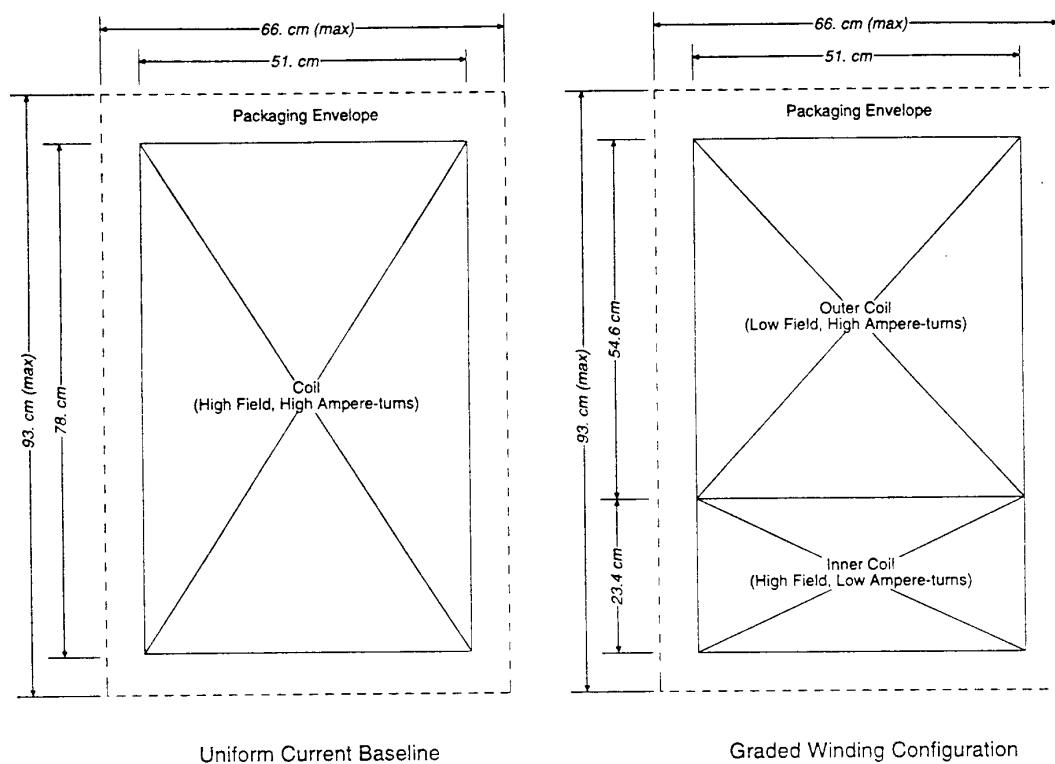


Figure 4.2-1. Comparison of uniform current density and graded coil concepts.

The insert portion of the winding pack is also layer-wound for a total turn count of 240 turns. With a 10 layer radial build, the number of turns per layer is 24. Like the outsert coils, the insert coil is also forced-flow cooled with supercritical helium. However, key differences exist. First, because of a smaller minimum bend radius, this coil uses a smaller conductor. Second, the close proximity of the insert to the plasma implies higher mass flow rates are needed to remove greater energy deposition by neutrons penetrating beyond the radiation shields. Results of thermal-hydraulic simulations suggest peak helium mass flow rates as great as 8 g/s per layer at inlet conditions of 1.25 MPa and 3 K are needed to achieve temperature margins greater than 0.5 K. A third difference is that the insert coil includes a square auxiliary coolant conduit with a 13 mm diameter circular channel. This channel is located between the first and second layers of CICC and rasters in the same direction as the first CICC layer. It is flushed with supercritical helium at a flow rate of 0.1 kg/s at inlet conditions of 3 K and 1.25 MPa. It augments heat removal from conductor layers 1 and 3 by conduction across turn insulation barriers to the auxiliary coolant stream.

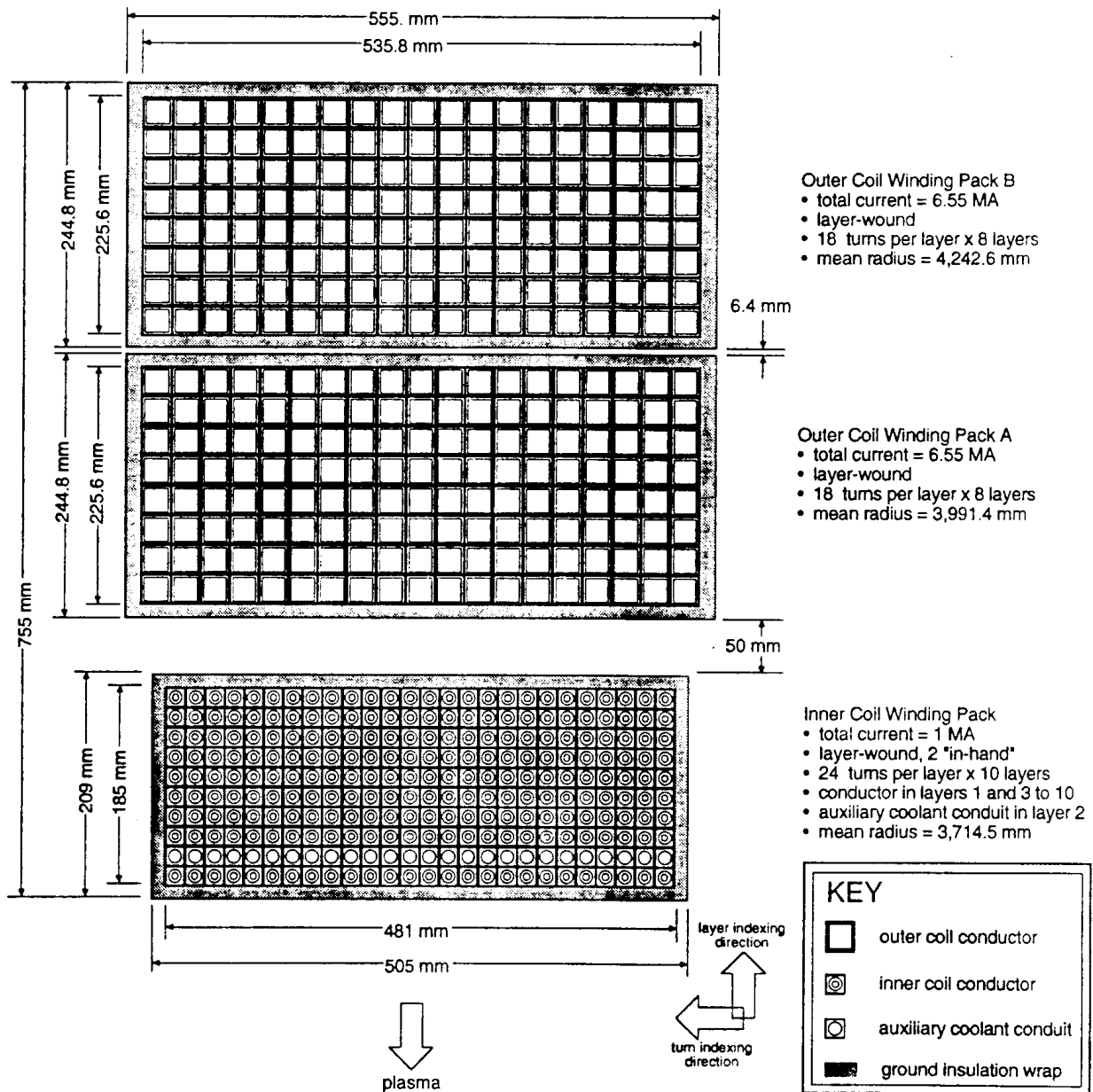


Figure 4.2-2. Design implementation of graded coil concept.

Both the insert coil conductors and auxiliary coolant conduit are wound “two in-hand” so that temperature rise and pressure drops can be maintained within tolerable limits. Here, the “two-in-hand” winding technique means that, within any given 24 turn coil layer, 2 sets of 12 continuous turns of conductor are connected electrically in series, but hydraulically in parallel. A design ramification is complex electrical joint and hydraulic plumbing layouts within highly confined clearance spaces between adjacent magnets. A design solution is illustrated in Fig. 4.2-3. It entails staggering the winding pack entry and exit points of the conductors by one half of a turn. Accordingly, two pairs of header boxes would be mounted at the “sides” of the insert coil and would contain helium inlet and outlet manifolds as well as layer-to-layer electrical joints as needed. This approach reduces the number of components within a given insert coil header box by 50%. Since the outer coil does not require “two in-hand” winding, it is equipped with one pair of header boxes. Because header boxes compete for in-vessel space with other fusion core equipment, like beam ducts and instrumentation feedthroughs, their actual placement is a system integration issue.

The two notional conductor designs are presented in Fig. 4.2-4. As envisioned, all coils would employ Nb₃Sn CICC. Both CICC designs use chrome-plated wires, to prevent sintering during the reaction process, and Incoloy 908 conduits. Selection of Incoloy 908 as the conduit material increases manufacturing cost, but mitigates against degradation of critical current density due to differential thermal contraction strain during cool-down.

The insert coil CICC is a reduced scale, modified version of the ITER model coil conductor. It consists of a square 16 mm conduit drawn down around an annular cable of superconducting wires tightly compacted around a 7 mm OD × 5 mm ID coil spring running the entire length of conductor. This central channel provides a low impedance path for the helium mass flows required to dissipate nuclear heat. Without this central channel, supercritical helium moving through the cable void space would eventually enter the subcritical state and unreliable conductor performance would probably occur as a consequence of two phase flow. A triplex-based, 3 or 4 stage cabling process could be used to spiral 6 to 8 subcables around the central channel. With a cable space void fraction of 0.3, wire diameter would be comparable to the Westinghouse LCT wire [1], ranging from 0.62 mm to 0.7 mm. However, Nb₃Sn processing yields would have to be greater than those for LCT since a Cu to non-Cu ratio of 1:1 would be needed to achieve margin. The ratio of minimum bend radius to conductor dimension is $340/16 \simeq 21.3$.

The outsert coils CICC is a larger version of the Westinghouse LCT conductor. It consists of a square 25.7 mm conduit, or jacket, drawn down around a 5 stage cable of 486 superconducting wires. With a cable space void fraction of 0.35 and conduit

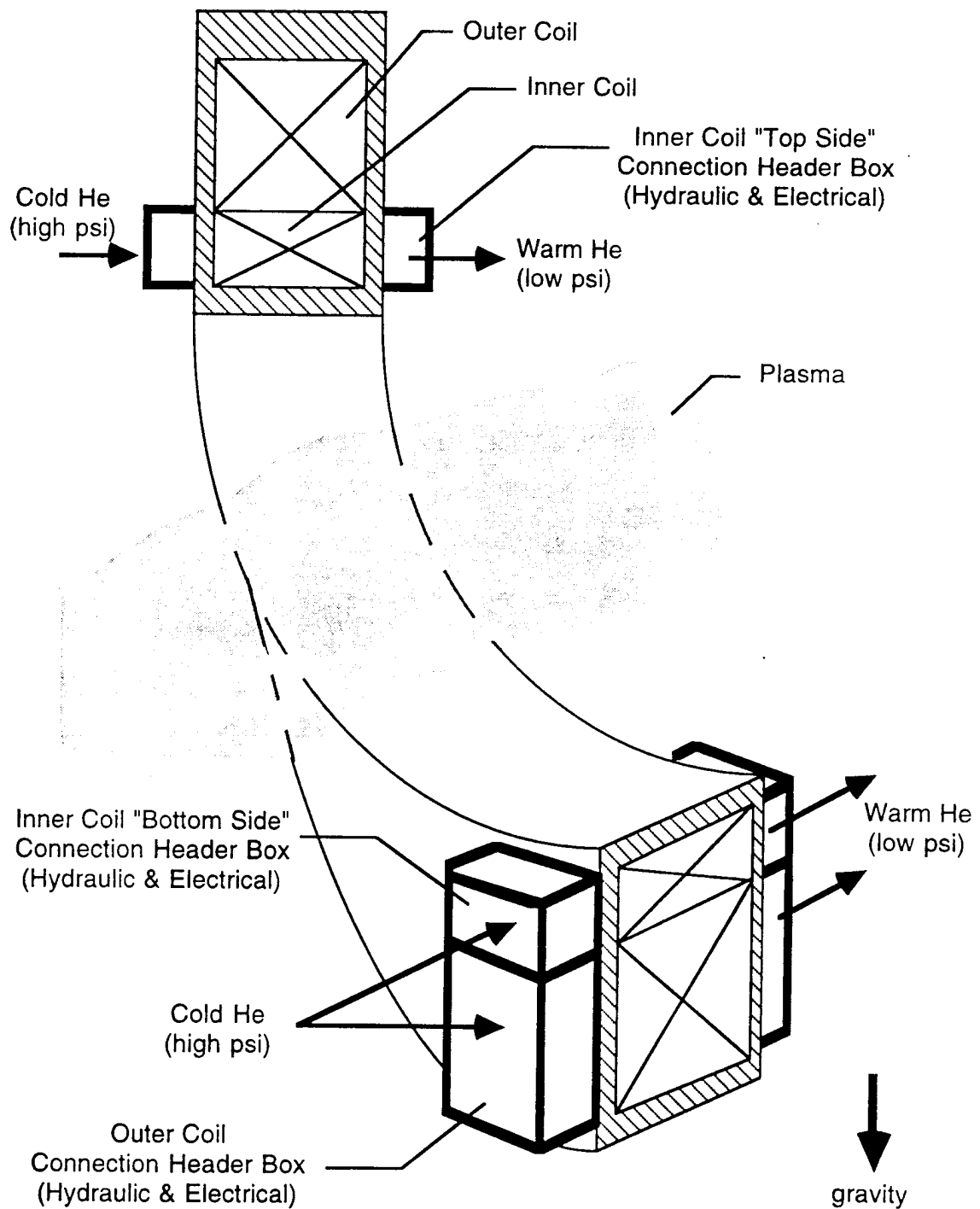
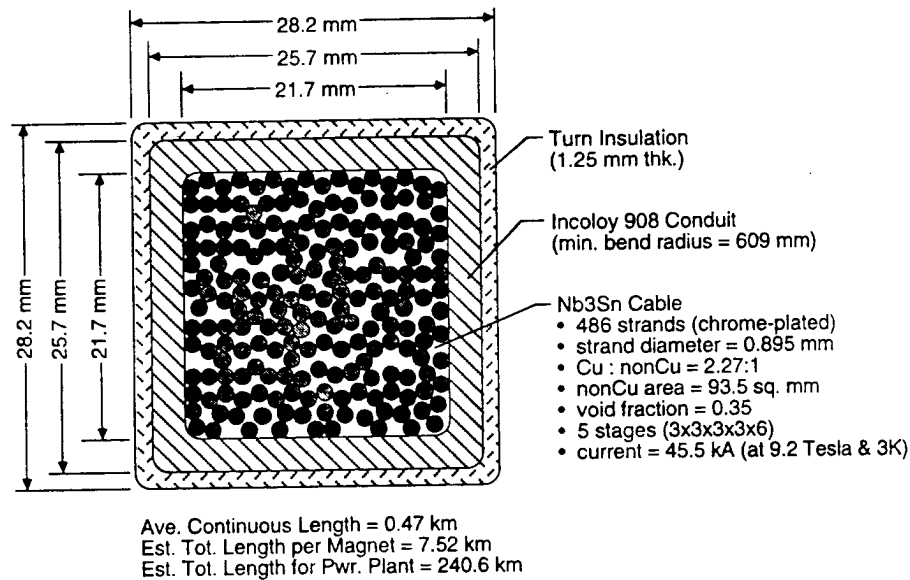


Figure 4.2-3. A concept for positioning of electrical and hydraulic connection header boxes.

Outer Coil CICC (preliminary)



Inner Coil CICC (preliminary)

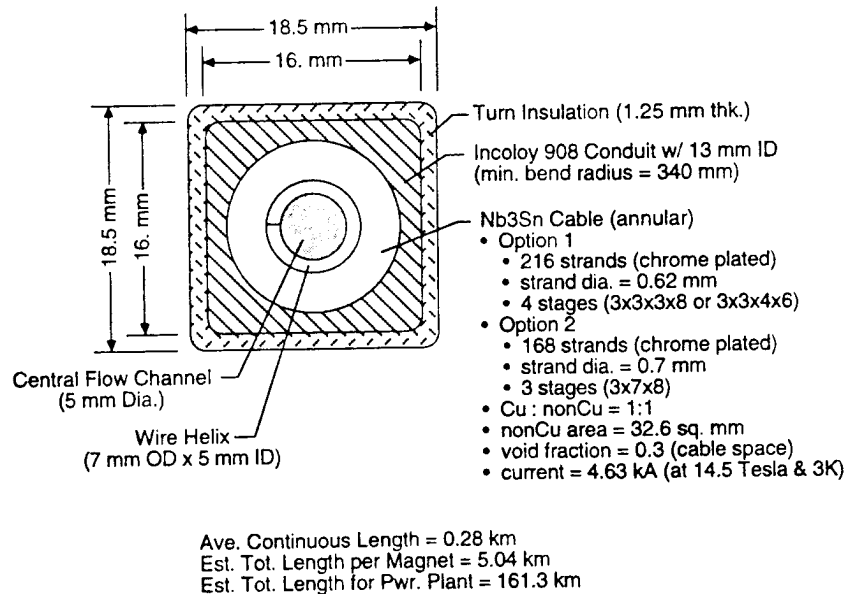


Figure 4.2-4. Notional conductor design configurations.

wall thickness of 2 mm, wire diameter would equal approximately 0.9 mm, roughly 32% greater than the LCT wire. No central channel is needed since adequate mass flow rates can be developed without decreasing pressure below the critical value near 0.23 MPa. The ratio of minimum bend radius to conductor dimension is $609/25.7 \simeq 23.7$.

Both conductors would be wrapped with turn insulation possessing a nominal compressed build of 1.25 mm (*i.e.*, four, 0.012" half-lapped tapes). If the coils were fabricated using present technology and techniques, turn and ground wall insulation would probably default to fiberglass or ceramic fabric tapes. This approach presents a tractable level of risk for the insert coil since predicted quench voltages are less than 1 kV. To assure reliable operation of the outsert coils, void-free vacuum pressure impregnation (VPI) with epoxy and a center-tapped winding would be absolute necessities since the predicted quench voltage drop to ground is 15 kV for a non center-tapped configuration (30 kV if both outserts are consolidated into a single non center-tapped coil). If coils are inserted within support cases after reaction of the conductor, then application of polyimide film sheets as a ground wall barrier becomes an option. Our key operating assumption has been that a cost-effective, reliable insulate-wind-react-VPI manufacturing sequence is available if coil fabrication were to begin at some future time, perhaps as much as twenty years hence.

