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MagRad: A Code to Optimize the Operation of Superconducting Magnets in a Radiation Environment

by
Christopher T. Yeaw

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A dissertation submitted in partial fulfillment
of the requirements for the degree of

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(Nuclear Engineering and Engineering Physics)

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University of Wisconsin - Madison
1995

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the reactor can be achieved by adopting a suitable superconductor annealing schedule. The ARIES-II magnet design has been demonstrated to have several performance difficulties requiring significant design modification. Nevertheless, a 10-15% cost reduction compared to the modified base case design can be achieved via a superconductor annealing schedule. In both of these reactor analyses, the performance predictive capability of MagRad and its associated costing techniques have been demonstrated.

J J Kulcinski 8/11/95

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List of Acronyms:

AC	Alternate Current
ARIES	Advanced Reactor Innovation and Evaluation Study
B/S/VV	Blanket / Shield / Vacuum Vessel
CDA	Conceptual Design Activity
CICC	Cable-in-Conduit Conductor
COE	Cost of Electricity
CS	Central Solenoid
D-T	Deuterium-Tritium
EDA	Engineering Design Activity
EDA/HP-I	Engineering Design Activity / High Performance - I
EDA/HP-II	Engineering Design Activity / High Performance - II
FPY	Full Power Year
GFRP	Glass Fiber Reinforced Polyimide
IEEE	Institute for Electrical and Electronic Engineering
ITER	International Thermonuclear Experimental Reactor
LLNL	Lawrence Livermore National Laboratory
MIT	Massachusetts Institute of Technology
MLI	Multi-layer Insulation
ONEDANT	One-Dimensional, Diffusion-Accelerated, Neutral-Particle Transport
PF	Poloidal Field
RRR	Residual Resistivity Ratio
RTNS-II	Rotating Target Neutron Source - II
SMES	Superconducting Magnetic Energy Storage
TF	Toroidal Field
TFTR	Tokamak Fusion Test Facility
US-DPC	United States - Demonstration Poloidal Coil
WH-LCP	Westinghouse - Large Coil Program

PART I: INTRODUCTION

CHAPTER 1: DESCRIPTION OF THESIS GOALS

The ultimate goal of the thesis is to investigate and develop an optimization process for fusion magnet coils, with a view to minimizing the cost of electricity from a commercial power reactor. A related goal is to calculate the equivalent cost figure of merit, such as capital cost per full power day, in the case of an experimental reactor. Optimization of a current specific design, namely ITER [1], provides a solid basis to extend the application of these optimization schemes to a power reactor design, such as one of the recent ARIES designs [2]. Trade-offs, such as annealing the magnets at high temperatures versus increasing the shield thickness are analyzed on a cost basis, bearing in mind that the overall optimization depends heavily upon scaling laws and costing laws which are currently in use. The scope of the fundamental research, an additional goal for this thesis, is the comprehensive optimization of the toroidal field (TF) coils (using the cable-in-conduit conductor (CICC) geometry and a Nb_3Sn alloy superconductor) for a deuterium-tritium (D-T) reactor with all fluence effects included.

Optimization of a CICC magnet with respect to stability is generally regarded as the most complicated optimization process in the design of magnets for fusion [3]. *RadStab*, a code developed by this author, has been benchmarked against the numerical results of much more complex codes [4], and is incorporated as a module into *MagRad*, a code developed in this thesis which optimizes magnet operation in the D-T

radiation environment. All fluence effects to the various materials composing the coil have been included in *MagRad* and the stability of these coils can be accurately predicted with a high degree of confidence. *RadQuench*, another code developed by this author, has a common general structure, such as the one used by J. R. Miller at the National High Magnetic Field Laboratory at Florida State University (NHMFL) [5, 6], but is developed based upon a unique three phase model. This protection code [7], which has been benchmarked to the Westinghouse - Large Coil Task coil (WH - LCT) [8], is incorporated into *MagRad*. In particular, the work done on the effects of fluence upon the stability of fusion coils has been presented at a stability workshop conducted at NHMFL. This same work, expanded upon, has also been presented at the 15th IEEE Symposium on Fusion Engineering [9].

The heat removal analysis used to examine the steady state operating temperatures and pressures in the coil, is based upon a code developed by A. Long at MIT [10, 11]. This code, after significant modification, has been incorporated as a module into *MagRad*. Poloidal nuclear heating peaking factors are calculated using an analytic expression based upon numerical results for ITER. These routines, having been integrated into the coil optimization code, are able to optimize against any relevant magnet design variable, and are used to conduct sensitivity investigations against design-specific parameters. Finally, given various scaling laws, rules of thumb (for example, in calculating the fluence at the back of the shield, a scaling law based upon detailed neutronics analyses will be used rather than performing a full-scale 3-D neutronics calculation), and costing laws, the overall fusion reactor optimization is performed. This optimization, significantly, includes annealing schedules and materials options. The emphasis is upon the minimization of total reactor cost.

In conclusion, the main goal of the thesis has been to accurately predict how the cost figure of merit for a fusion reactor depends upon TF coil design. The results of

this research have been used to investigate the effects of coil optimization upon the cost of electricity from commercial fusion power plants. A secondary goal has been to understand and recommend ways to improve the performance of present D-T tokamaks. Toward these goals, the ITER Conceptual Design Activity (CDA) and the ARIES-II/IV designs have been used as reactor relevant, design specific benchmarks. It should be known that none of this work was funded by the ITER or ARIES project, therefore complete objectivity can be preserved. Furthermore, it should be kept in mind that the ITER Engineering Design Activity (EDA) design is only a tentative design and subject to rapid change. Therefore it is used sparingly, and only as a convenient benchmark against which the optimizing process may be tested. Throughout this work, emphasis has been placed upon gaining insight on how the TF coils can operate more efficiently in D-T tokamaks.

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CHAPTER 2: GENERAL INTRODUCTION

A. Fusion Overview

The development of fusion energy as a source of commercial power generation has been the ultimate goal of the fusion community for decades. The need for a new energy source can be clearly understood in view of the dwindling supply of fossil fuels coupled with the current underlying mistrust of the general public toward fission power. Commercial fusion energy would have the advantages of having a virtually unlimited source of fuel (given that the reactors are breeding tritium from lithium), an integrated biological hazard potential lower than that of fission energy (primarily due to the conspicuous absence of long-lived actinides), a less complicated waste disposal problem, very little connection to nuclear weapons, and a competitive cost of electricity. Of course, the cost of fusion energy in the short run is certainly greater than any present sources of commercial energy due to very large capital costs, but it is possible that later generation reactors may be more competitive in the cost of electricity which they offer [1]. Given these advantages, both national and international programs have been established in magnetic fusion and are moving ahead toward the design of the first full-size experimental reactor, the International Thermonuclear Experimental Reactor, ITER [2-4]. These programs are also looking forward toward the design and construction of the first prototype commercial reactor, yet several decades away.

The current status of the US magnetic fusion program is still not as developed as some scientists would desire. However, there still is a significant program in place. Some fundamental research in the areas of plasma physics and reactor materials continues, and the government is presently funding the design of two large scale experiments, as well as finishing the TFTR experimental program at Princeton [5]. The two major designs for near

term experimental machines are the aforementioned ITER design [2-4] and the Tokamak Physics Experiment, TPX [6, 7].

It is in the context of the present magnetic fusion program that the scope of this thesis has been defined. Furthermore, the thesis also confines itself to Tokamak designs, since the coils for other magnetic configuration are quite different, usually simpler (except for stellarators) and one could make use of scaling laws in general. This thesis is also concerned with only systems fueled by the D-T fusion cycle, since other fuel cycles are still far away from any specific engineering designs and, in any case, represent somewhat easier challenges with respect to shielding and radiation effects. An example of an advanced fuel cycle would be $\text{He}^3\text{-He}^3$, which produces no neutrons, and therefore demands a much less rigorous temporal analysis (i.e. properties do not change over time). The physics of these more advanced fuel cycles is quite challenging, however, and will require many more years of study. In all D-T systems that are examined, all variables that are physics-driven are kept constant. It is beyond the scope of this thesis to treat physics parameters as well as technological parameters.

In commercial magnetic fusion energy (MFE) systems, the coils must be superconducting (neglecting, for example, the possible use of hyperconducting aluminum) in order to reduce both the size of the machine and the power input to drive the current in the coils. Furthermore, the large magnetic fields which must be generated can only be done so, at reasonable expense, by superconducting magnets. These magnets can be said to be the fundamental component of all magnetic confinement machines, and so are of the utmost importance. Furthermore, the cost of these magnets represents a significant fraction of the total direct cost of the reactor (up to about 30%) [8]. It is, therefore, of crucial importance to optimize these coils with respect to their performance. Designing safe and reliable coils is the primary task of the magnet engineer. Safety of the coils concerns both the protection of the coils themselves against irreparable damage as well as the containment

of any damage due to the failure of the coils themselves. In other words, damage to other reactor systems must be precluded in the event of coil fault conditions. The reliability of the coils requires a sufficiently high stability margin such that the coils are rarely, if ever, quenched during regular operation of the reactor. Both safety and reliability are of immense importance, not only in the design of commercial reactors, but also in the design of an experimental device such as ITER. If the coils do not operate safely during fault conditions (e.g. a coil quench) then either great damage can be done to the coil itself, rendering it inoperable or, what could be even worse, system components external to the coil, such as the blanket and shield, might be damaged. In either case, a significant amount of time could be lost repairing the machine (TF coils are very difficult to remove). The result would be a loss in valuable experimental time (in the case of an experimental reactor) or a removal of the power plant from the grid for extended periods of time (ranging up to several months or longer). In both cases, a very costly component of the reactor must be replaced. In addition, if the coils do not operate reliably, the coils will quench often. Even if the coils demonstrate safe operation under these quenching fault conditions, much time would be lost due to the re-cooling of the coils, which can take up to several weeks for one cool-down cycle. Even in the case that the coil current is dumped with very little temperature rise in the coil, the reactor core would be shut down at least temporarily.

Currently, the leading candidate conductor for large fusion magnets is the CICC design using one of several superconductors in the Nb-Sn system as the superconducting material. The CICC design has been chosen as the best candidate due to several factors: the ability to maintain mechanical integrity under large forces; the ability to remove large amounts of power (from nuclear heating of the cable); the ease of designing sufficient turn-to-turn insulation; low AC losses; low strain in the cable due to the conductor following the deformation of the conductor jacket; and ease of fabrication [9]. The ternary Nb₃Sn alloys (or the corresponding compound, Nb₃Sn, with no tertiary alloying elements)

have been chosen as the materials of choice due to the large fields which are permitted by their use, as well as the extensive fabrication database which has been developed for them (as opposed to, say, $\text{Nb}_3(\text{Al}, \text{Ge})$). Thus, the calculations in this thesis are restricted to Nb_3Sn alloys in the CICC geometry, but the formulation could be easily extended to other superconducting materials and coil configurations.