A candidate case structure design concept is illustrated in Figs. 4.2-5 and 4.2-6. Figure 4.2-5 depicts the magnet case without coils. Figure 4.2-6 depicts the coils inserted within the case. The case is constructed from four circumferential bands and three pairs of side wall plate assemblies. The circumferential bands also function as mandrels for coil winding, so they are referred to as mandrel bands. All case material is 316LN stainless steel or, alternatively, Incoloy 908. The insert coil mandrel band is overwrapped with a 24 turn coil of conductor conduit (w/o cable) on its outermost surface. Supercritical helium flows in this conduit, intercepting nuclear heat within the band before it conducts to the first conductor layer. Usage of CICC conduit as a coolant tube negates the need for extensive machining or welding operations performed on the mandrel band to interface cooling hardware. A similar arrangement exists between the insert coil and the adjacent outsert coil. Accompanying flange surfaces for all mandrel bands are provided by side wall plates joined to the bands. As presently envisioned, this joining is accomplished by full plug welds located at through-running holes in the side wall plates. The proposed usage of plug welds is intended as a potential means of eliminating or greatly reducing plate distortion due to weld shrinkage. Since the primary load transfer path passes through these plug welds, their number and size must be chosen to provide sufficient section area such that weld zone stresses do not reach yield values. These plug welds are supplemented

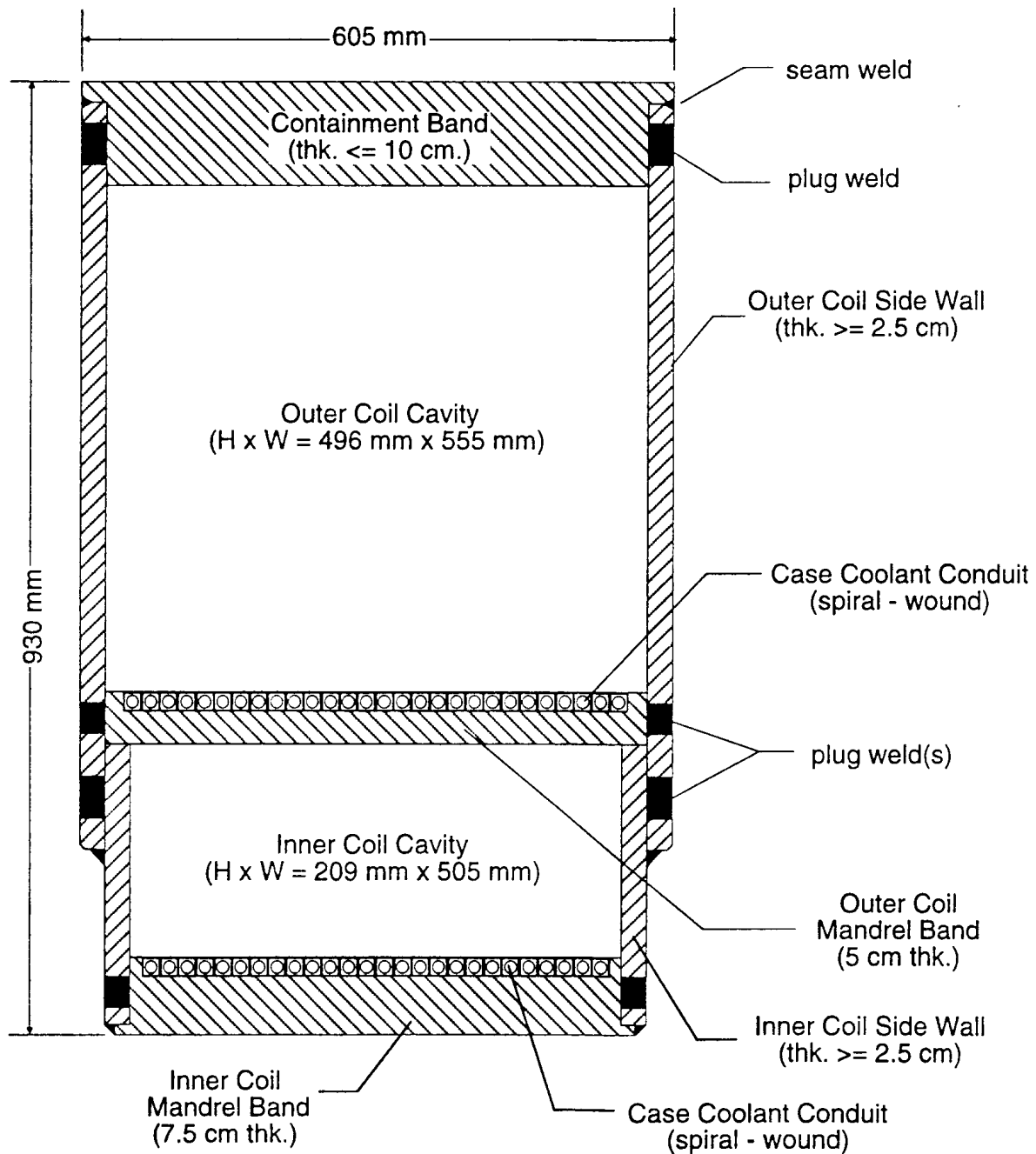
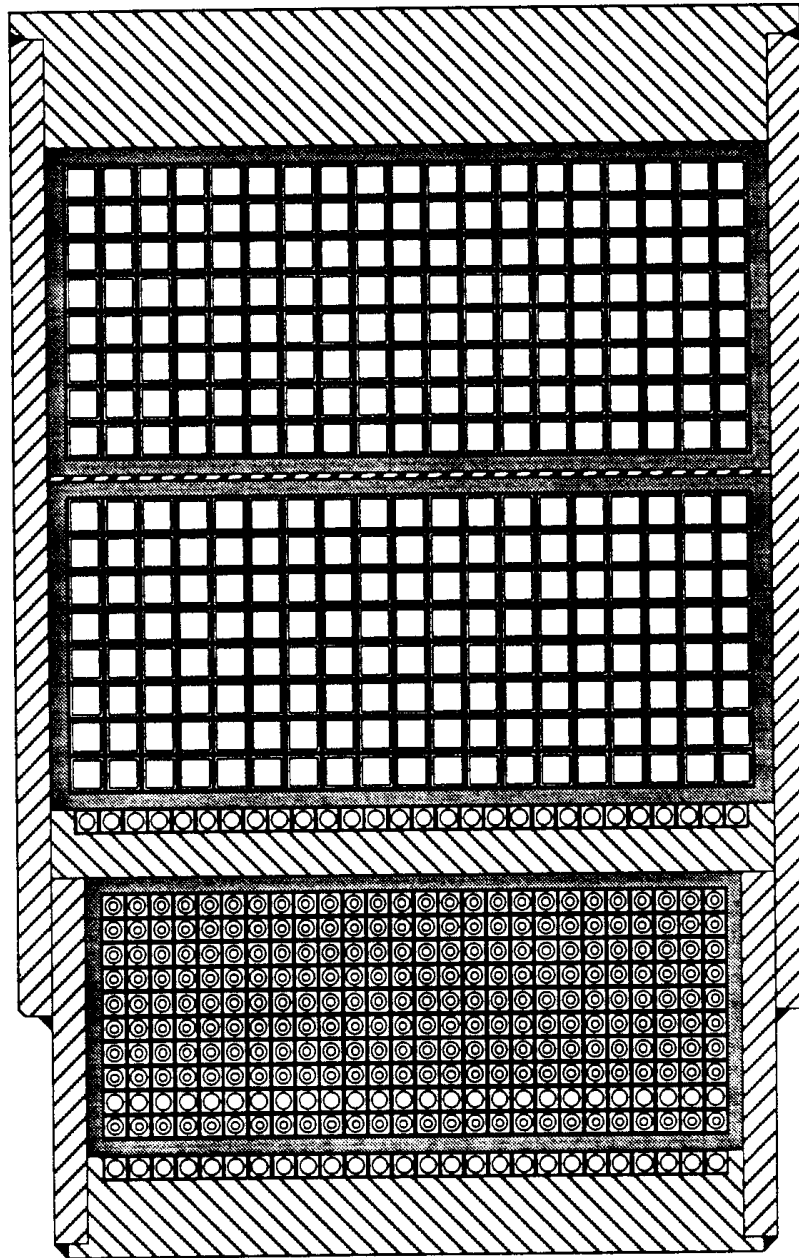


Figure 4.2-5. Magnet case concept without coils.

**NOTE:**

- 1) Plug welds for joining plate structure assemblies are not depicted.
- 2) Internal water-cooled chill blocks for coil protection during case welding operations are also not depicted.

Figure 4.2-6. Cross-section view of magnet case and coils.

with continuous fillet “seam” welds which traverse the case circumferentially, parallel to the direction of current flow. Although the seam welds improve case rigidity and overall load-bearing strength, their primary function is to seal the case for VPI during manufacture and to contain conductor coolant in the event of conduit leakage. Closing the case is a 7.5 cm thick outer belt which contains the outsert coil. Again, plug and seam welds would be used for joining.

In general, considerable care must be taken during welding to prevent thermal damage. For mitigating against thermal damage to the winding pack, each mandrel to flange plate interface surface undergoes a right angle bend to reduce exposure of the ground wall insulation material to weld flash. In addition, although not depicted in either Fig. 4.2-5 or Fig. 4.2-6, the use of integral, water-cooled, chill blocks is envisioned to shield the winding pack from weld-related heat effects. These chill blocks would be located within shallow channels milled into the interior-facing sides of all mandrel and side wall plates.

4.2.2. Rationale

4.2.2.1. Cooling Design

To produce magnetic fields in the >14 T regime, recent magnet development efforts have relied upon coil operation at 1.8 K for achieving adequate superconductor temperature margin. At the National High Magnetic Field Laboratory (NHMFL), for example, the design of the 14-T, Nb_3Sn , CICC outsert [2] of a 45-T hybrid magnet system was based on static HeII cooling. And in Japan, a pool-boiled, fully-superconducting, $(\text{Nb,Ti})_3\text{Sn}$ magnet system [3] successfully achieved 20 T while operating in a bath of saturated HeII.

Although both of the aforementioned cooling design approaches are suitable for laboratory applications, they are not readily scalable to power-plant operating environments. Consider, for example, a hypothetical CICC coil stabilized by static HeII cooling at 1.8 K with a peak nuclear heat input of roughly 1000 W/m^3 per unit length of cable. With a maximum critical channel heat flow limit [4] of $7.4 \text{ W/cm}^{5/3}$ for HeII at atmospheric pressure, the spacing of 1.8 K coolant ports along the conduit wall could not exceed 16 m without causing fluid temperature to exceed the lambda transition value at the halfway point between the ports. Alternatively, if magnets are HeII bath-cooled, winding packs must be provided with a multiplicity of internal coolant channels to dissipate the nuclear heat load adequately and maintain near isothermal conditions. These channels increase structural compliance of the winding packs, thereby increasing the difficulty of reliable

load transfer between adjacent magnets. Bath-cooling of non-planar stellarator coils also presents the nontrivial challenge of preventing vapor lock along horizontal portions of the coolant channels. To prevent bubble formation, these channels must be sized also according to the critical channel heat flow limit. Consequently, effective current density would tend to decrease.

Based on the aforementioned considerations, forced-flow cooling with supercritical helium was selected as the preferred option for the design baseline. Subcooling the helium to 3 K was motivated by results of conductor operating margin analysis, as documented in Section 3.3. Since the flow path lengths are very long (approximately 25 m per turn on average), helium inlet pressure was set to 1.25 MPa to provide sufficient pressure head for sustaining single phase flow at the flow rates required for removing large nuclear heat loads. Furthermore, these inlet conditions are approximately equivalent to those of the Westinghouse LCT coil [1], so they are therefore not a far extrapolation from known refrigerator design space. Selection of the CICC approach to forced-flow cooling also provides the benefits of a distributed winding support and coolant vessel structure as well as minimum quench energies greater than 150 mJ/cm³ of strand material [5].

Three winding options were considered from the perspective of cooling design. These options were single-pancake winding, double-pancake winding, and layer winding. Layer winding was chosen for two reasons. First, in comparison to double-pancake CICC coils, layer-wound CICC coils do not suffer from the significant increase [6] in hot spot temperature, near the midpoint of the flow path, beyond that which would occur if the turns were adiabatically isolated from one another. In layer wound geometries, coolant temperature increases monotonically along the flow path, attaining the hot spot value at the outlet. Second, due to the aspect ratio of the nominally-rectangular cross-section of the winding window, layer winding translates to shorter flow paths than those associated with double-pancake or even single-pancake winding. Hence, pumping power and quench pressure rise are reduced.

4.2.2.2. Coil Grading and Conductor Size

As illustrated in Fig. 4.2-7, the benefit of a graded winding pack versus an ungraded winding pack (*i.e.*, uniform J) is that current can be shifted away from the high field region, where conductor operating margins are small, to a low field region where the margins are comparatively large. This approach increases margin within the high field region at the expense of relatively small percentage decreases in margin within the low field region. However, the redistribution of current must be determined with care so

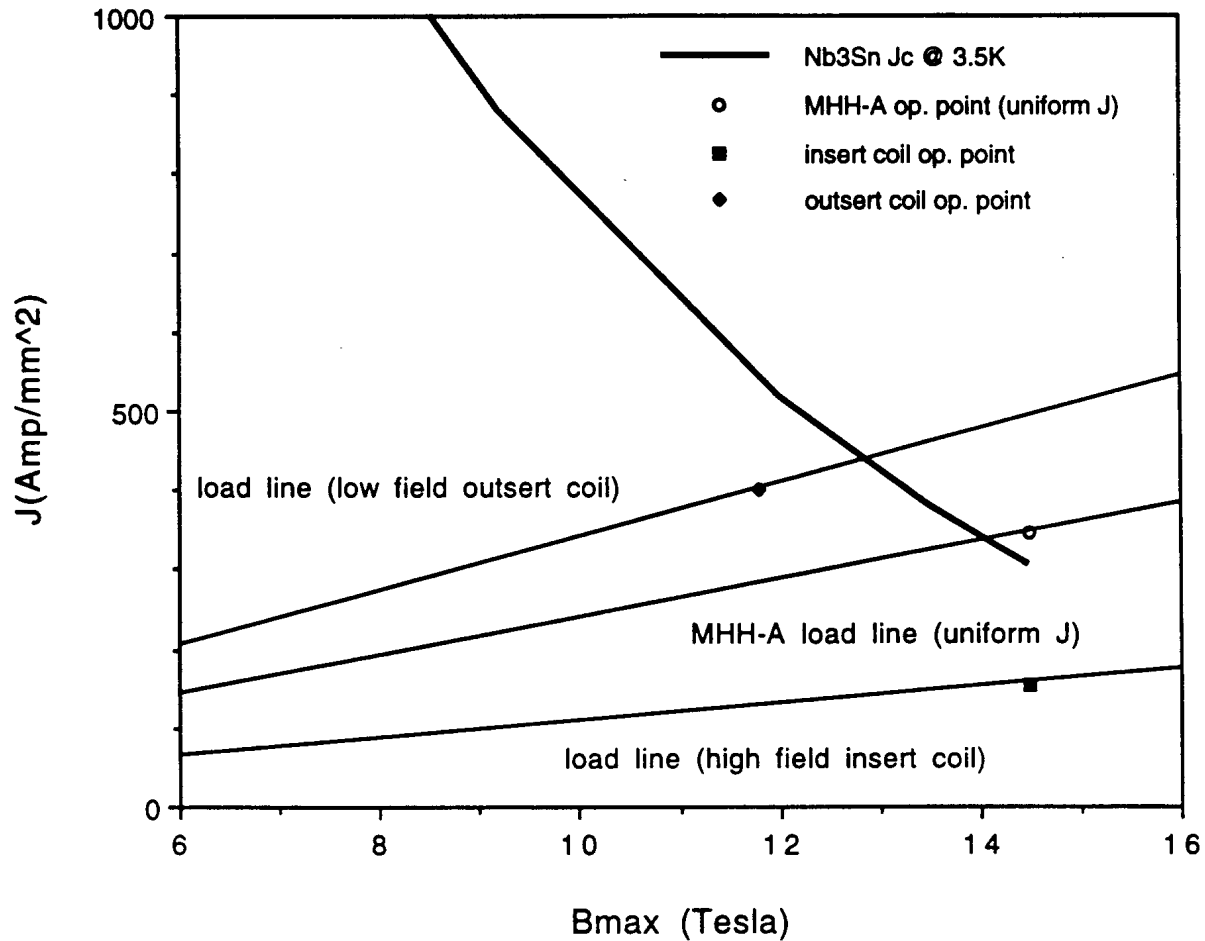


Figure 4.2-7. Benefit of graded winding pack vs a uniform current density configuration.

magnetic field at the plasma axis is maintained at roughly 5 T. Magnetic field calculations were undertaken to determine that the change in plasma axis field attributable to the chosen grading scheme would not exceed 5% of the nominal value. Results of these analyses are presented in Sec. 4.3.1 and suggest the plasma field impact of the chosen grading scheme is limited to about 3%. The effect on field quality (*i.e.*, harmonics), however, was not assessed.

The most significant coil geometric characteristic is the presence of at least one, small radius, out-of-plane bend. Since the coils minimum bend radii are extremely small fractions of the gross packaging dimensions, magnet internal design is impacted significantly by stellarator topology. This impact manifests itself in the necessary design tradeoff between quench voltage and quench temperature rise. As bend radius decreases, allowable

CICC cross-section area and current capacity must decrease, causing an increase in total turn count to maintain a constant ampere-turns value. To achieve acceptable quench temperature rise, small time constants for field collapse are desirable, so dump resistance increases with coil inductance according to the square of the total number of conductor turns. The resulting increase in peak quench voltage must be reconciled against the voltage standoff capability of the winding pack insulation design. In general, fewer turns (*i.e.*, larger CICC dimensions) translates to lower quench voltage, hence less dielectric stress on the insulation design.

Based on results of ITER model coil CICC bending tests [7], the bend radius limit is estimated to be no less than 20 times the conduit external dimension. This criterion was applied to size both insert and outsert coil CICC conduits. The larger bend radius of the outsert coils allowed a larger CICC, hence a reduction in self-inductance. However, this reduction was not sufficient for achieving quench voltages less than 20 kV to ground. To limit outsert coil quench voltage to less than 20 kV while simultaneously limiting quench temperature to less than 300 K, the outsert coil was subdivided such that each outsert “subcoil” would possess the same number of turns and would, in the event of a detected quench, discharge into a separate dump resistor. Results of quench performance analyses presented in Section 3.4 suggest this coil partitioning scheme reduces quench temperature to roughly 150 K at a quench voltage of 15 kV, or ± 7.5 kV if each outsert winding is center-tapped. Such quench performance was judged as providing a reasonable balance between design conservatism, system complexity, and technical risk.