Cable-in-conduit conductor overall cross-sectional geometries have been designed according to several possibilities: the cable could be placed in a rectangular conduit, which serves also as the jacket; the cable could be placed in circular conduit, which is then placed in a rectangular jacket; or the cable could be placed in a circular conduit, which also serves as the jacket. These possibilities involve only minor modifications to analyses, so we have chosen to restrict this thesis, for the sake of simplicity, to the case of a circular conduit which also serves as the jacket. Central core helium cooling flowpaths may also be incorporated into any of the above general geometries. *MagRad*, the optimization code which has been developed, accommodates for this possibility.

Moreover, the bulk of the work presented is specific for the toroidal field, TF, coils only. The reason for this further restriction in the scope of the thesis is that it is the TF coils which will receive the highest end-of-life neutron fluences of any of the coils in a tokamak. The TF coils also will be operating at very high fields (in the range of 11-14 Tesla), nearly as high as the poloidal field (PF), or even, the central solenoid (CS) coils. Moreover, the CS coils have the primary difficulty of keeping AC losses to a minimum in the coils, which has much more to do with superconductor cable fabrication (including parameters such as effective filament diameter and resistive coating barriers) than with fluence effects. In the two major designs mentioned previously, the CS coils are shielded by the TF coils, which greatly reduce the end-of-life fluence to the CS coils.

Grading of large coils is a significant design option which can be quite economical. Grading, in a shell type layer-wound coil, of TF magnets has been proposed for most

reactor designs [2, 10-12]. Using less costly grades of conductor in the outer portions of the magnet, however, does not change the general performance characteristics of the inner portions of the magnet, and it is these inner turns of the magnet where the analysis is especially constraining due to high fields, high fluxes, and high fluences. While there may be some perturbation in the maximum hot spot temperature in the event of a quench in the inner turns, this perturbation will be relatively small, and can only be accurately treated by a very detailed numerical simulator (which has not yet been developed). As far as the stability of the inner turns is concerned, the grading of the magnet has absolutely no effect since stability is a local, transient phenomenon, highly dependent upon very localized variables. Furthermore, since in these shell-type designs each layer is designed to contain an independent flowpath (as opposed to pancake-type designs, in which each pancake layer is, wound radially outward, and considered to be an independent flowpath), grading has no effect upon the steady state heat removal characteristics of the inner turns. Thus, grading is not taken into account as a design variable to be optimized in this thesis.

Fluence effects upon the two primary design criteria of the TF coils, stability and protection, together with the analysis of high-temperature annealing potential, comprise the bulk of the work in this thesis. Very little work had been previously performed in this area of research, and what had been done in taking fluence effects into consideration will be shown to be quite unreliable and often inaccurate. Although some work had been done concerning the optimization of conductors for use in fusion magnet coils, the previous work had largely neglected fluence effects, possible annealing schemes, interface of conductor optimization with reactor optimization, etc. [9, 11, 13-23]. Optimization of the coil with respect to several variables simultaneously is quite important. These criteria may, in general, not optimize independently at the same values given the range of variables to be considered. The criteria to be optimized for the conductor design are: steady state heat removal, stability, and protection. These criteria are also optimized against such variables

as the copper fraction in the cable space, the superconductor fraction in the cable space, the inlet pressure (or equivalently, the mass flow rate), etc. Some variables are not explicitly optimized, such as strand diameter, operating field, etc. Rather, these variables are taken as design specific (such as ITER specifications) and the sensitivity of the various design criteria against these variables is analyzed.

Finally, the coil optimization is integrated into an overall system optimization scheme. This overall optimization scheme is, of course, not comprehensive in scope, because of the extreme complexity of fusion reactor systems and their components. This scheme does, however, incorporate as much relevant input as is needed in order to sufficiently investigate the impact of system optimization upon coil optimization. The overall optimization also incorporates such possibilities as the use of inorganic insulators in the coils coupled with annealing schemes. In this way, this scheme represents new work in system optimization, as well as the areas mentioned above.

B. MagRad Overview

MagRad is a user-friendly code designed to optimize fusion tokamak TF coils with respect to the balance of the reactor system. Being an optimization code, the final parameter to be optimized is a cost figure of merit which will differ, in general, depending on the purpose of the reactor (experimental or commercial). *MagRad* has been developed with ease of use in mind. In light of this, the code operates from the Excel® spreadsheet software platform, and has been designed for the Macintosh™, though with slight modifications it can run in Excel® on either a PC or a workstation. Technically, *MagRad* analyzes most of the relevant parameters for such coil systems: conductor specifications; steady state heat transfer characteristics; transient stability response; quench protection characteristics; and necessary annealing schedules. These parameters, additionally, are coupled quite strongly to the rest of the reactor design, especially the blanket and shield.

The purpose of *MagRad* is not so much to give the exact values of the characteristics of the coil, but rather to present to the user a good first approximation to an optimal coil design and give some indication concerning various trends in the optimization procedure. It has been decided not to attempt to predict magnet performance characteristics exactly for two main reasons. The first is that the uncertainties involved in the prediction of such values is a present limitation even for the most advanced codes, such as the codes which calculate stability margins using full thermal hydraulic numerical analysis. Inadequately tested assumptions and restrictions on the dimensionality of the problem rule the day in such codes. It has become clear from the benchmarking of *MagRad*, which will be discussed in the several chapters to follow, that the use of simpler methods does not suffer from any significant disadvantages.

The second reason for avoiding the calculation of exact values is that the determination of such values is very time consuming. Not only do codes which can

perform such predictions take a long time to write and debug, they involve a prohibitively long computation time on present hardware. One such code, *CICC*, forms the basis of the present work on stability, and while it is an exceptionally useful code, it requires hours of cpu time on a Cray to calculate the relevant information. Such codes are excellent tools in the second step of the design procedure, but for quick, fairly accurate optimization studies they are counterproductive. A researcher could spend years on the development of a sophisticated code which performs in detail what just one module of *MagRad* performs. The writing and coupling of several such codes would be well beyond the scope of a normal PhD program. Again, it must be stressed that such large and sophisticated numerical prediction codes are absolutely necessary as the second step in the design process, and offer the designer invaluable tools by which to establish and confirm final coil designs. *MagRad* offers the designer a rapid and powerful method for the prediction of the optimal coil design, as well as allowing for a multitude of trade studies.

Modularization has been a key component in the development of *MagRad*. Not only does modularization produce a very organized code which is easy to use, but it has also allowed for the independent benchmarking of the several code modules against both experiment and more complex numerical codes. By design, the code modules are kept relatively simple in order to keep the computing time to a minimum. Benchmarking takes on an important role, then, since in reducing the computing time involved it is obviously undesirable to sacrifice accuracy. However, the accuracy of *MagRad* will be shown to be quite acceptable, for both optimization and benchmarking studies.

Once the modules of *MagRad* were independently demonstrated to be accurate and reliable, they were coupled in various ways. Some of the interfacing is rather direct, such as the coupling between steady state heat transfer and stability modules. In this case, the steady state heat transfer characteristics of the coil determine the maximum operating temperature on the inner turn of the inboard midplane, which in turn determines the

temperature at which stability analysis must begin. An example of less direct coupling between modules would be that which exists between the design of the shield and the steady state heat transfer modules. The neutron flux which penetrates through to the coil is converted, via a scaling law, into a nuclear heat load imposed upon the conductor. This, in turn, determines the steady state heat load along the length of the conductor and becomes the dominant input to the steady state heat transfer module.

Structurally, *MagRad* consists of a main analysis module, to which the user is interfaced, together with several macro codes (much like sub-routines in FORTRAN) which serve to perform all of the calculations. In this main module, called *MagRad Master*, relevant input is accepted, and all vital output is returned. Other modules included in the *MagRad* workbook include:

- a report summary worksheet which contains all of the most important design variables and output in an easily accessible form for design comparisons, called *Report*;
- the macro which records all of the relevant data to *Report*, called *Reporter*;
- the macro which calculates the steady state operating temperature, a modified version of a different code [2], called *RadHeat*;
- a look-up macro which includes all of the relevant properties of helium called by *RadHeat*, called *Thermo*;
- a macro which calculates the superconducting operating point, called *SuperOp*;
- a macro which calculates the stability margin both at start-up and at shut-down, called *RadStab*;
- a macro which calculates the necessary stabilizer annealing schedule, called *StabAnneal*;
- a macro which calculates the temperatures attained in the magnet during a quench, called *RadQuench*;

- a macro which calculates the high temperature annealing characteristics of the design, called *SuperAnneal*;
- a linked spreadsheet which calculates the re-cool characteristics of the design, called *CoolDown*,
- a macro which calculates the cost of the reactor and all components, called *CostComp*.

The linked structure of the *MagRad* code is shown in Fig. 1.

Fig. 1: Diagram of coupling links between modules in *MagRad*.

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PART II: MAGNET DESIGN

CHAPTER 3: RADIATION DAMAGE EFFECTS UPON SUPERCONDUCTING COIL MATERIALS

A. Introductory Remarks:

Before examining, in detail, the effects of radiation upon the materials composing the magnet coils, a few observations will be made concerning radiation damage. In fusion design studies, it is common for the designers working on the neutronics of the device to specify the flux and fluence (flux multiplied by time) at the coils. While these values are helpful, they do not give clear information concerning the actual damage which the materials sustain. This damage is caused by the collision of the incoming radiation upon the material matrix. In the case of fusion magnet coils, the types of radiation which cause the damage are gammas and neutrons. The gammas will only be a major damage concern for the organic insulators (if used), although the gamma flux is also the primary component responsible for the nuclear heating in the magnet. The neutrons, on the other hand, do damage in every material component. In the materials which have a crystal structure, the steel, the copper and the superconducting material, the damage can be characterized by a parameter called the damage energy. The damage energy is defined as the total energy imparted to the lattice atoms averaged over all of the atoms. To convert from fluence to damage energy the following equations may be used,