4.2.3. Cooling Design Options for Insert Coil

At present, the predicted minimum temperature margin of the insert coil is 0.7 K. Three cooling design options have been identified for increasing this minimum temperature margin. These options are:

1. Change present routing of helium through “two-in-hand” wound layers from parallel flow to counterflow.
2. Remove electrical insulation wrap surrounding each turn of the auxiliary coolant conduit layer, thereby reducing the transverse heat conduction impedance between the first three coil layers by nearly 50%.
3. Add a second layer of auxiliary coolant conduit. This can be done by replacing the existing auxiliary coolant conduit layer with a CICC layer and replacing the

resulting first and third CICC layers with auxiliary coolant conduit. (Here, the tradeoff centers around doubling the local heat removal capacity to reduce hot spot temperature versus slightly decreasing temperature margin as conductor current increases by 12.5%).

Analysis of these design optimization measures was beyond the scope of the present feasibility study. However, the availability of various approaches to increasing design conservatism is another favorable indicator of overall magnet design feasibility.

4.3. DESIGN ANALYSIS

4.3.1. Coil Grading

Four alternative schemes for coil (*i.e.*, current) grading were considered. These alternative schemes are illustrated in Fig. 4.3-1. Scheme 1 employs three coils in an attempt to reduce manufacturing cost by utilizing NbTi superconductor in 30% of the current window. The other schemes all employ two coils. Schemes 2 and 3 are geometrically identical, but operating temperature and superconductor differ. Scheme 2 operates with superfluid helium, allowing the large outer coil to be wound with the less expensive NbTi conductor. Scheme 3 operates at subcooled supercritical helium temperatures and would utilize all Nb₃Sn superconductor. Scheme 4 is essentially a geometric variation of Scheme 3. Also shown on Fig. 4.3-1 are estimated values and locations for magnetic field intensity at the coil boundaries. These estimates were made by visual inspection of magnetic field plots provided [8] to Westinghouse. It is important to note that it was assumed for expediency that the fields at the coil do not change significantly by grading. This simplifying assumption is clearly not correct in a rigorous sense, but it uncouples the problem, allowing the conductor temperature margin calculations to proceed for purposes of down-selecting a preferred scheme.

Magnetic Field Code (MAFCO) was used to develop two simple Biot-Savart electromagnetic models for evaluating these schemes (Figs. 4.3-2 and 4.3-3). The first model, presented in Fig. 4.3-2, is a circular toroid consisting of a total of 32 solenoid coils whose centers are coplanar and located at 14 m from the global origin. These coils have the same 4 m average radius as the stellarator coils. The coil centers are spaced at equal angular increments around a 28 m diameter circular path. The coils are oriented so that their mid-planes all intersect at a vertical line running through the global origin. The

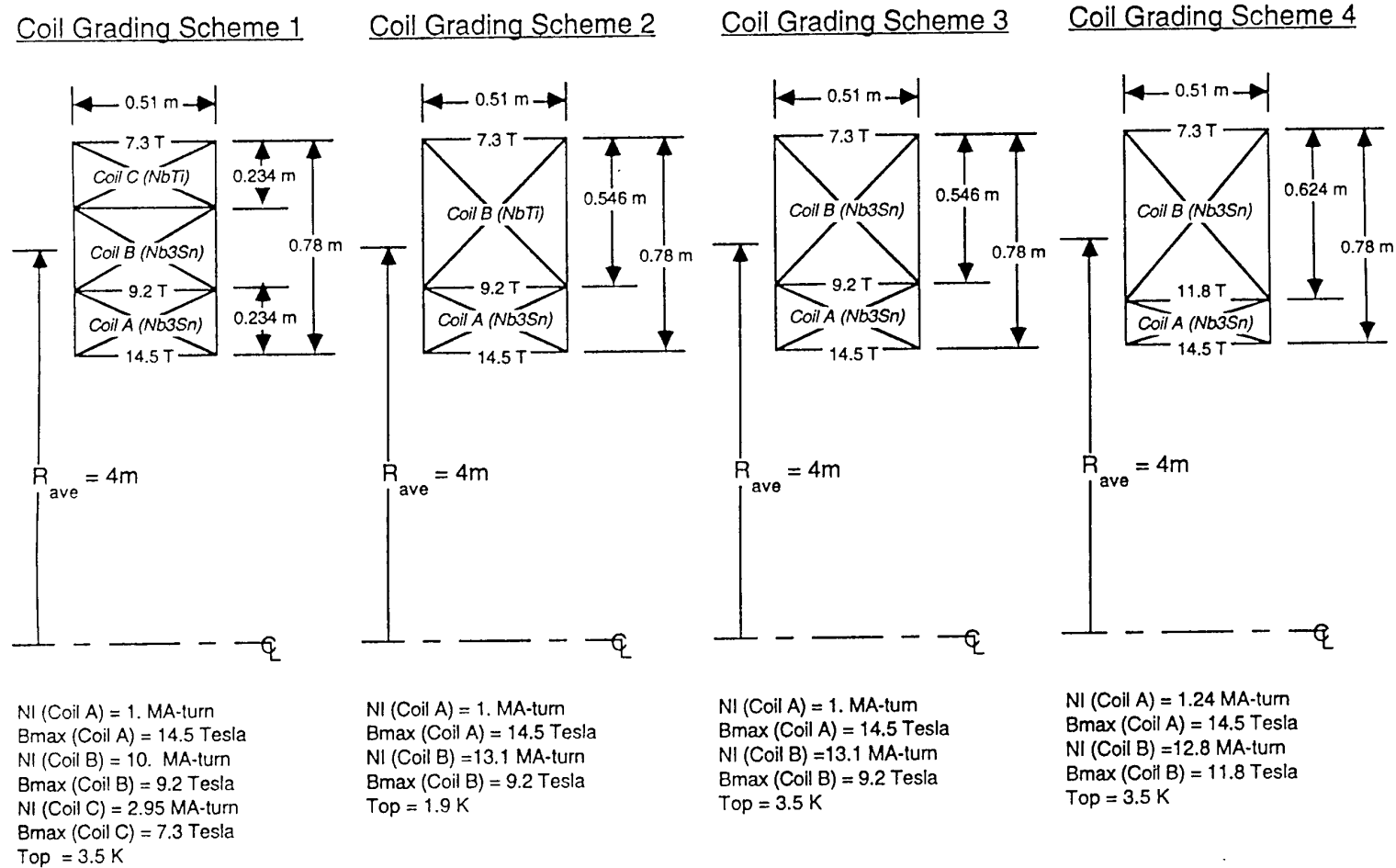


Figure 4.3-1. Candidate schemes for coil grading.

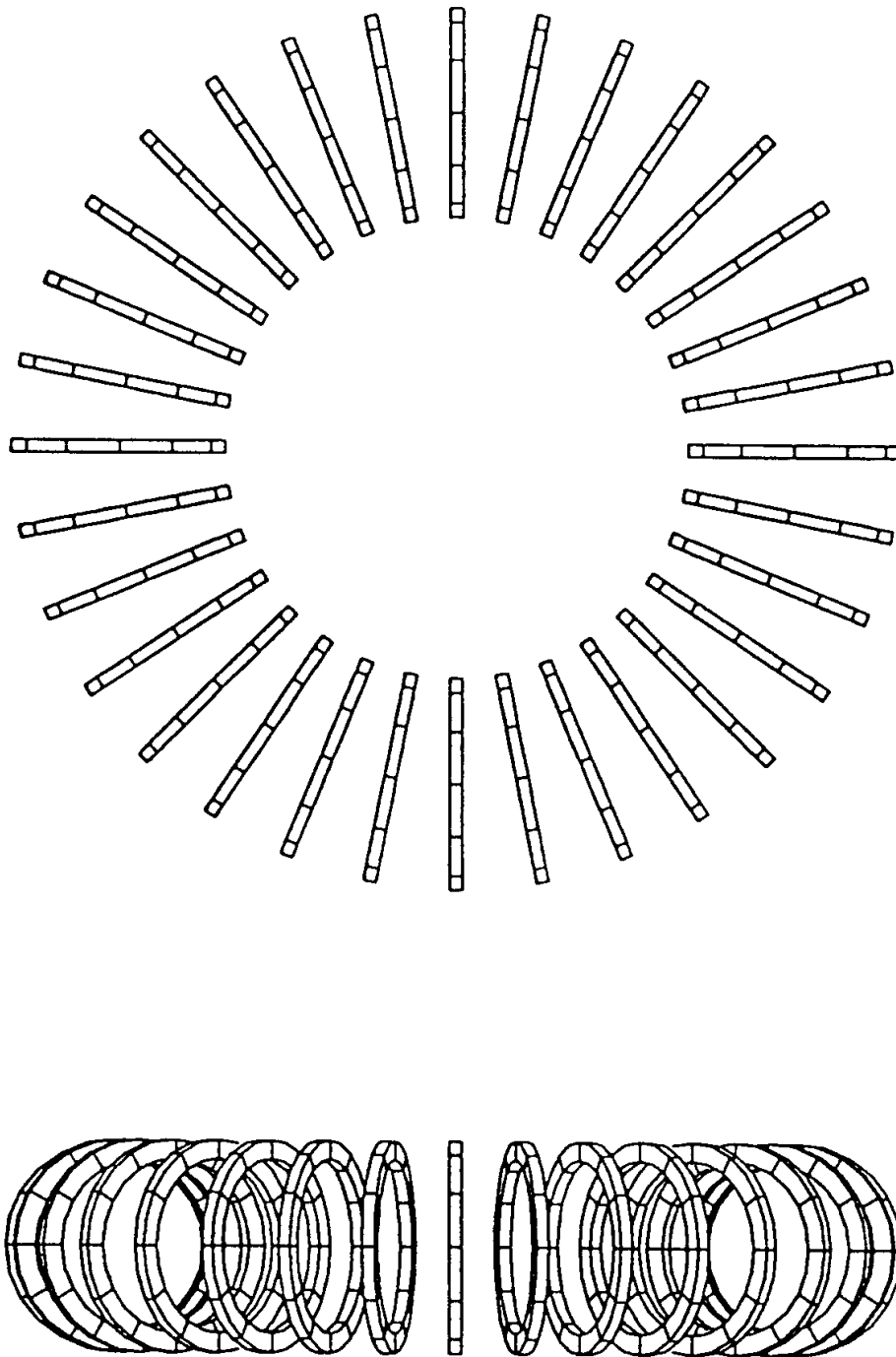


Figure 4.3-2. Plan and elevation views of circular toroid model used to predict relative changes in plasma magnetic field due to coil grading.

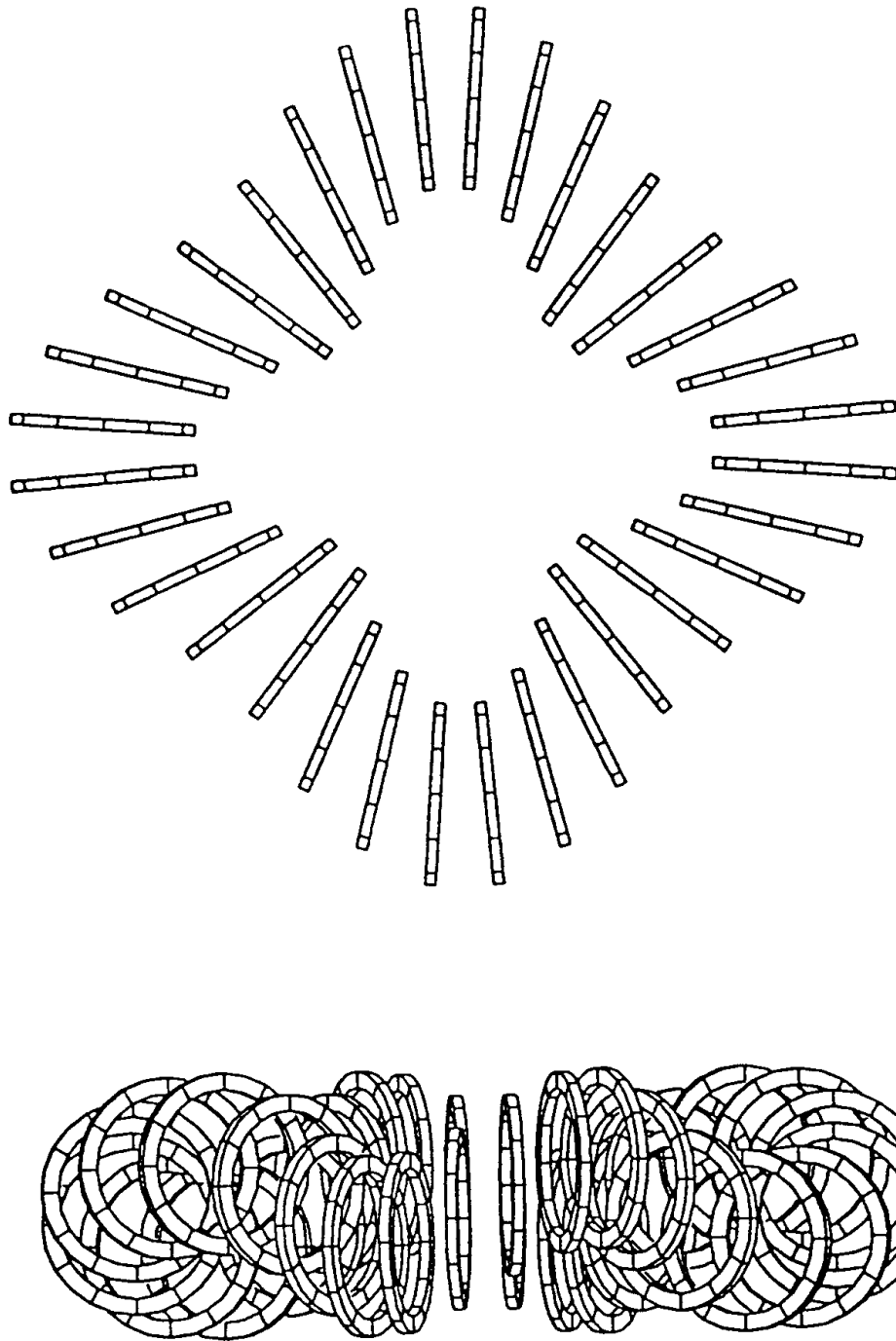


Figure 4.3-3. Plan and elevation views of 4-lobed toroid model used to predict relative changes in plasma magnetic field due to coil grading.

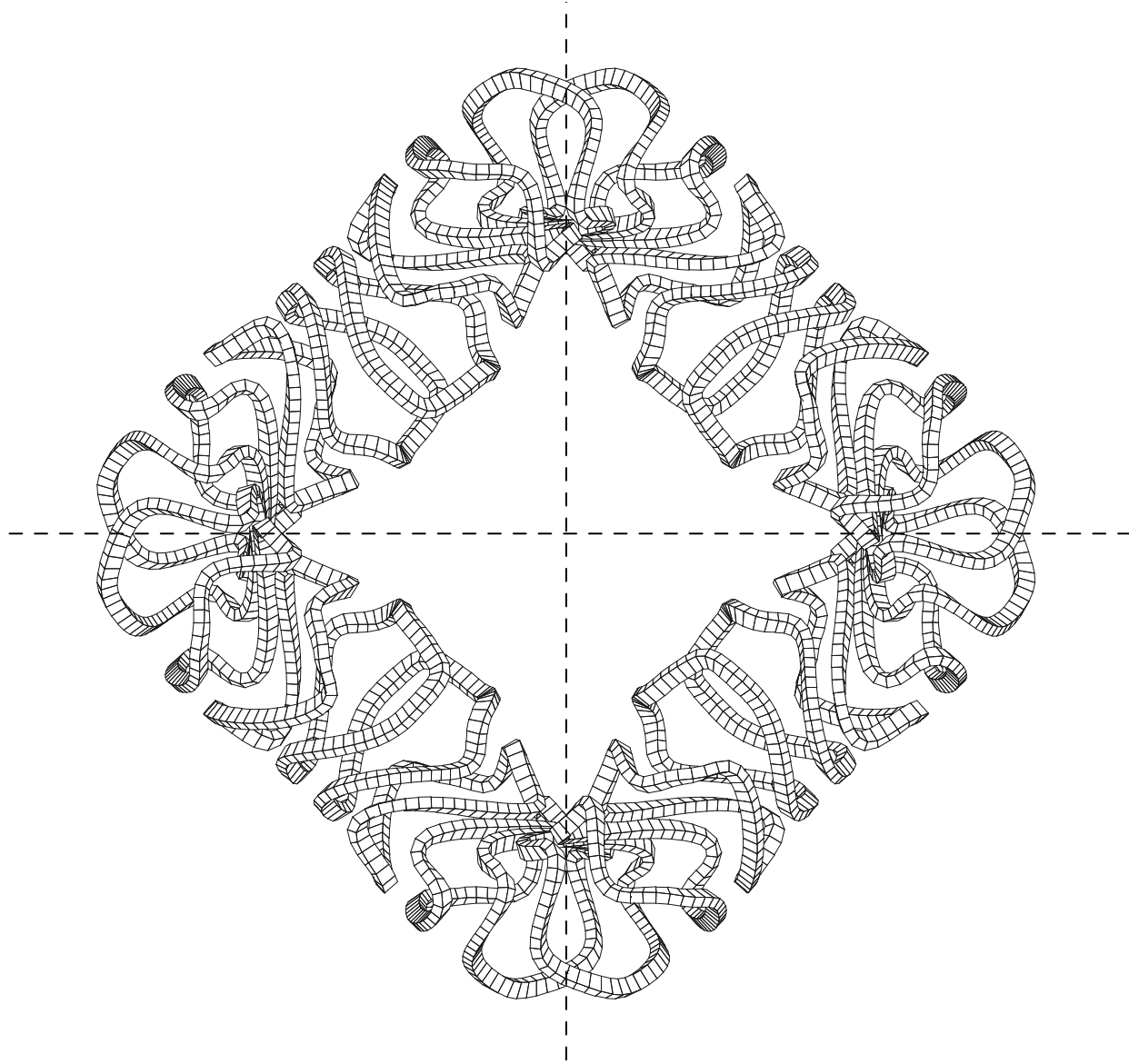


Figure 4.3-4. Plan view of SPPS magnet array.

second model, presented in Fig. 4.3-3 and called the 4-lobed toroid, is derived from the first model by co-locating the solenoid coil centers in Cartesian (X, Y, Z) space with the corresponding centers for the stellarator coils. This refinement was done to simulate the quadrant layout of the stellarator coil array, which is illustrated for comparison purposes in the plan view in Fig. 4.3-4. Clearly, both models are gross, first order approximations to the stellarator magnet array geometry. The intent, however, was not to calculate magnetic field with a high degree of accuracy, but to estimate relative changes in magnetic field due to coil grading.