$$n = N \times \sigma \times \phi \times t \quad (1)$$

$$E_d = \kappa \times E_n \times n \times \frac{MW}{\rho \times N_A} \quad (2)$$

where n is the number density of atomic collisions (m^{-3}), N is the number density of atoms in the lattice (m^{-3}), σ is the average cross section for elastic scattering (m^2), ϕ is the flux ($\text{m}^{-2}\text{s}^{-1}$), t is the time (s), E_d is the damage energy imparted to the lattice atoms (eV/atom), κ is the damage efficiency (taken in the Kinchin-Pease model as 0.8, though for fusion neutrons impacting the conductor material lattices, κ is closer to about 0.5), E_n is the energy of the incident neutron, MW is the molecular weight of the material (kg/mol), ρ is the density of the lattice material (kg/m^3), and N_A is Avogadro's number (atoms/mol). The damage can also be characterized in its more familiar form of displacements per atom (dpa), which is a measure of how many atoms have been displaced off of their lattice sites,

$$dpa = \frac{E_d}{E_b} \quad (3)$$

where E_b is the energy holding the atom in the lattice (eV). The energy, E_b , is usually estimated to be about 25 eV. Thus, for example, a fluence of 1×10^{18} RTNS-II n/cm² would correspond to a damage energy of about 0.25 eV/atom or 0.001 dpa.

The value of the damage given in dpa gives a good indication of the microstructural damage at 4 kelvins, since very little recombination between freely migrating defects can occur at such low temperatures. Care must be taken, though, even when using the measure of dpa, since not all properties of these materials scale in some way with dpa. The copper resistivity and the superconducting critical temperature do, but the upper critical field and the critical current density do not [1-4].

These latter two parameters scale with the degree of flux pinning, which may scale more directly with evolved microstructural defects, rather than simply with Frenkel pairs, or even defect clusters.

Finally, as this thesis deals with the TF coils for tokamaks, a drawing of tokamak is presented in Fig. 3 in order to help the reader visualize what is being considered. As one can readily see, the TF coils are shielded by a blanket and shield region (closer to the plasma), thus softening the neutron spectrum in which the coils must operate. Figure 2 shows the geometric configuration of two D-shaped coils of different sizes, without the surrounding reactor structure. Shown in Fig. 4 is a plot of the critical parameters for an advanced Nb_3Sn alloy superconductor (parameters specified [5] by the ITER Magnet Group). The critical parameters are the critical current density (vertical axis), the upper critical field, and the critical temperature. This type of plot will be referred to as a JBT plot. It will be the surface in this plot which will undergo changes with respect to fluence, and thus impact the optimization of the coil design. Stability, as will be explained more fully later in the proposal, can be thought of as a measure of the distance, in JBT space, between the operating point, which is not designed to change usually, and the JBT surface, which changes with fluence. The greater this distance, the more stable is the magnet.

Fig. 2: Schematic of two nested D-shaped TF coils (with typical dimensions).

Fig. 3: D-shaped cross sectional view of the ITER tokamak with TF coils [6].

Fig. 4: Surface plot of superconductor critical parameters as specified in ITER CDA.

B. Upper Critical Field:

One of the more challenging obstacles that needs to be overcome in the design of superconducting TF coils is the problem involving the accurate prediction of the upper critical field at zero temperature, strain, and current density, B_{c20} . The form of B_{c20} currently in use is [7],

$$B_{c20m}(E_d) = B_{c20m}(0) \times \frac{\rho_0 + 600 \times E_d}{\rho_0} \times \exp\left(-\frac{5}{8} \times E_d\right) \quad (4)$$

where $B_{c20}(E_d)$ is the upper critical field prediction (in Tesla) at the given damage energy, $B_{c20}(0)$ is the upper critical field (in Tesla) at zero fluence, ρ_0 is the initial normal state resistivity (in $n\Omega m$), and E_d is the damage energy (in eV/atom). This form has been used in all of the stability codes previously surveyed. That fact, coupled with the apparently strong theoretical basis for the expression [7], led to the incorporation of this expression into the code *MagRad*. However, for any reasonable value of the initial normal state resistivity (less than, say, 400 $n\Omega m$), this expression grossly overestimates the upper critical field (see Fig. 5, which uses the most commonly used value for the initial normal state resistivity, 160 $n\Omega m$). This overestimation results in the increasing of predicted conductor stability with fluence, a result which has been predicted by other codes as well. Although the upper critical field does not explicitly enter into the equation for the limiting current, it impacts stability in that both the reduced field at the limiting current and the reduced temperature at the limiting current are greatly depressed with fluence, since there is a constraining relation between these two parameters. This dual parameter depression yields an increase in the distance, in JBT space, between the operating point at the limiting current and the critical surface at the limiting current, thus enhancing stability. It is this stability enhancement with

fluence, certainly going against intuition, that caused a review of this upper critical field expression.

Fig. 5: Dependence of B_{c20} is not well predicted in the literature.

In its favor, Eqn. (4) has a logical theoretical basis, namely that the upper critical field is proportional to the normal state resistivity, the electron specific heat, and the critical temperature. However, the peak upper critical field with respect to fluence predicted by Eqn. (4) may be found analytically by taking its derivative,

$$\frac{\partial B_{c20m}(E_d)}{\partial E_d} = B_{c20m}(0) \times \left[\frac{600}{\rho_0} \times \exp\left(-\frac{5}{8} \times E_d\right) - \frac{5}{8} \times \left(1 + \frac{600}{\rho_0} \times E_d\right) \times \exp\left(-\frac{5}{8} \times E_d\right) \right] \quad (5)$$

Setting the derivative to zero, it is found that the upper critical field is predicted to peak at a damage energy of about 1.33 eV/atom (0.053 dpa), corresponding to an RTNS-II

fluence of about 5×10^{18} n/cm² (very high for the magnets), and has a peak value of about 63 T. This value is obviously much too high. Hahn's data [8-10] gives a B_{c20} of about 21.4 T at zero fluence increasing to about 22.4 T at a fluence of 0.9×10^{18} n/cm². Accordingly, until a theoretically based expression can come close to matching the data, the upper critical field is predicted by simply fitting Hahn's data with a fourth order polynomial, taking careful note of the fluence range over which the data are applicable.

Pursuing the development of a theoretically based expression, following the path taken by Guinan [7], does not seem to help much. It seems that the electron specific heat is proportional to the critical temperature [11-13], which would give even worse upper critical field predictions. However, there are conflicting data concerning the dependence of the normal state resistivity upon critical temperature (or, equivalently, upon fluence) [2, 11, 13]. Compounding these difficulties, it seems as though Guinan takes the electron specific heat 'data' as just that, data. However, this is not the case. The electron specific heat is, even if it is presented in the literature in a form suggesting that it is measured data, in fact, only 'derived data'. That is, a plot is made of the electron specific heat based upon certain equations (which differ slightly from paper to paper) which are themselves derived using the 'dirty limit' approximation [13], in which the normal state electron mean free path is assumed to be much smaller than the superconducting coherence length. Thus, if it happens that this approximation is not quite valid, not only is Guinan's basic assumption inappropriate, but also the derivation culminating in Eqn. (4) will be incorporating inappropriate data, compounding the error. These problems make it expedient to pursue a different course of theoretical analysis.

The problem lies in one of the assumptions used as a basis for Guinan's expression. It is only in the so called dirty limit that the previously mentioned proportionalities hold true. It has been shown assuming Nb₃Sn is in the dirty limit is

not an appropriate assumption under all conditions, and, in fact, at zero fluence the pure binary compound Nb_3Sn is in the 'clean limit' [11, 14]. Moreover, since the electron specific heat is not the measured data in the literature, nothing is gained by choosing a theoretical basis for the desired expression which includes this property as a basic building block. Instead, it seems more prudent to choose a theoretical path culminating in an expression which itself incorporates only those variables which correspond to measured data. This is, in fact, what has been pursued in the course of this work. Although the form of the expression which has been developed is much more desirable and the predictions which come from it are somewhat closer to the experimental data, the results are still not good enough for use in magnet design. Work on the development of such an expression based upon solid state physics theory (which would incorporate fundamental solid state physics parameters) for the prediction of the upper critical field has been dropped at this time. Pursuing such an expression lies somewhat outside the proposed purpose and scope of the thesis, and would require a substantial and expensive experimental effort. At this time it is sufficient for our purposes to have an expression, regardless of the empirical nature of it, which is based upon the data and accurately predicts the upper critical field as a function of fluence.

C. Critical Temperature:

Much work has been done with respect to the effect of neutron fluence on the critical temperature [2, 8-13, 15-38]. The outcome of these studies is that the critical temperature has been predicted by theory and found by experiment to be an exponentially decreasing function of fluence. The form of the expression used to predict the critical temperature at a given fluence is quite simple,

$$T_c(E_d) = T_{c0} \times \exp(-\alpha \times E_d) \quad (6)$$

where $T_c(E_d)$ is the critical temperature (in Kelvin) prediction at a given fluence (damage), E_d is the damage energy again (in eV/atom), T_{c0} is the initial critical temperature (in Kelvin and at zero fluence), and α is a constant which can be experimentally determined or, less accurately, theoretically predicted. In fact, Hahn [9] has fit the data obtained from the low temperature irradiations of commercial superconducting wires to this form and obtained values for α which vary from alloy to alloy. This form of Eqn. 6 and these experimentally determined values of α are used in *MagRad* in order to correctly predict critical temperature. It was found that the value of the constant, α , when E_d is converted to RTNS-II fluence, varies between about 0.062 and 0.098. Of course, it must be kept in mind that the initial values for the critical temperatures of the various materials, unless otherwise specified, have been taken to be those which are specified by the materials community. Whether these values can actually be attained for the ternary alloys remains to be seen, but significant doubts as to their attainability exist among those involved in the development of these conductors. Nevertheless, in most cases these have been used, except when a comparison to presently attainable technology can be made.

D. Critical Current Density:

While the critical current density does not explicitly enter into the stability equations, it is quite important with respect to quench protection. Even though the stability is not explicitly dependent upon the critical current density, if the critical current density obeyed scaling laws that gave it a higher value at any given temperature and field, the operating point of the magnet in JBT space (see Fig. 4) would be farther from the limiting surface, thus resulting in greater stability. Regarding quench protection, a conductor with a higher critical current density can be fabricated using less superconducting material (incidentally, reducing the cost of the magnet) and more stabilizer, thus reducing the maximum hot spot temperature in the event of a quench. Thus, it is of great advantage to be able to accurately predict the response of the critical current density in a radiation environment.