Graphs of the predicted relative field change versus global radius due to coil grading are presented for Coil 1 (a coil at the corner of the array) and Coil 4 (a mid-side coil between corners) in Figures 4.3-5 and 4.3-6, respectively. These results were generated by the 4-lobed model and have been normalized to a parameter designated as B^* , which is the field for the uniform current density baseline case. Our results suggest that all four grading schemes should have negligible impact ($< 3\%$) on the field magnitude at the plasma axis. This is not surprising, however, since the winding window dimensions are less than one fourth of the mean coil radius.

The circular toroid model was used also to predict the product of overall current density times circumferential field in the global $Z = 0$ plane. This product is the local Lorentz force density and, on a relative basis, is an indicator of which coil grading scheme should incur the least mechanical stress. The results for grading schemes 2 (same as 4) and 3 are plotted versus global radius at the inboard leg in Fig. 4.3-7 and are compared against those for uniform current density. The relative differences in peak force density magnitude suggest that grading scheme 4 would be the most benign from a structural engineering perspective. However, scheme 3 was chosen because it is characterized by the greatest combined conductor temperature margin for both inner and outer coils. This margin was calculated according to the methodology discussed in Sec. 4.3.3. The results for isothermal coil conditions, corresponding to no internal nuclear heating, are presented in Table 4.3-I. A Nb_3Sn wire strain of 0.35% was assumed.

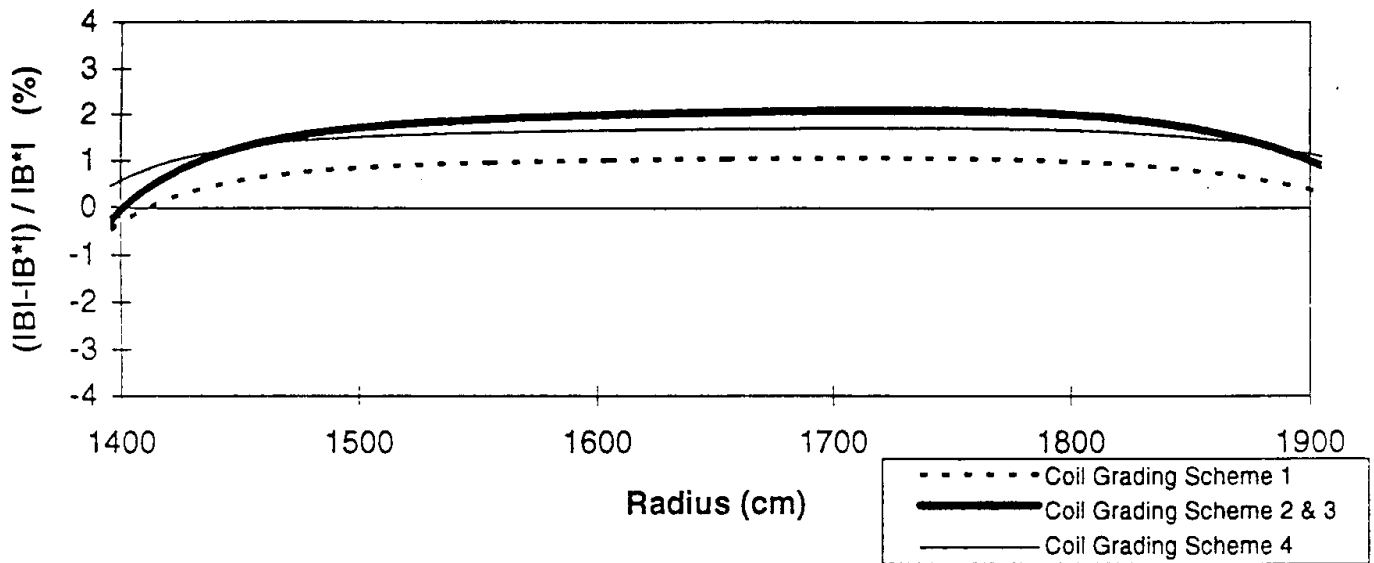


Figure 4.3-5. Relative changes in coil 1 magnetic field distribution at $Z = 0$ and $\Theta = 4.463^\circ$ due to alternative coil grading schemes (B^* is the field with a uniform current density).

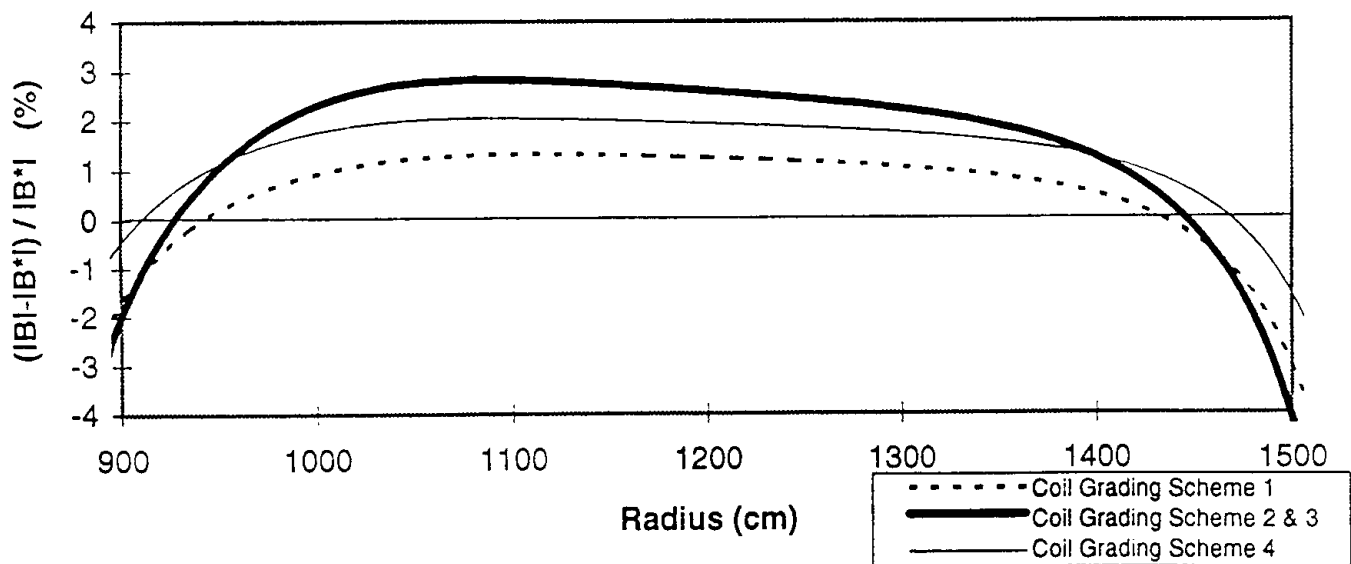


Figure 4.3-6. Relative changes in coil 4 magnetic field distribution at $Z = 0$ and $\Theta = 37.541^\circ$ due to alternative coil grading schemes (B^* is the field with a uniform current density).

Table 4.3-I.
Preliminary Temperature Margin Predictions
for Coil Grading Schemes (No Nuclear Heating)

Temperature Margin (K)	Coil A (insert)	Coil B (outsert)	Coil C (outsert)
Scheme 1	7.6	4.2	2.25
Scheme 2	9.2	2.6	
Scheme 3	7.6	6.4	
Scheme 4	6.0	3.9	

Figure 4.3-7. Product of current density times theta component of magnetic field versus radius as predicted by circular toroid model.

4.3.2. Thermal Hydraulic Performance

The steady-state thermal hydraulic performance of the stellarator coils determines hot spot temperature rise due to nuclear heating. This hot spot temperature value has the effect of translating the superconducting critical surface towards the design operating point along the magnet load line since peak magnetic fields and peak nuclear heating rates are co-located within the same region of the stellarator coil. Consequently, coil design feasibility is strongly dependent upon the extent of temperature rise.

To model the fluid mechanics of supercritical helium, the Navier-Stokes equations including the effects of finite compressibility are used. Helium pressure and temperature profiles along the CICC flow path are obtained from integrating the following nonlinear differential equations, derived from an enthalpy balance [9] on a control volume:

$$\frac{dP}{dx} = \frac{-2fG^2/(\rho D) + 4qG\beta/[\rho D(C_p - u^2\beta)]}{1 - (G^2/\rho)(\kappa + B\phi)}, \quad (4.3-1)$$

$$\frac{dT}{dx} = \frac{-2fG^2\phi/(\rho D) + 4q/[GD(C_p - \mu^2\beta)]}{1 - (G^2/\rho)(\kappa + \beta\phi)}, \quad (4.3-2)$$

where the parameter ϕ is defined as

$$\phi = \frac{\mu_j C_p + u^2 \kappa}{C_p - u^2 \beta}. \quad (4.3-3)$$

Here, P is pressure, T is temperature, x is position along the flow path mass flow rate, ρ is density, D is hydraulic diameter, q is the heat input flux, C_p is constant pressure specific heat, u is the bulk flow velocity, β is bulk expansivity, κ is bulk compressibility, and μ_j is Joule-Thomson coefficient. Helium properties were provided by Cryodata HEPAC software.

For the CICC cable space, relevant friction factors are given by Hooper's correlation [10] to Lue's data [11]. The correlation for the friction factor in the central channel [12] of the insert coil CICC is given by:

$$f = 0.092 Re_D^{0.2}, \quad (4.3-4)$$

where Re_D is the Reynolds number of the central channel. This correlation yields friction factors roughly twice that of smooth tubes. Consequently, almost all of the helium flow in the insert coil CICC occurs in the central channel. For these scoping studies, it was assumed all of the flow can be allocated to the central channel.

The heat input flux, q , along the flow path was determined from the volumetric nuclear heat generation profile plotted in Fig. 4.3-8 from data supplied [13] to Westinghouse. This was done by multiplying the volumetric heat generation rate for a given coil layer by the ratio of CICC cross-section area to wetted perimeter. The effect of turn-to-turn heat conduction between adjacent coil layers was included via an equivalent thermal circuit approach based on the conduit insulation turn geometry and an assumed thermal conductivity comparable to that of G10/11 epoxy-glass composite.

Results for the outsert coil A layer closest to the plasma are presented in Fig. 4.3-9. The predicted hot spot temperature is 5.2 K and is located at the helium outlet. The pressure drop is roughly 4 atmospheres at a flow rate of 3.5 g/s. Hot spot temperature could be reduced by reducing inlet pressure to increase helium specific heat capacity, by increasing mass flow rate, by subcooling to 2.5 K, or a combination of all three. Figure 4.3-10 presents the thermal-hydraulic response of the secondary auxiliary case coolant flow stream. Case coolant exits at nearly 4.7 K, which is less than the aforementioned 5.2 K hot spot temperature, so it clearly is also dissipating some of the heat deposited in the outsert coil as well as intercepting case heat load. Despite the large flow rate of 100 g/s, the pressure drop is roughly 2.3 atm.

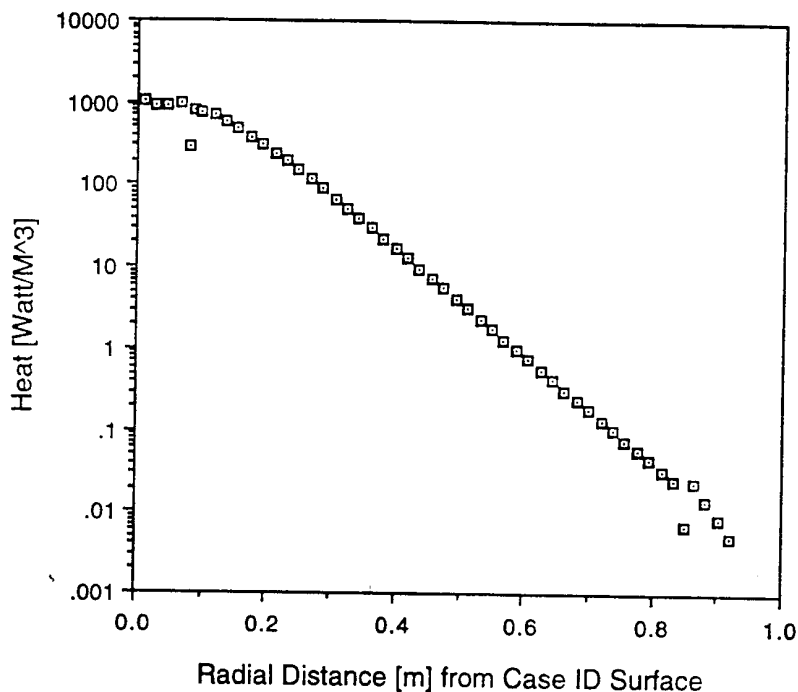


Figure 4.3-8. Distribution of volumetric nuclear heating within magnet.

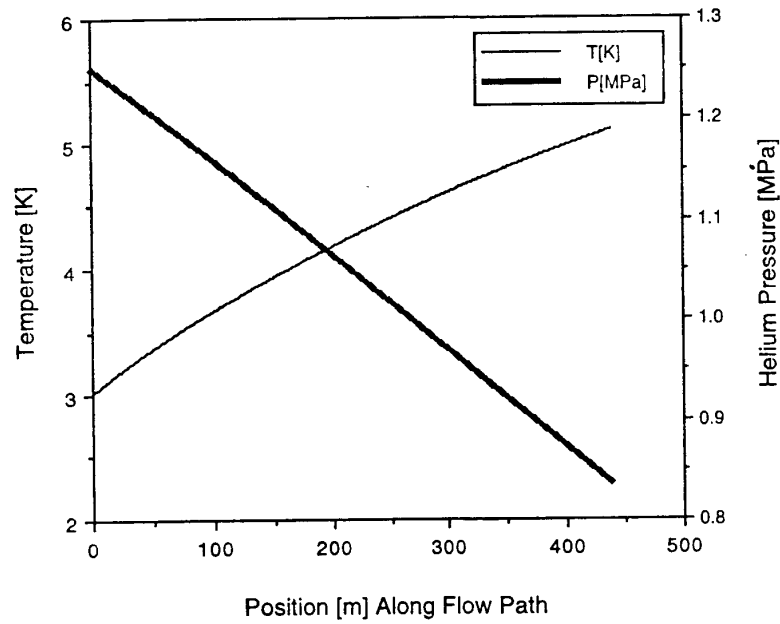


Figure 4.3-9. Predicted worst case thermal-hydraulic performance of outsert coil (heat input is 40 W/m^3 and flow rate is 3.5 g/s).

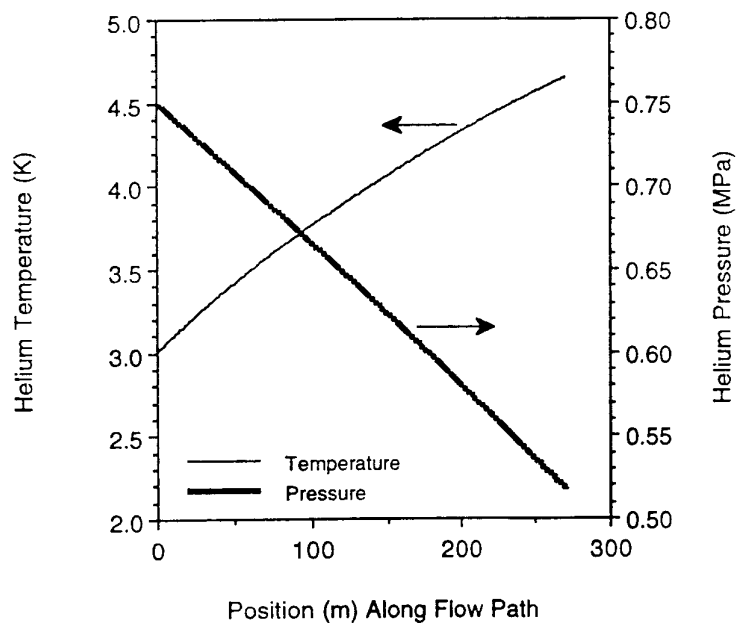


Figure 4.3-10. Predicted helium temperature and pressure profiles in secondary case cooling conduit (heat input is 32 W/m^3 and flow rate is 100 g/s).

Results for the insert coil layer closest to the plasma are presented in Figs. 4.3-11 and 4.3-12. Figure 4.3-11 indicates the predicted hot spot temperature is 5.7 K. The hot spot occurs upstream of the outlet due to expansion cooling of the helium (Joule-Thomson effect). A similar effect is predicted for the third layer (second CICC layer). However, the auxiliary coolant stream (layer 2) exhibits monotonically increasing temperature. Figure 4.3-12 presents the pressure profiles for these three layers. The predicted CICC outlet pressure in the 0.3 MPa to 0.4 MPa range suggests inlet pressures cannot be greatly reduced while safely maintaining single phase flow everywhere along the flow path. The great disparity between CICC flow rates and auxiliary coolant conduit (layer 2) flow rates is due to greater friction factors and smaller hydraulic diameters of the CICC. These results were based on the assumption the primary auxiliary case coolant flow stream intercepts all of the roughly 1 kW of nuclear heat deposited in the insert coil mandrel band.

4.3.3. Conductor Stability

The stability of the stellarator conductor is described by how close the operating point is to the critical surface in terms of distance in (I, B, T) space. Field margin, temperature margin, quench point, minimum propagating zone, and minimum quench energy are useful figures of merit in evaluating design feasibility.

The superconducting critical surface is determined by using temperature and magnetic field dependent critical current correlations. Morgan's correlation [14] and a Westinghouse correlation [15, 16] similar to Green's formulation [17] is used for NbTi, and Summer's formulation [18] is used for Nb₃Sn.

The quench point is the intersection of the load line with the critical surface. The distance along the load line, LL , is given by:

$$LL = \frac{I_{op}}{I_q} = \frac{B_{op}}{B_q}. \quad (4.3-5)$$

The temperature margin, TM , is given by:

$$TM = T_q - T_{cond}. \quad (4.3-6)$$

The field margin, FM , is given by:

$$FM = \frac{I_q - I_{op}}{I_{op}} = \frac{B_q - B_{op}}{B_{op}}, \quad (4.3-7)$$

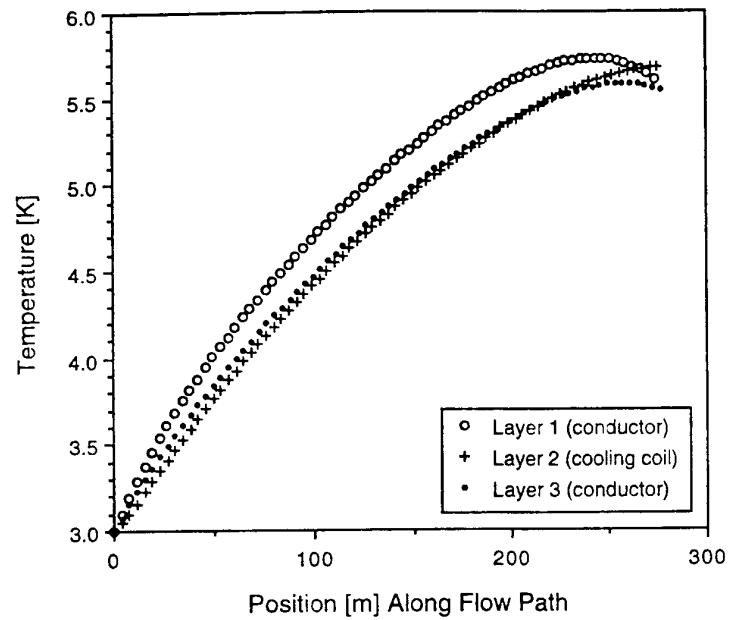


Figure 4.3-11. Predicted worst case conductor temperature profiles in the insert coil (heat input is 800 W/m^3).