Numerous studies have been performed showing that the critical current density of the Nb-Sn alloys and compounds demonstrates a peak with respect to fluence at moderate (a few $\times 10^{18}$ fast n/cm²) fluences [1, 3, 8-10, 26-28, 36-42]. Moreover, the expression used in the ITER design for the critical current density is [43],

$$J_c(B, T, \epsilon) = C(\epsilon) \times [B_{c2}(T, \epsilon)]^{\frac{1}{2}} \times (1 - t^2)^2 \times b^{\frac{1}{2}} \times (1 - b)^2 \quad (7)$$

where $J_c(B, T, \epsilon)$ is the critical current density (in A/mm²) as a function of the given magnetic field (in Tesla), temperature (in Kelvin), and strain. The value of $C(\epsilon)$ is assumed by most as a coefficient (with units of AT^{1/2}/mm²) only dependent upon the given strain. $B_{c2}(T, \epsilon)$ is the upper critical field (in Tesla) as a function of both the given temperature and the given strain, t is a dimensionless temperature parameter, equal to the given temperature divided by the critical temperature (in Kelvin and at zero field and

current density), and b is a dimensionless magnetic field parameter, equal to the magnetic field (in Tesla) divided by the upper critical field at the given temperature and strain. Some caution must be used concerning the use of this scaling law by magnet designers.

First, one must consider the idea of consistency. This scaling law was developed specifically for the ternary alloy $\text{Nb}_3(\text{Ta},\text{Sn})$, and for no other alloys in the Nb-Sn system. Yet, it is being applied somewhat indiscriminately to several Nb-Sn alloys, including the binary compound, Nb_3Sn . This is dangerous for two reasons. The first reason is that the ternary alloys are known to have better flux pinning, and hence higher critical current densities, than the binary compound. Therefore, it is not at all obvious that a critical current density scaling law developed specifically for one ternary alloy should apply to the binary alloy, or even to other ternary alloys. This matter needs further investigation, but it is well outside the scope of this thesis. The second reason that applying this scaling law to all Nb-Sn alloys is dangerous is that it has been demonstrated that the various alloys behave differently with respect to neutron fluence [9, 10]. Thus, according to the data, they do not all obey the same scaling law when fluence effects are taken into account. This brings us to the second word of caution. This scaling law does not hold (again, according to the data) even for a single alloy, unless the constant, $C(\epsilon)$, out in front of the right hand side of Eqn. (7) is allowed to vary with neutron fluence in a rather complicated way. The variation of this constant, C (as normalized to its non-irradiated value, C_0) with respect to fluence is shown in Fig. 6, and it can easily be seen that the scaling law would be quite inadequate without this variation. At present, no designer has taken this variation with fluence into account. Again, as with the value of the upper critical field, it seems wisest to simply fit the data over a given fluence range, thus predicting a fluence dependence for the leading coefficient, rather than use an expression which is

apparently defective (that is, if the leading coefficient is taken to be constant, as most magnet designers do presently). Moreover, it is certainly to be expected that this constant will vary in different ways in different alloys. Finally, it must be stressed again that the design should be consistent. It is quite fortunate that the binary compound demonstrates a significant peak in the upper critical field and critical current density, but if designers decide to go with a ternary alloy containing tantalum rather than the binary compound they must recognize that the data shows this alloy's upper critical field to decrease monotonically with fluence. Moreover, its critical current has only a very shallow peak at low to moderate fluences. Again, the higher upper critical field and critical current densities at zero fluence of the ternary alloys is very helpful, but if designers choose binary Nb_3Sn they must be aware that a different scaling law may be applicable. The moral of the story is to go by the data and be consistent.

Fig. 6: Graph of normalized prefactor for Eqn. (7), in opposition to the commonly held assumption that C/C_0 (normalized prefactor) is constant with fluence.

E. Copper Resistivity:

The fluence dependent resistivity of the copper stabilizer is of critical importance to the overall design of the magnet system. The electrical resistivity profoundly impacts both the stability of a coil and the maximum hot spot temperature of that coil in the event of a quench (which must be designed for). Intuitively, one may guess that as the resistivity of the stabilizer increases, the stability of the magnet will degrade since, in the event of a transient temperature excursion along a length of the conductor, the stabilizer (in our case copper) must begin to share the current. The higher the resistivity of the stabilizer, the more Joule heat is produced (with a resistivity squared dependence), thus increasing the likelihood of a quench sequence being initiated. Dresner [44-48] showed explicitly that the increasing the stabilizer resistivity will decrease the limiting current of the conductor (as will be explicitly shown later in the paper), and thus one may infer also the stability margin. Qualitatively, it can also be seen that an increase in the stabilizer resistivity will also increase the maximum hot spot temperature in the event of a quench for exactly the same reason, the increased production of Joule heating. Thus, it is imperative that the magnet designer be able to accurately predict the value of the resistivity of the stabilizer as a function of fluence.

In order to make such an accurate prediction, several things must be known: the neutron spectrum at the coil; the resistivity increase as a function of fluence in that spectrum at zero field; the effects of room temperature annealing upon the stabilizer resistivity; and the magnetoresistivity of the stabilizer [49]. Details of the neutron spectrum are given elsewhere in this paper, and the effects of room temperature anneals will also be discussed later. Recently, several different methods of predicting the resistivity of the copper stabilizer have been proposed. The variations between the

values given by these several predictive expressions is only several percent. These several methods are summarized in Table 1.

Table 1: Prediction from Several Models of the Results of Neutron Damage Effects on Copper Magnetoresistivity.