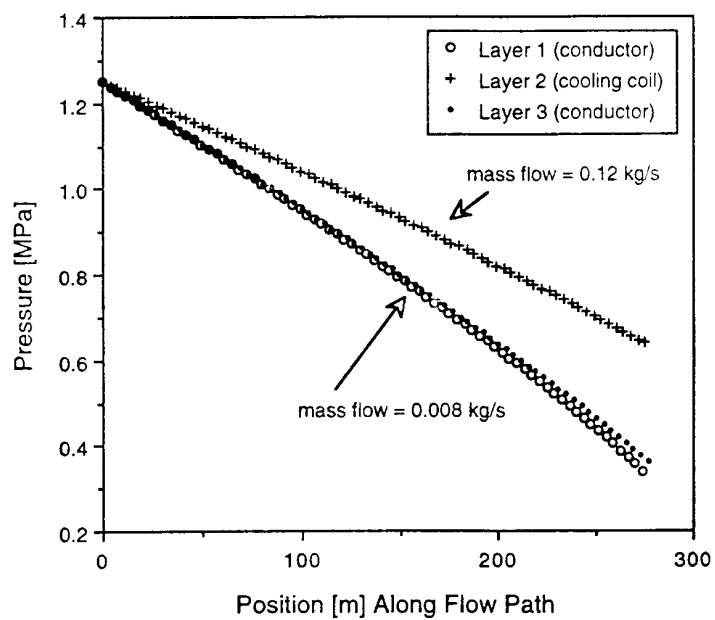


Figure 4.3-12. Predicted helium pressure profiles in the insert coil (heat input is 800 W/m^3).

where I_q is the quench current, I_{op} is the operating current, B_{op} is the magnetic field at the operating point. It is assumed that the load line is linear (*i.e.*, no materials are present which exhibit saturation effects). The temperature margin is found by numerically finding the temperature at which the critical surface intersects a particular point on the load line.

Stability is also described by the minimum propagating zone (MPZ) and the minimum quench energy (MQE) [19]. A normal zone larger than the MPZ will grow in length. A normal zone smaller than the MPZ will recover. In a one dimension adiabatic approximation, the MPZ is estimated by l ,

$$l = \left[\frac{2k(\Theta_c - \Theta_o)}{J_c^2 \rho} \right]^{1/2}, \quad (4.3-8)$$

critical temperature, Θ_o is the bath or operating temperature, J_c is the superconducting critical current density, and ρ is the resistivity of the strand. The MQE is the energy necessary to create a normal zone of length equal to the MPZ, *i.e.*,

$$\text{MQE} = \int_{\Theta_o}^{\Theta_c} l A C(T) dT, \quad (4.3-9)$$

where A is the conductor area and C is the conductor specific heat. While a one dimensional approximation for MQE is used here for simplicity, a more detailed design effort should consider two-dimensional geometry that is more appropriate to the stellarator geometry. The one dimensional approximation is used for simplicity. The one dimensional approximation is invalid when the MPZ is smaller than the strand diameter. In this case the strand diameter is used for the MPZ instead of the calculated value.

The effects of cooling and enthalpy of helium are included in the MQE. The dual stability region separating the well cooled regime from the ill-cooled regime is found from

$$i_{lim} = \left[\frac{A_{cu} p h (T_c - T_b)}{\eta I_c^2} \right]^{1/2}, \quad (4.3-10)$$

where A_{cu} is the copper area, p is the wetted perimeter, h is the heat transfer coefficient, T_c is the critical temperature, and η is the copper resistivity [20]. The physical constants used in the MPZ and MQE are Kohler plot for the resistivity of copper [21], Lorentz ratio is used to determine thermal conductivity, and the computer code Cryodata [22] is used for the specific heat of the elements comprising the conductor and helium.

The stability is actually described by comparing the MQE to the disturbance spectrum of the magnet. Prediction of the disturbance spectrum is beyond the scope of this work.

Typical energy deposition due to ac losses from plasma disruptions in Tokamak machines will range from 100 to 150 mJ/cm³ of strand materials [23]. A MQE of about 100 mJ/cm³ is assumed to be a minimum value for stability in these large conductors.

4.3.3.1. Insert Coil Operating Point and Margin

The operating point for the insert coil without nuclear heating is 84% along the load line, shown in Fig. 4.3-13. The temperature margin is 3.4 K at the operating point. Including nuclear heating results in a temperature rise of 2.7 K with the conductor operating at 5.7 K. The margin is reduced to 0.7 K, and the conductor operates closer to the critical surface, 95% along the load line, see Fig. 4.3-14. (In both figures, the unit for the vertical axis is ampere-turns.)

The MQE is calculated using MPZ theory. Inclusion of the cooling and enthalpy of the helium in the conduit results in a MQE of about 2,000 mJ/cm³, as shown in Fig. 4.3-15. The well-cooled regime extends past the operating point. Experimentally, the LCP conductor had a MQE of about 1.5 to 1.8 J/cm³ [24]. This MQE exceeds the 100 mJ/cm³ design goal.

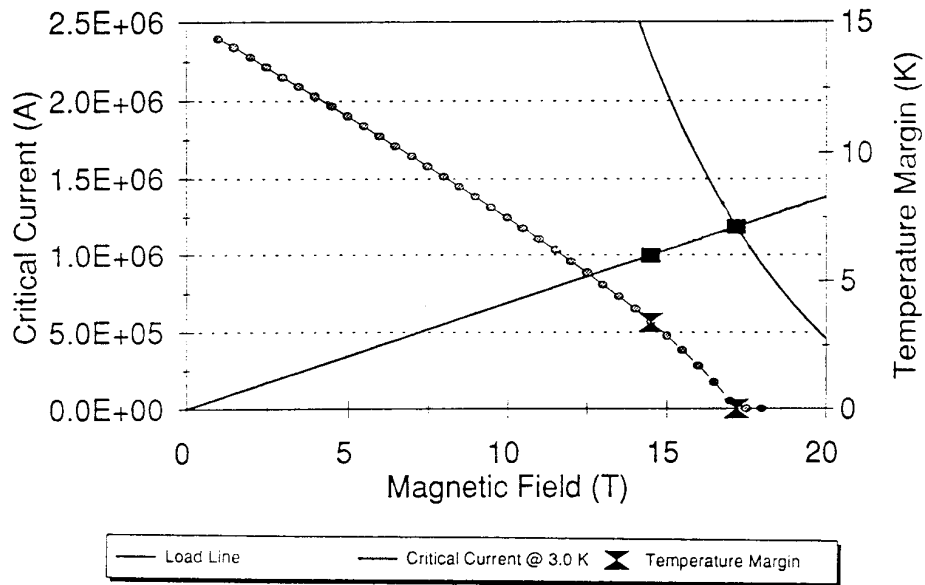


Figure 4.3-13. The critical surface, load line, operating point, quench point, and temperature margin for the proposed insert coil conductor without the nuclear heating temperature rise. (For $LL = 84\%$, $I_{op}/I_c = 43\%$, $TM = 3.4$ K, and $k_f = 0.09$.)

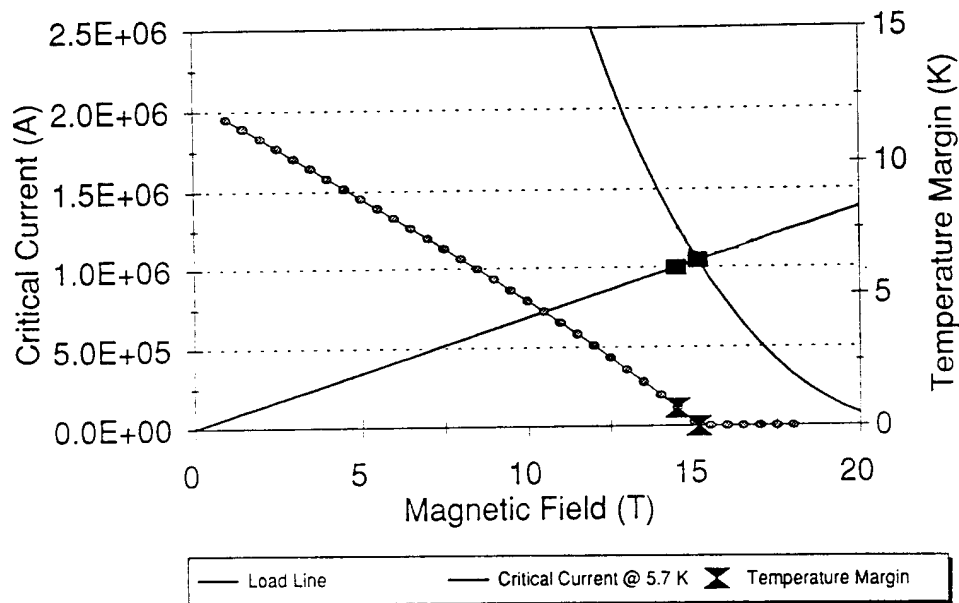


Figure 4.3-14. The critical surface, load line, operating point, quench point, and temperature margin for the proposed insert coil conductor with the nuclear heating temperature rise included. (For $LL = 96\%$, $I_{op}/I_c = 78\%$, $TM = 0.7$ K, and $k_f = 0.09$.)

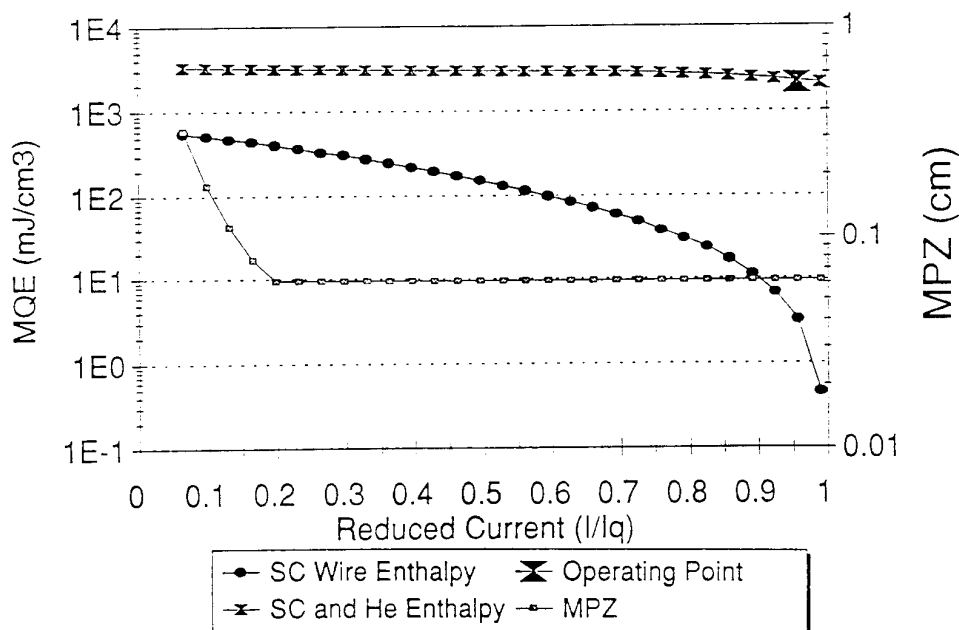


Figure 4.3-15. The minimum quench energy of the insert coil derived from the minimum propagating zone.

4.3.3.2. Outsert Coil Operating Point and Margin

The operating point for the outer coil without nuclear heating is 80% along the load line, shown in Fig 4.3-16. The temperature margin is 4.3 K at the operating point. Including nuclear heating results in a temperature rise of 2.2 K with the conductor operating at 5.2 K. The margin is reduced to 2.1 K, and the conductor operates closer to the critical surface, 88% along the load line as is shown in Fig. 4.3-17.

The MQE is calculated using MPZ theory. Inclusion of the cooling and enthalpy of the helium in the conduit results in a MQE of about 3,000 mJ/cm³, as shown in Fig. 4.3-18. This MQE exceeds the 100 mJ/cm³ design goal. The well-cooled regime extends well past the operating point.

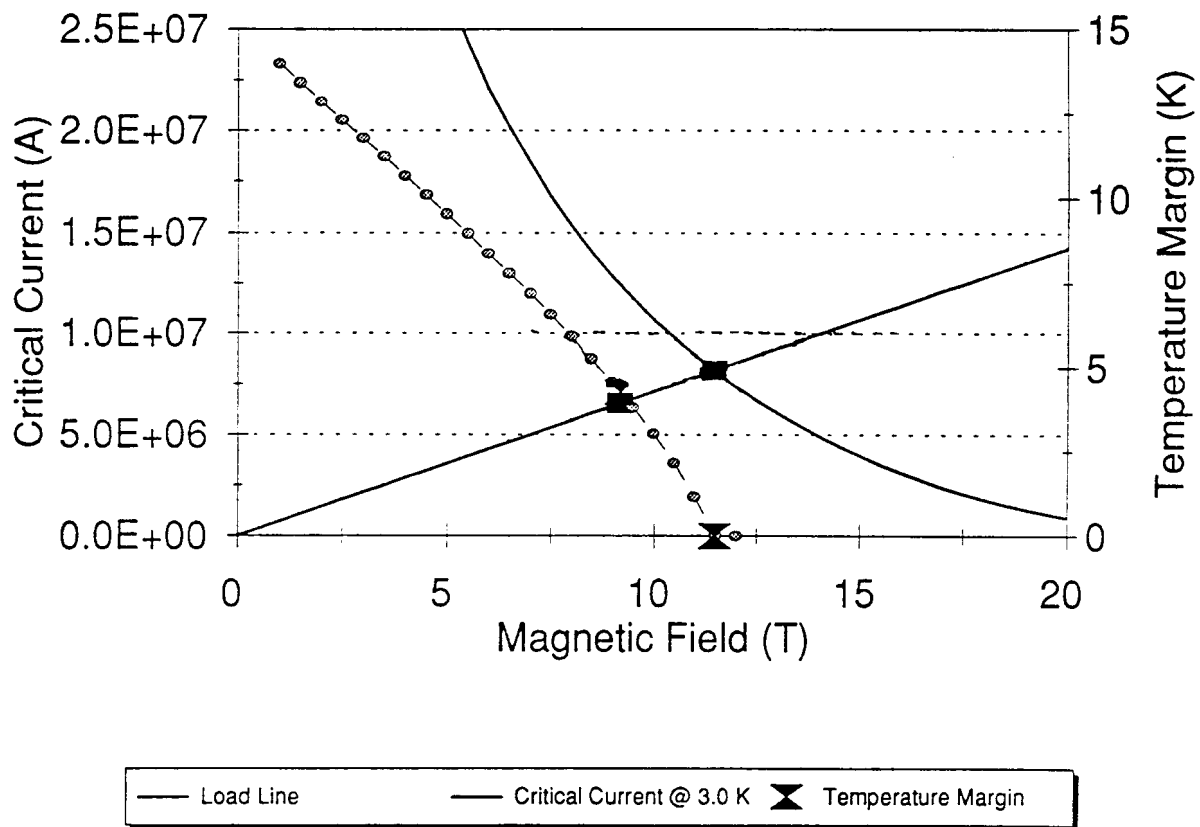


Figure 4.3-16. The critical surface, load line, operating point, quench point, and temperature margin for the proposed outsert conductor without the nuclear heating temperature rise. (For $LL = 80\%$, $I_{op}/I_c = 53\%$, $TM = 4.3$ K, and $k_f = 0.12$.)

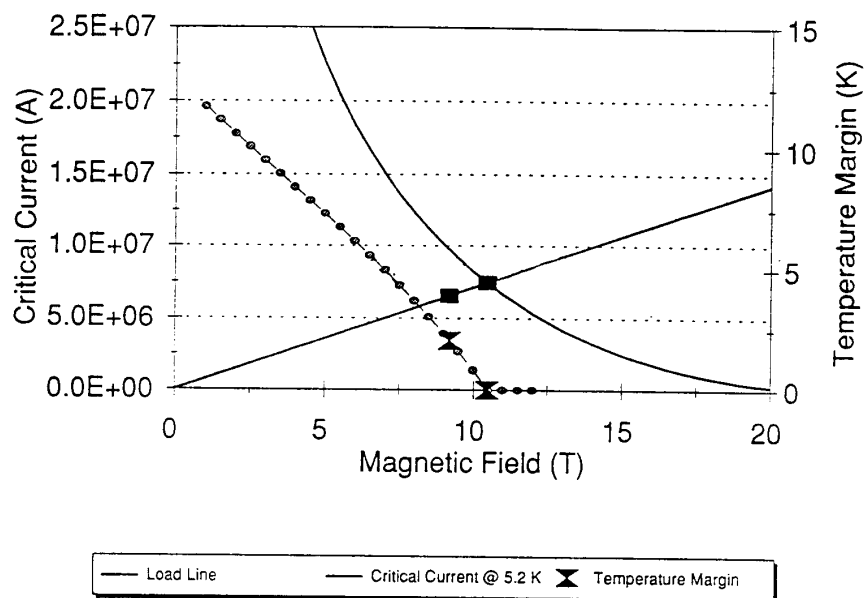


Figure 4.3-17. The critical surface, load line, operating point, quench point, and temperature margin for the proposed outsert coil conductor with the nuclear heating temperature rise. (For $LL = 88\%$, $I_{op}/I_c = 67\%$, $TM = 2.1$ K, and $k_f = 0.12$.)

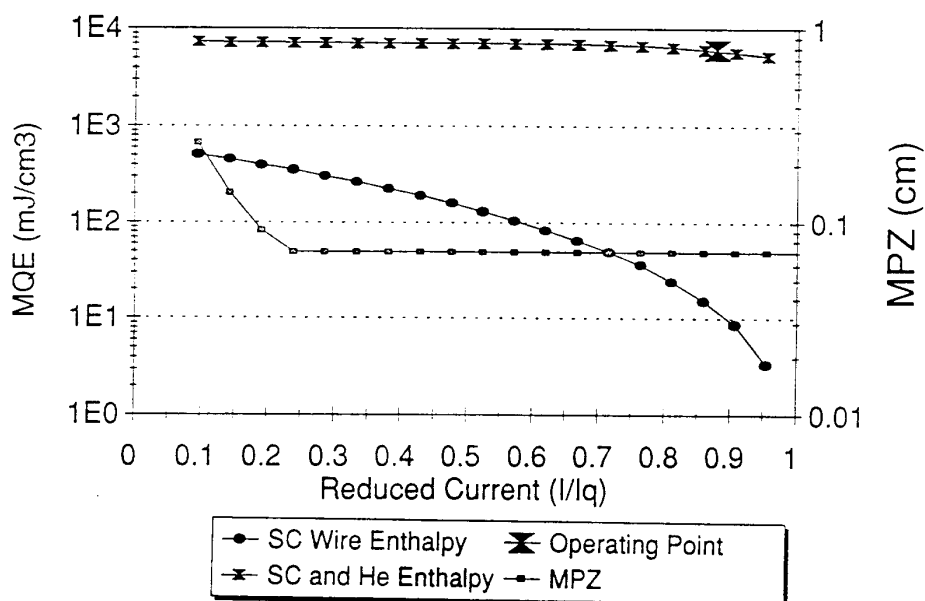


Figure 4.3-18. The minimum quench energy of the outsert coil derived from the minimum propagating zone.

4.3.3.3. Discussion of Radiation Effects and Wire Development

The Nb₃Sn superconductor critical current is sensitive to strain in the conductor. Strains over 0.35% result in unrecoverable degradation in the critical current. In addition, the Nb₃Sn filaments will break if the stress in the conductor exceeds about 250 to 300 MPa. Using typical stress-strain values for the materials used in the conductor and coil pack, the stress for a particular superconductor fill factor can be found at a given strain. Results are shown in Fig. 4.3-19.

Preliminary estimates of the stress in the conductor indicate values around 250 MPa. The coil design is in the correct stress-strain and fill factor region for a feasible design.

The critical currents used in the margin and stability sections are typical values in use today, resulting in a magnet design that will work with today's superconductors. Improvements in the critical current are possible, see for example an increase by a factor of two described by Pourrahimi [23] or Foner [24] for laboratory powder metallurgy processing. It is also possible a wire development program today with the wire manufacturers might allow optimization of the superconductor at the magnetic fields required for a stellarator.

The radiation environment of the stellarator will be intense with a neutron fluence of 10^{23} n/m². Radiation creates defects in the superconducting lattice. For weakly pinned superconductors typical of bulk superconducting filaments, the radiation defects can actually add pinning sites and increase the critical current initially (*e.g.*, see Ref. [25]). Summers estimated the effect of radiation upon the critical current. Calculations for Nb₃Sn superconductor are shown in Fig 4.3-20. Initially the critical current increases, but then the critical current decreases. This figure suggests that the radiation environment will not be a problem in the stellarator. However, the change in critical current will be dependent upon the initial pinning state of the superconductor.

4.3.4. Quench Performance

The quench performance of the cable-in-conduit conductors used in the inner and outer coils is modeled by two different techniques. One technique calculates the hot spot temperature in an adiabatic, one dimensional approximation. This simple calculation is typically called a MIITS calculation. The second technique solves the one dimensional conservation relations for the helium mass and momentum together with energy conservation relations for the helium, the conductor, and conduit wall.

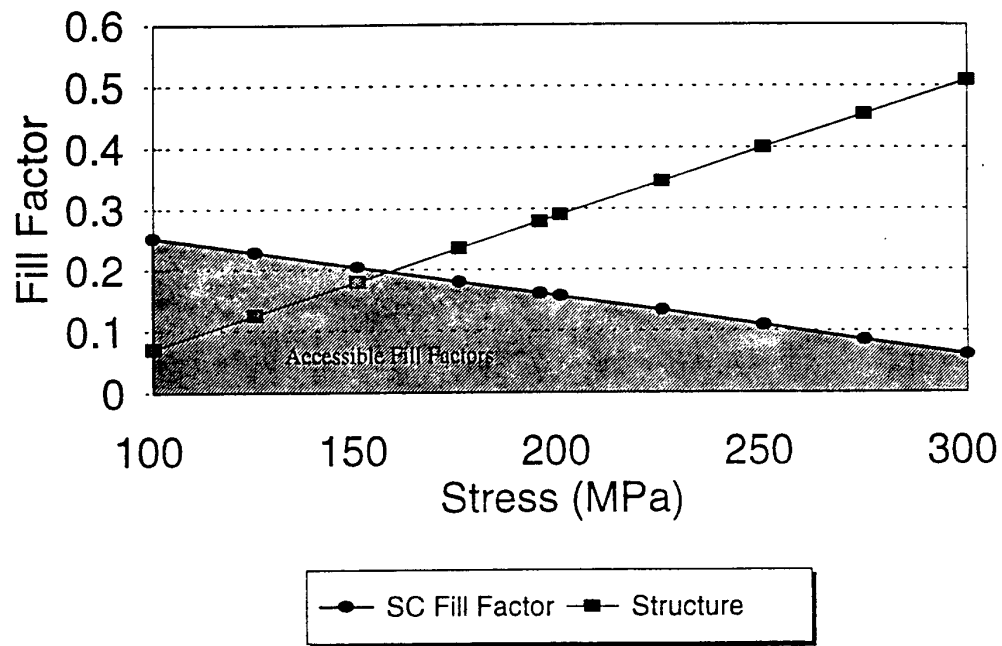


Figure 4.3-19. Acceptable fill factors for different amounts of stress at a strain of 0.35%.

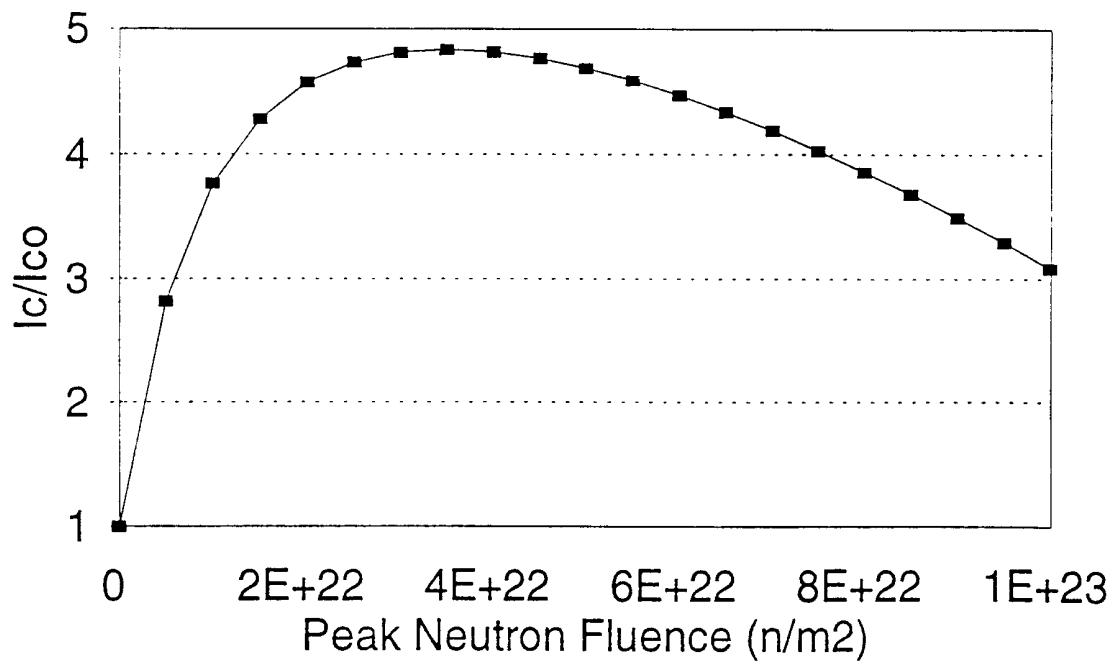


Figure 4.3-20. Change in critical current in Nb₃Sn as a function of neutron fluence.

4.3.4.1. MIITS Calculation

The quench temperature rise is predicted using an adiabatic, one dimensional approximation of a conductor carrying a current. A simple heat balance is

$$\frac{I^2(t) \rho(T)}{A_{cu}} dt = A C(T) dT. \quad (4.3-11)$$

Rearranging and integrating gives [26]

$$\int_0^t I^2 dt = A_{cu} A \int_{T_0}^T \frac{C(T)}{\rho(T)} dT. \quad (4.3-12)$$

The units are MIITS ($10^6 \text{ A}^2\text{-s}$). The left hand integral is a function of temperature. To simplify the problem to save time, it is assumed that the current decay is entirely controlled by the dump resistor. The current decays with a time constant of L/R where L is the inductance of a single magnet and R is the resistance of a single dump resistor which is slaved to a single magnet. The inductance is derived from the total stored energy and scaled by $1/32$ to estimate the self inductance of a single typical magnet. Mutual inductance between adjacent coils is ignored. The constant L/R current decay approximation ignores propagating normal zones.

Evaluation of the right hand integral requires values for the physical parameters. The specific heat copper, iron, and the elements comprising Nb_3Sn are obtained from the computer program Cryodata [22] (Values beyond 300 K are extrapolated). The resistance of copper as a function of field is derived from a Kohler plot with Fickett's empirically derived coefficients [21], and the temperature dependence follows the equation [26]

$$\rho(T, RRR) = 1.545 \times 10^{-8} RRR^{-1} + \left[2.32547 \times 10^{17} T^{-5} + 9.57137 \times 10^{13} T^{-3} + 1.62735 \times 10^{10} T \right]^{-1}. \quad (4.3-13)$$

These quantities are plotted in Fig. 4.3-21.

Insert Coil. The estimated stored energy of the insert coil is 13 MJ which, at a current of 4.63 KA, gives an inductance of 1.213 H. Setting the quench voltage as 500 V yields a dump resistor of 0.108Ω and a decay constant of roughly 11 s. It is assumed that the current decays exponentially, controlled entirely by the dump resistor. Evaluating the MIITS integrals gives a value of 117 MIITS, and a hot spot temperature of 73 K, as shown in Fig. 4.3-22.

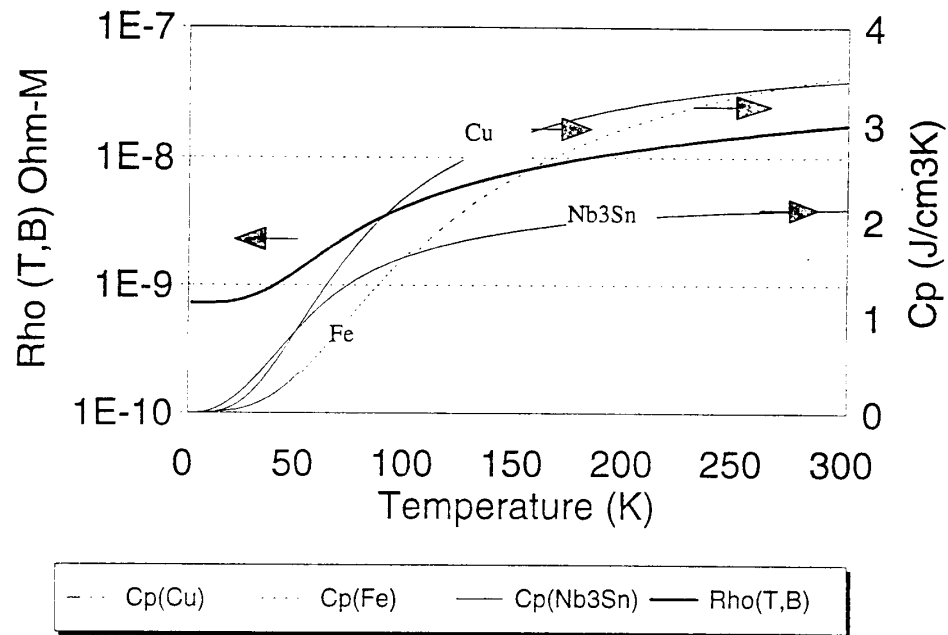


Figure 4.3-21. The physical properties used in the MIITS calculation.

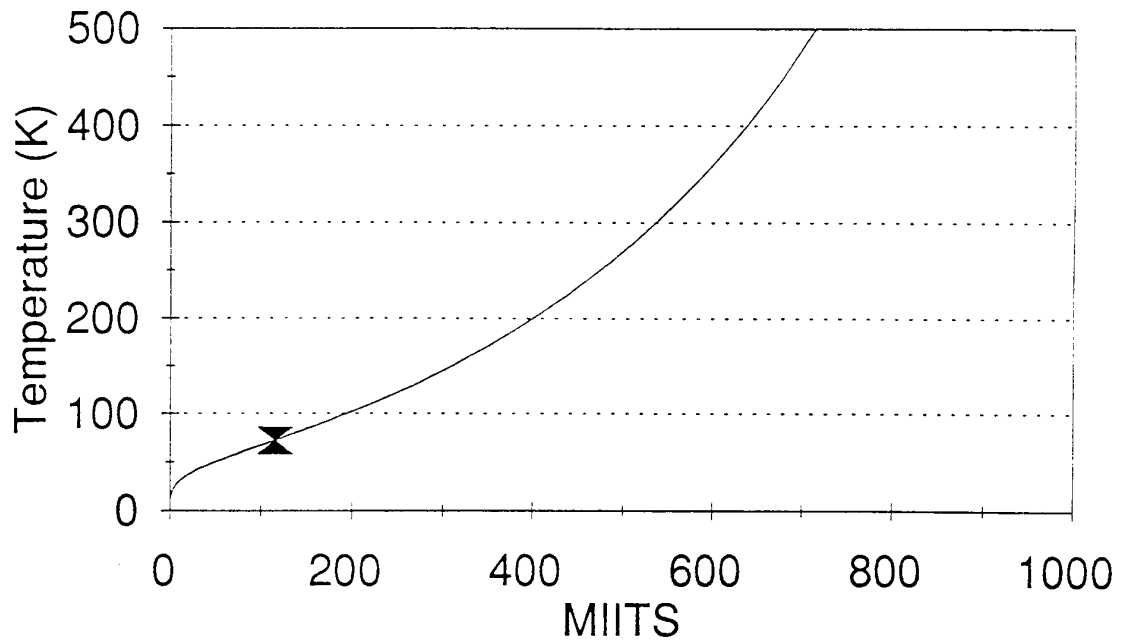


Figure 4.3-22. The maximum temperature of the hot spot following a quench for the proposed insert coil conductor and dump resistor.

Since it is assumed the current dump is controlled by the dump resistor, it is possible to obtain the hot spot temperature as a function of time. The current in the magnet decreases exponentially. The MIITS integral can be evaluated at different times. Assuming the energy is deposited into the conductor, the conductor hot spot temperature versus time is found. Next it is assumed that energy is transferred from the conductor to the conduit wall through a heat transfer coefficient. Thus, the conduit wall temperature as a function of time is also found. Results are shown in Fig. 4.3-23.

Varying the quench voltage and dump resistance results in different decay times and different maximum temperature rises. The conductor quench voltage is varied to display the variation in quench temperature, as is shown in Fig. 4.3-24.

Outsert Coil. The outsert coil is subdivided into two equal coils to keep the quench temperature rise and the quench voltage at reasonable values less than 200 K and 20 kV, respectively. The estimated stored energy of the outsert coil is 2.4 GJ which, at a current of 45 kA, gives an inductance of 2.37 H or roughly 1.2 H after subdivision. Setting the quench voltage as 15 kV yields a dump resistor of 0.33Ω and a decay constant of 3.6 s. Again, it is assumed that the current decays exponentially, controlled entirely by the dump resistor. Evaluating the MIITS integrals gives a value of 3681 MIITS, and a hot spot temperature of 140 K, as shown in Fig. 4.3-25.

Since it is assumed that the current dump is controlled by the dump resistor, it is possible to obtain the hot spot temperature as a function of time. The current in the magnet decreases exponentially. The MIITS integral can be evaluated at different times. Assuming the energy is deposited into the conductor, the conductor hot spot temperature versus time is found. Next it is assumed that energy is transferred from the conductor to the conduit wall through a heat transfer coefficient. Thus the conduit wall temperature as a function of time is also found. Results are shown in Fig. 4.3-26.

Varying the quench voltage and dump resistance results in different decay times and different maximum temperature rises. The conductor quench voltage is varied to display the variation in quench temperature, as shown in Fig. 4.3-27.

4.3.4.2. Result Verification with QUENCHER

The QUENCHER code was developed by A. Shajii at MIT [27,28]. The code solves the one dimensional conservation relations for the helium mass and momentum together

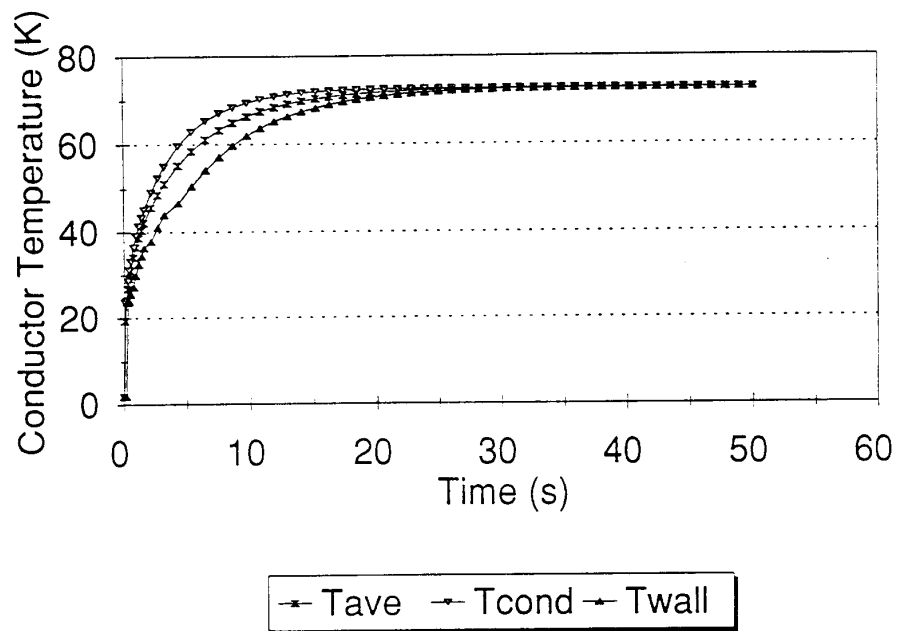


Figure 4.3-23. Projected transient response of the insert coil during a quench.

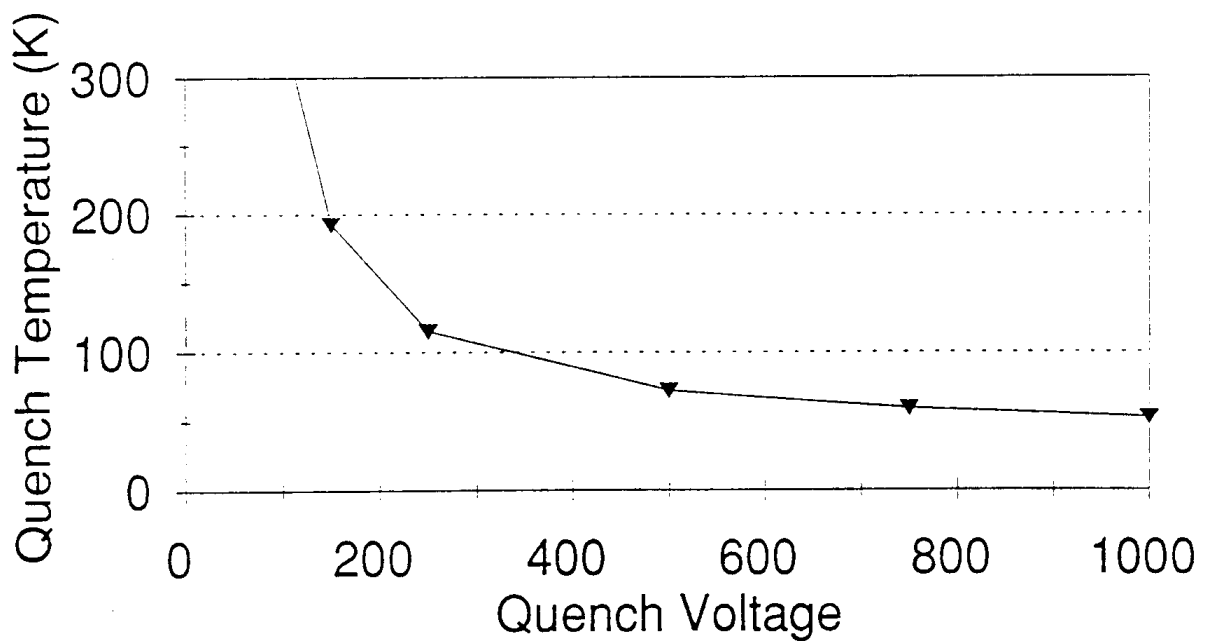


Figure 4.3-24. The insert coil quench hot spot temperature as a function of quench voltage.

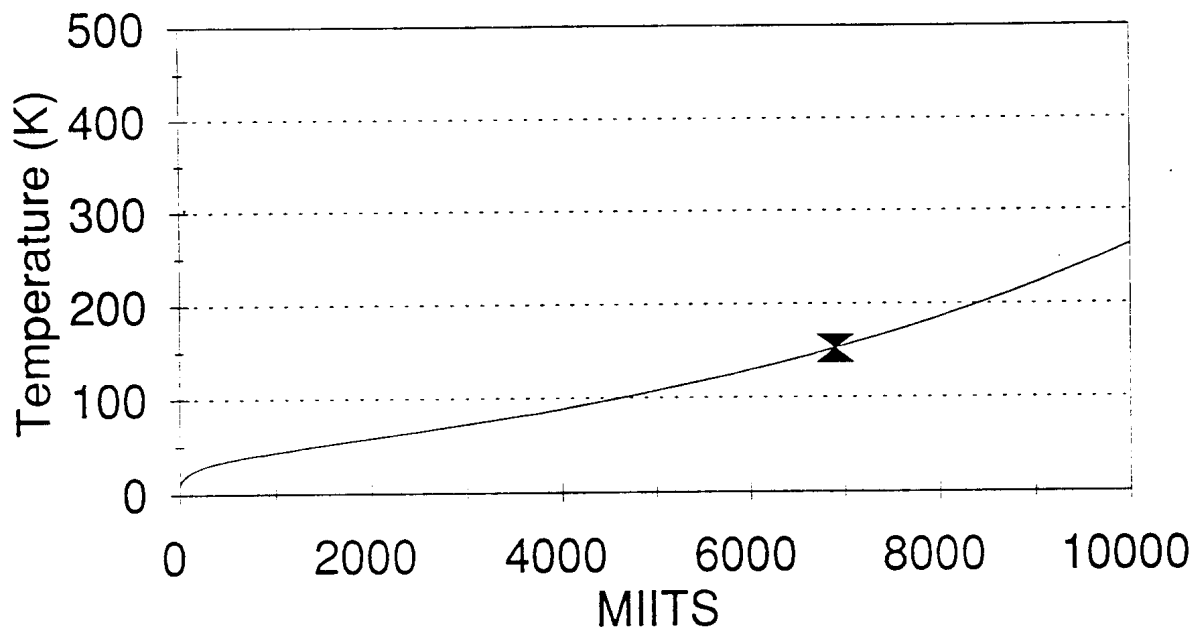


Figure 4.3-25. The maximum temperature of outsert coil hot spot following a quench for the proposed outer coil conductor and dump resistor.

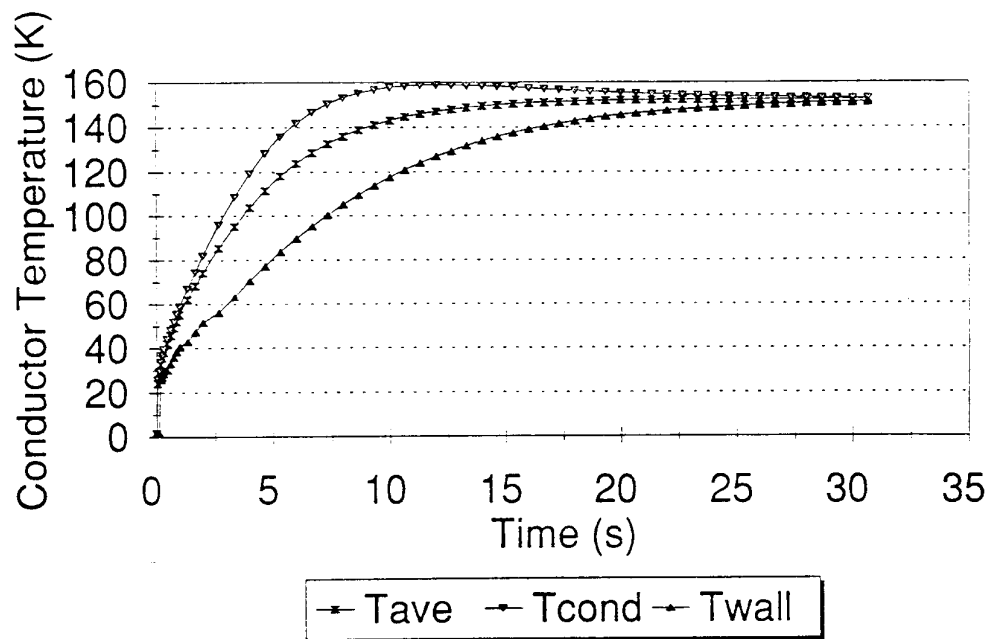


Figure 4.3-26. Projected transient response of the outsert coil during a quench.

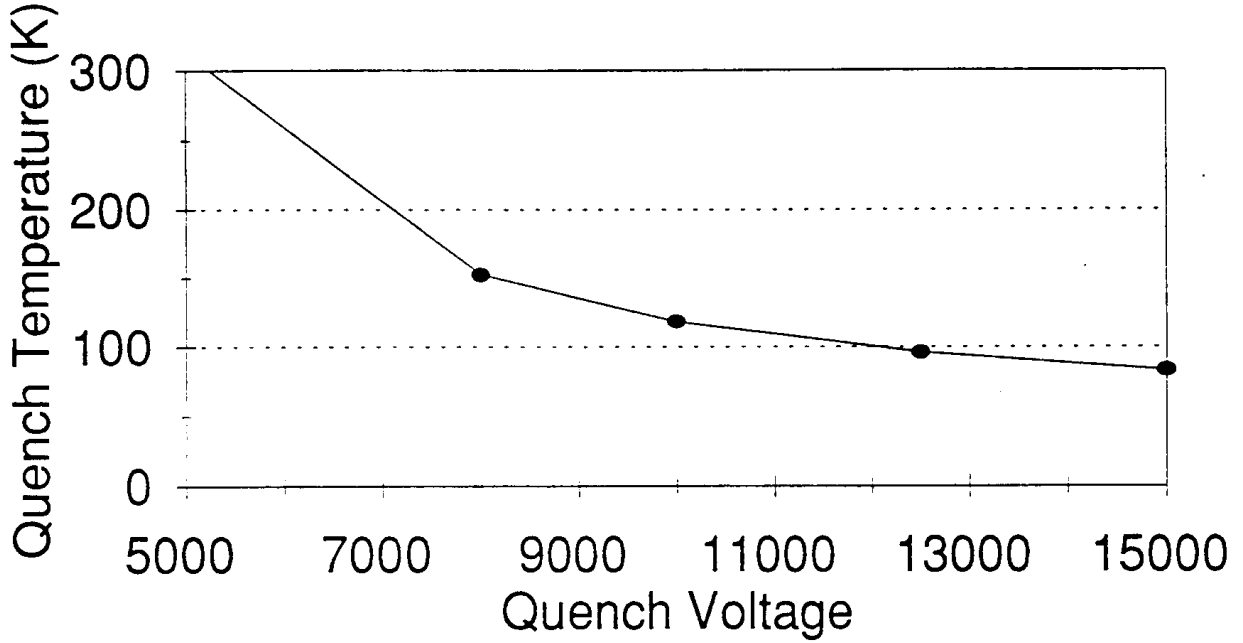


Figure 4.3-27. The outsert coil quench hot spot temperature as a function of quench voltage.

with energy conservation relations for the helium, the conductor, and conduit wall. Helium inertia is neglected, and it is assumed that the supercritical helium coolant and conductor strands are at the same temperature.

The basic equations describing the quench process are:

$$\frac{\partial \rho}{\partial t} + \frac{\partial}{\partial x}(\rho v) = 0, \quad (4.3-14)$$

$$\frac{\partial p}{\partial x} = \frac{f \rho v |v|}{2d}, \quad (4.3-15)$$

$$\begin{aligned} \rho C_t \frac{\partial T}{\partial t} + \rho C_h \gamma \frac{\partial T}{\partial x} + \rho C_\beta T \frac{\partial v}{\partial x} &= \frac{\partial}{\partial x} \left(\kappa_c \frac{\partial T}{\partial x} \right) + \frac{A_h}{A_c} \frac{f \rho |v| v^2}{2d} \\ &\quad + \eta_c J^2 H(T - T_{cs}) + \frac{h P_w}{A_c} (T_w - T), \end{aligned} \quad (4.3-16)$$

$$\rho_w C_w \frac{\partial T_w}{\partial t} = \frac{h P_w}{A_w} (T - T_w). \quad (4.3-17)$$

The quantities A_h , A_c , A_w represent the cross sectional areas of the helium, conductor, and conduit wall respectively, and C_c and C_w are the heat capacities of the conductor

and wall. Also,

$$C_h = \left(\frac{A_h}{A_c} \right) \left(\frac{T}{\partial T} \frac{\partial S}{\partial T} \right)_\rho, \quad (4.3-18)$$

$$C_\beta = - \left(\frac{A_h}{A_c} \right) \left(\frac{\rho}{\partial \rho} \frac{\partial S}{\partial \rho} \right)_T, \quad (4.3-19)$$

$$C_t = C_h + \left(\frac{\rho_c}{\rho} \right) C_c. \quad (4.3-20)$$

Results for outsert coil are shown in Figs. 4.3-28 to 4.3-31 and for insert coil are shown in Figs. 4.3-32 and 4.3-33. In general, the inner coil has a quench temperature of 70 K, quench pressure of 1.9 MPa, a normal zone length of 45 m at 30 s, and an inlet mass flow of 5 g/s with an outlet mass flow of 12 g/s at 30 s. It is interesting that the normal zone does not propagate upstream, and actually begins to translate downstream at large times. The outer coil has a quench temperature of about 170 K (slightly higher than the MIITS calculations), a quench pressure of 4 MPa, a normal zone length of 35 m at 15 s, and a mass flow efflux of 15 g/s out of the cable. The quench propagation is symmetrical around the quench initiation site in the outer coil. Also, in the outer coil the temperature difference between the conductor and conduit wall is about 100 K at about 5 s into the quench. The conductor temperature rises above 200 K at its maximum.

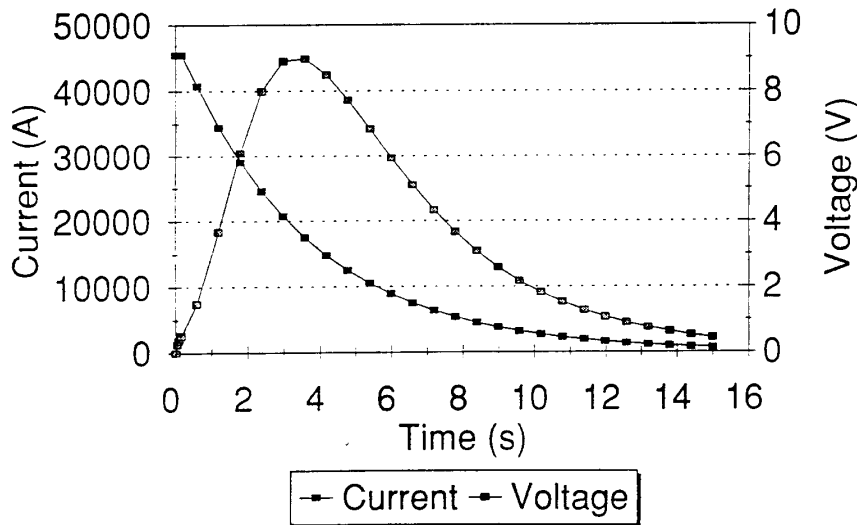


Figure 4.3-28. Predicted current and normal zone voltage transients when the coil and dump resistor are connected (outsert coil).

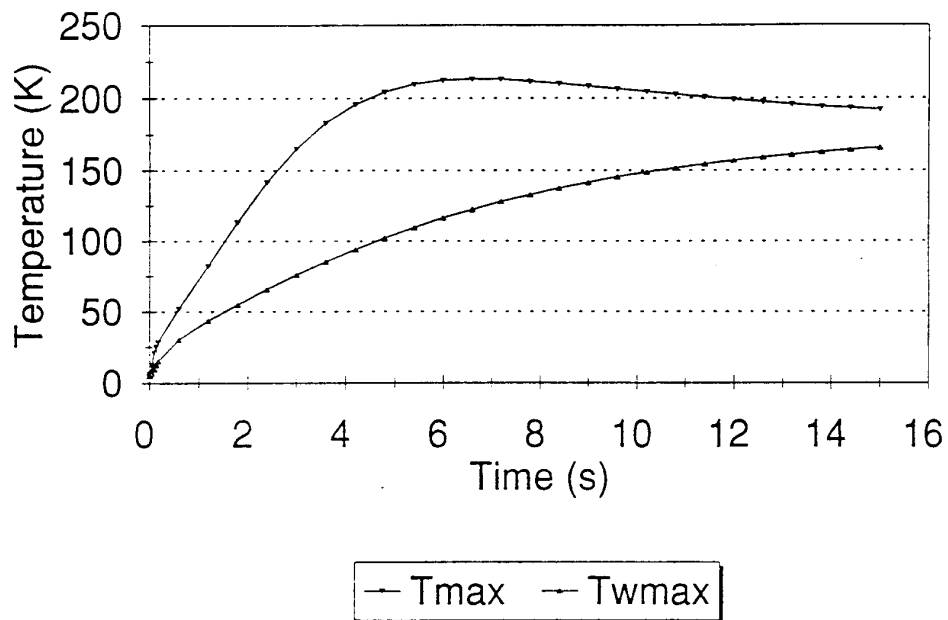


Figure 4.3-29. Predicted maximum wire and conduit wall temperature rise transient during a simulated quench (outsert coil).

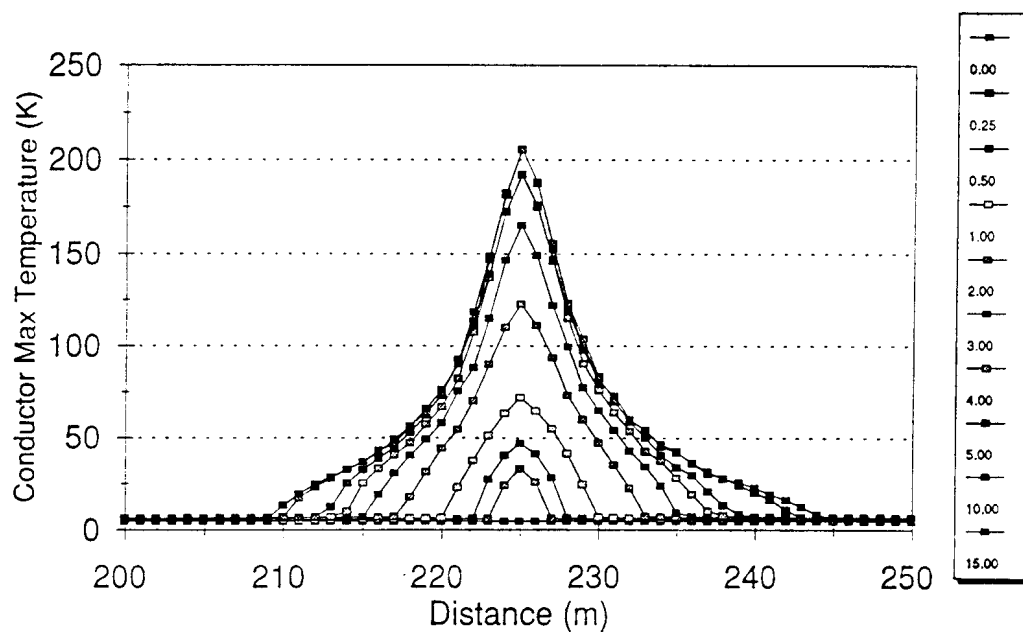


Figure 4.3-30. Time evolution of symmetric wire temperature profiles along coolant flow path during the first 15 seconds of a simulated quench (outsert coil).

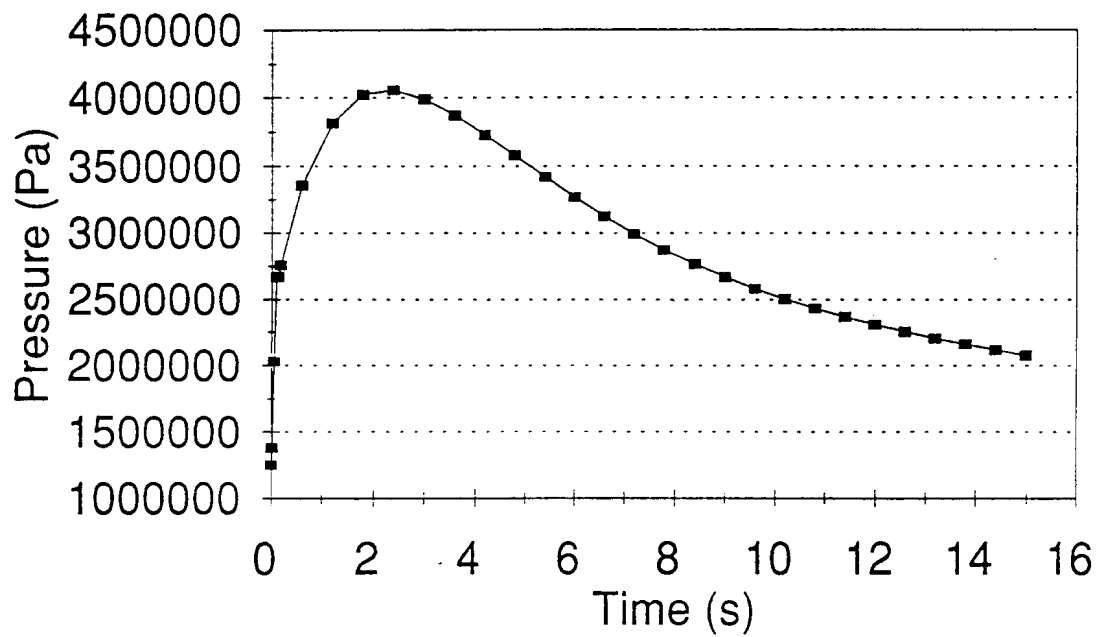


Figure 4.3-31. Predicted pressure rise transient during a simulated quench (outsert coil).

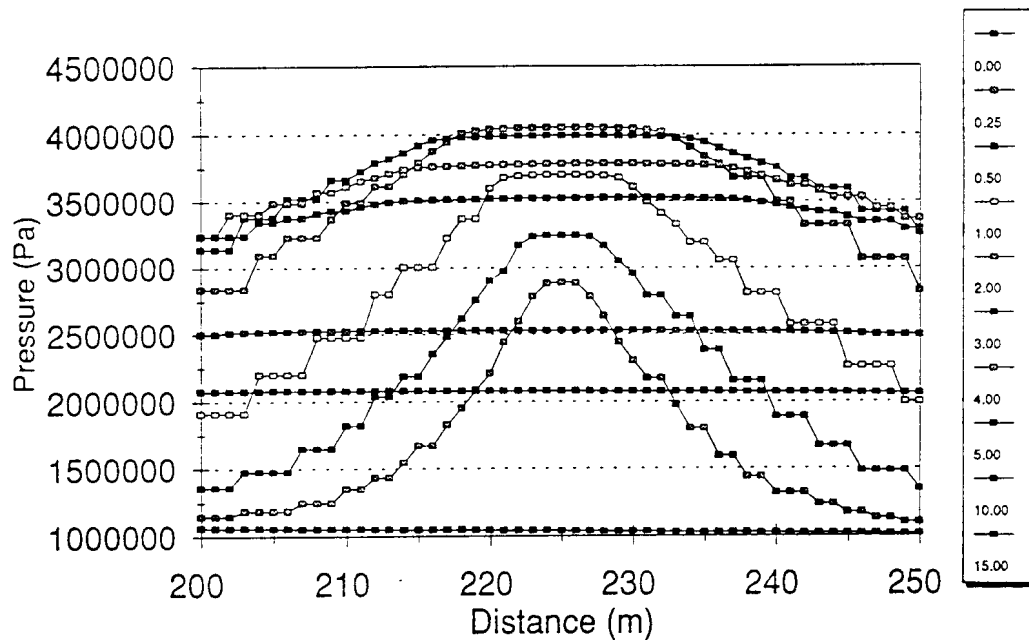


Figure 4.3-32. Time evolution of symmetric pressure profiles along coolant flow path during the first 15 seconds of a simulated quench (outsert coil).

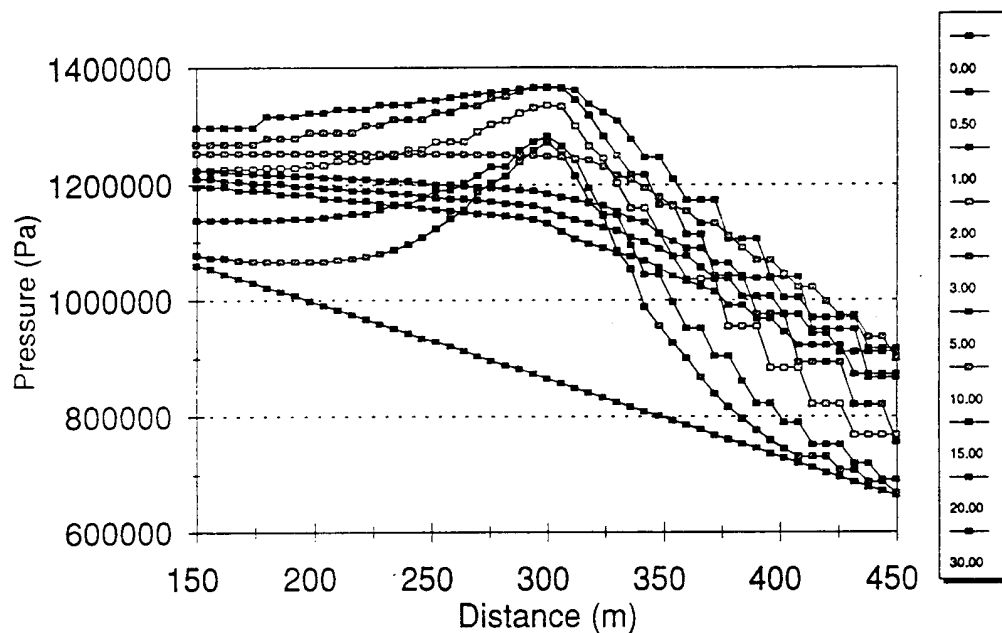


Figure 4.3-33. Time evolution of asymmetric pressure profiles along coolant flow path during the first 15 seconds of a simulated quench (insert coil).

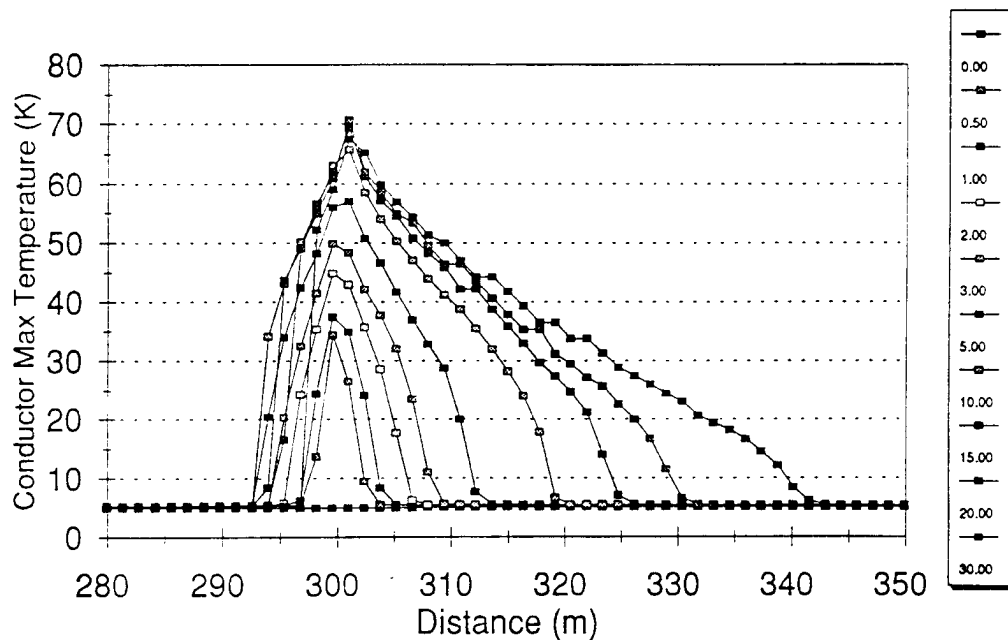


Figure 4.3-34. Time evolution of asymmetric wire temperature profiles along coolant flow path during the first 15 seconds of a simulated quench (insert coil).

4.4. SUMMARY AND CONCLUSIONS

A superconducting, forced-flow cooled, magnet internal design concept has been developed to meet the requirements of a stellarator power plant. Results of electromagnetic, thermal-hydraulic, quench and stability analyses suggest a layer-wound, graded CICC winding pack configuration with two nested coils offers the promise of design feasibility. A conceptual point design was developed as a preliminary baseline configuration. The characteristics of this conceptual point design are summarized in Table 4.4-I.

Several magnet concept development issues beyond the scope of this present study should be addressed. From a coil design perspective, these open issues are:

1. The minimum bend radius to allowable conduit dimension ratio was assumed to be 20:1 based on best engineering judgment. This ratio needs validation for stellarator-relevant conductor designs and may require an upward adjustment due to winding pack twist.
2. A conductor strain of 0.35% was assumed in computing operating margins. Actual strains may be significantly greater due to combined effects of Lorentz forces, differential thermal contraction upon cool-down, and stress risers related to local topology. Consequently, present operating margin predictions may be optimistic.
3. Conduit wall thickness may be undersized, given the extent of conductor bending combined with quench pressure rise effects.
4. The kinematics of non-planar winding need to be analyzed for this topology and conductor architecture, and factored into the design of the external case structure.
5. Predicted temperature margins may be under-predicted significantly due to the fact the cooling design is not optimized.
6. Variability in the local angle of neutron incidence with respect to the coil may effect peak-to-average nuclear heating factors such that outsert coil hot spot temperature may increase significantly beyond present values.
7. Electrical transient response of the inductively-coupled, nested coils and case structure during quench needs to be analyzed from the perspective of how electromagnetic imbalance forces between coils affect design of the magnet structure.

Table 4.4-I.
SPPS/MHH Magnet Internal Design Data Summary

	Insert	Outsert A	Outsert B
Winding window equivalent inside radius (mm)	3,610	3,869	4,145.2
Winding window equivalent outside radius (mm)	3,819	4,113.8	4,390
Winding window lateral width (mm)	505	555	555
Minimum radius of curvature (mm)	340	609	878
Case-to-coil clearance (mm) for groundwall	12	9.6	9.6
Total number of conductor layers	9	8	8
Total number of auxiliary coolant conduit layers	1	0	0
Total number of layers in winding window	10	8	8
Total number of conductor turns per layer	24	18	18
Avg. Total conductor length (m) per layer	560	451.4	482.7
Avg. Coolant flow path length (m) per layer	280	451.4	482.7
Total winding window current (MA-turns)	1	6.55	6.55
Total number of turns	216	144	144
Estimated total stored energy (MJ per coil)	13	1200	1,200
Estimated total inductance (H)	1.2	1.2	1.2
Dump resistance (Ω)	0.1	0.33	0.33
Center-tapped winding quench voltage (kV)	± 0.25	± 7.5	± 7.5
Number of CICC strands	216	486	486
Nb ₃ Sn strand diameter (mm)	0.62	0.895	0.895
Cu/non-Cu ratio	1:1	2.27:1	2.27:1
Non-Cu area (mm ²)	32.6	93.5	93.5

Table 4.4-II.
Table 4.4-I (Cont'd)

	Insert	Outsert A	Outsert B
Number of cabling stages	4	5	5
Cable space void fraction	0.3	0.35	0.35
Central flow channel diameter (mm)	5	n/a	n/a
Peak magnetic field (T)	14.5	9.2	7.3
Operating current (kA)	4.63	45.5	45.5
Predicted minimum temperature margin (K)	0.7	2.1	TBD
QUENCHER predicted quench temperature (K)	70	170	TBD
QUENCHER predicted quench pressure (MPa)	1.9	4	4
Square outer conduit dimension (mm)	16	25.7	25.7
Conduit minimum wall thickness (mm)	1.5	2	2
Conduit inner diameter (mm)	13	n/a	n/a
Conduit material type	I908	I908	I908
Insulation wrap thickness (mm)	1.25	1.25	1.25
Turn-to-turn spacing (mm)	2.5	2.5	2.5
Layer-to-layer spacing (mm)	2.5	2.5	2.5
Helium maximum mass flow rate (g/s per layer)	8	3.5	TBD
Helium maximum inlet pressure (MPa)	1.25	1.25	TBD
Helium inlet temperature (K)	3	3	3
Helium maximum outlet temperature (K)	5.7	5.2	TBD

Steps which can be taken during a subsequent design development effort to address the aforementioned issues include:

1. Perform experimental conductor bending trials with twist to revise bend radius limits.
2. Develop a smeared property finite element structural model of an encased non-planar coil to predict strains due to cool-down and electromagnetic loads.
3. Review recent wire developments, such as tertiary alloys and artificial pinning center (APC) technologies, to determine potential gains in critical current density and applicability to the stellarator power plant.
4. Conduct a manufacturing study of the winding and insulating process.
5. Develop a structural model of conduit and refine wall thickness to account for strains due to bending and quench pressure rise.
6. Perform a more detailed, self-consistent cooling design study based on steady-state thermal-hydraulic simulations of the entire winding pack, including case conduction [29]. Investigate the effects of counterflow coolant routing, case cooling with conductor effluent, subcooling to 2.5K, and more efficient utilization of the helium heat capacity via optimal selection of coolant inlet pressure and temperature combinations.
7. Refine MIITS calculation of quench performance, using a nonlinear coupled electrical circuit model of nested coil and case assembly to predict case eddy currents as well as revise quench voltage and temperature rise. Use prediction of case eddy currents to determine impact on magnet structural design.
8. Obtain input from wire vendors to assess the extent to which further increases in Nb₃Sn critical current density and processing yields are possible and how such increases would impact the magnet internal design.

REFERENCES

- [1] D. S. Beard *et al.*, "The IEA Large Coil Task," *Fusion Eng. & Des.*, **7** (1988).
- [2] J. R. Miller, S. W. Van Sciver, *et al.*, "A Design for the Superconducting Outsert of a 45-T Hybrid Magnet System Using Cable-in-Conduit Conductors," *IEEE Transactions on Magnetics*, **3**:71-77 (1993).
- [3] T. Kiyoshi, K. Inoue, *et al.*, "Generation of Magnetic Fields over 20 T Using a Newly Developed Superconducting Magnet System," *IEEE Trans. Magnetics*, **3** (1993) 78.
- [4] S. W. Van Sciver, *Helium Cryogenics*, Plenum Press, NY (1986).
- [5] L. Bottura, N. Mitchell, J. V. Minervini, "Design Criteria for Stability in Cable-in-Conduit Conductors," *Cryogenics*, **31** (July 1991) 510.
- [6] R. L. Wong, "Thermo-hydraulic Analysis of Superconducting Toroidal-Field Magnets for the Tokamak Physics Experiment," *Adv. In Cryo. Eng.*, **39** (1994) 1715.
- [7] S. K. Singh, Westinghouse Science & Technology Center, private communication (June 1994).
- [8] F. Rau, Max-Planck-Institut für Plasmaphysik (IPP), Garching, Germany, private communication (October 1994).
- [9] V. Arp, "Forced Flow, Single-Phase Helium Cooling Systems," *Adv. In Cryo. Eng.*, **17** (1972) 342.
- [10] R. J. Hooper, "Friction Factor Correlations for Cable- in-Conduit Conductors," Fusion Engineering Design Center report FEDC-M-84-E/M-016, Oak Ridge National Laboratory (1984).
- [11] J. W. Lue, J. R. Miller and J. C. Lottin, "Pressure Drop Measurements on Forced Flow Cable Conductors," *IEEE Trans. Magnetics*, **15** (1979) 53.
- [12] Chen-yu Gung, MIT Plasma Fusion Center, private communications (July 1994).
- [13] L. El-Guebaly, University of Wisconsin, Madison (October 1994).
- [14] G. Morgan, "A Comparison of Two Analytic Forms for the J_c (B,T) surface," SSC Tech. Note No. 310-1 (SSC-MD-218), (January 1989).

- [15] O. R. Christianson, H. L. Chuboy, M. P. Krefta, and S. K. Singh, "High Energy Booster Dipole Model Magnet (HDMM) Margin," IISSC, San Francisco, (May 6, 1993).
- [16] O. R. Christianson, "Conductor Margin in the SSC Collider Dipole Magnet (CDM)," Westinghouse STC Doc. 93-8TM4-CDMCD-R1, (July 1, 1993).
- [17] M. A. Green, "Calculating the J_c , B_c , T_c Surface for Niobium Titanium Using a Reduced-State Model," *Proc. 1988 App. Superconductivity Conf.*, San Francisco, (August 21-25, 1988).
- [18] L. T. Summers, M. W. Guinan, J. R. Miller, and P. A. Hahn, "A Model for the Prediction of Nb_3Sn Critical Current as a Function of Field, Temperature, Strain and Radiation Damage," *IEEE Trans. Magnetics*, **27** (March 1991).
- [19] M. N. Wilson, *Superconducting Magnets*, Oxford Science Publications, (1983).
- [20] L. Bottura, N. Mitchell, and J. V. Minervini, "Design Criteria for Stability in Cable-in-Conduit Conductors," *Cryogenics*, **31** (J1991) 510.
- [21] F. R. Fickett, "A Preliminary Investigation of the Behavior of High Purity Copper in High Magnetic Fields," INCRA Project No. 186 (June 1972).
- [22] "User's Guide to HEPAK Version 3.21," Cryodata Inc., P. O. Box 558, Niwot, CO 80544.
- [23] S. Pourrahimi, C. L. H. Thieme, and S. Foner, "21 Tesla Powder Metallurgy Processed Nb_3Sn (Ti) Using (Nb-1.2wt%Ti) Powders," *IEEE Trans. Magnetics* **23** (1987) 661.
- [24] S. Foner, C. L. H. Thieme, S. Pourrahimi, and B. B. Schwartz, "Practical Processing of A-15 Multifilamentary Superconducting Wire from Powders: Nb_3Al and Nb_2Sn ," *Adv. Cryo. Eng.*, **23** (1986) 1031.
- [25] G. D. Cody *et al.*, "Phenomenon of Superconductivity," Air Force Materials Laboratory Report AFML-TR-65-169, Wright-Patterson AFB, Ohio, (June 1965).
- [26] M. S. McAshan, "MIITS Integrals for Copper and for Nb- 46.5% Ti," SSC-N-468 (1988).
- [27] A. Shajii and J. P. Freidberg, "Quench in Superconducting Magnets. I. Model and Numerical Implementation," *J. Appl. Phys.*, **76** (1994) 3149.

- [28] A. Shajii, and J. P. Freidberg, "Quench in Superconducting Magnets. II. Analytical Solution," *J. Appl. Phys.*, **76** (1994) 3159.
- [29] T. Kupiszewski and O. R. Christianson, "Predicted Thermal-Hydraulic Characteristics of Cable-in-Conduit Conductor Windings During Steady-State Operation," presented at CEC/ICMC 95, Columbus, OH, (July 1995).