Damage	ITER	Guinan	Miller	Other
	[5]	[49]	[50]	[50]
dpa	nΩ-cm	nΩ-cm	nΩ-cm	nΩ-cm
0.0	62.8	59.7	63.2	68.2
0.2	67.7	66.3	66.2	73.0
0.5	75.9	74.1	72.9	80.8
1.0	90.0	86.0	85.6	94.3
1.5	103.8	97.7	98.5	107.6
2.0	117.1	109.3	111.2	120.5
4.0	164.1	152.4	157.0	166.6
6.0	201.9	188.4	194.2	203.9
8.0	232.2	217.5	224.2	233.9
10.0	256.4	240.9	248.2	257.9
15.0	297.6	280.6	289.0	298.8
20.0	321.0	303.2	312.2	322.1
50.0	350.5	331.9	341.6	351.4

NOTE: Fluences are in 1×10^{18} n/cm² (E>0.1 MeV) and resistivities are in nΩ-cm.
and, B=11.2 T RRR=155.0

A determination of the resistivity at a given magnetic field (that is, magnetoresistivity) as a function of fluence is made using the expressions given by Guinan [49]. These expressions seem to have a sound theoretical basis, as well as representing an excellent fit to the data. Another potential advantage in using these expressions is that they were developed specifically with fluence and annealing effects in mind, and fit not only irradiation data but also the annealing data. Since it is from this primary source, namely Guinan, et al. [49], that we will be obtaining our annealing data and expressions, it seems most consistent to use their expression for magnetoresistivity as a function of fluence as well. However, these expressions yields the lowest values of the several models mentioned above for the magnetoresistivity at any fluence. Fortunately, this fact should not affect the final outcome of the stability of the coils as a function of fluence, though, except in that the initial stability will be slightly higher using Guinan's expressions. The stability at higher fluences will be very similar regardless of the particular expressions adopted, since the copper will undergo periodic and frequent anneals upon reaching 100 nΩcm. It should furthermore be noted that, at low to moderate fluences (in the range of several $\times 10^{19}$ n/cm², which will be encountered at the TF magnet coils) with a spectrum similar to that characteristic of a fast fission reactor, transmutations in the copper stabilizer will account for less than 0.1% of the change in the electrical resistivity [49].

It has also been found that the magnetoresistivity of the copper does not lie on a single Kohler plot [49]. The general form of the Kohler plot is,

$$\rho(E_d, B) = \frac{\rho(E_d, 0)}{[P(E_d, B) + Q(E_d, B) + R(E_d, B)]}, \quad (8)$$

$$P(E_d, B) = \frac{\rho(E_d, 0)}{B} \times \arctan\left(\frac{f_1(E_d) \times B}{C \times \rho(E_d, 0)}\right), \quad (9)$$

$$Q(E_d, B) = \frac{\rho(E_d, 0)}{B} \times \arctan\left(\frac{f_2(E_d) \times B}{D \times \rho(E_d, 0)}\right), \quad (10)$$

$$R(E_d, B) = \frac{\rho(E_d, 0)}{B} \times \arctan\left(\frac{f_3(E_d) \times B}{E \times \rho(E_d, 0)}\right), \quad (11)$$

$$\rho(E_d, 0) = \rho_0(0) + \Delta\rho_r(E_d, 0), \quad (12)$$

$$\Delta\rho_r(E_d, 0) = \Delta\rho_r^{\infty}(0) \times [1 - \exp(-\alpha \times E_d)] \quad (13)$$

where $\rho(E_d, B)$ is the magnetoresistivity at the given fluence, $\rho_0(0)$ is the resistivity at zero fluence and at zero field, $\rho(E_d, 0)$ is the resistivity at the given fluence and at zero field, $\Delta\rho_r(E_d, 0)$ is the radiation damage induced change in the resistivity at zero field and at the given fluence (damage), $\Delta\rho_r(0)$ is the radiation damage induced saturation resistivity change at zero field and at the given fluence, α is an experimentally determined constant, B is the magnetic field (in Tesla), E_d is the damage energy (in eV/atom) corresponding to the given fluence, C , D , and E are fluence independent material parameters that have been fit to the data, and $f_1(E_d)$, $f_2(E_d)$, and $f_3(E_d)$ are fluence dependent electronic parameters that have been fit to the data. All copper resistivities are usually given in units of $\text{n}\Omega\text{cm}$. It can be seen in Fig. 7 [49] that, especially at lower fields, a pronounced spread develops in the data, as opposed to a single curve in the Kohler plot. This spread is the result of varying defect structures within the irradiated copper, with the extremes representing copper which has only point defects and small clusters of defects on the lower side and copper which has extended defects on the higher side. The copper samples having only point defects and small clusters of defects were either unirradiated-recrystallized or irradiated-unannealed

samples, while the copper samples containing extended defects were either cold-worked or irradiated-and-subsequently-annealed samples. To fit the data Guinan used the expressions given in Eqns. (8)-(13), and varied the coefficients, f_1 , f_2 , and f_3 . This method seemed to fit the data very well. However, in the high field region in which fusion systems will be operating, the spread converges to a single curve. In fact, at 11.2 T, the maximum field at the toroidal coils in the ITER CDA design, it can be shown that the two sets of coefficients representing the opposing extremes of the spread give a difference of less than 2 nΩcm. This difference will manifest itself after room temperature anneals, but is so small (less than the deviations between the various methods of determining resistivity) that it may be ignored. The outcome of this is that the annealing studies done primarily by Sawan [51] remain valid, and therefore will be used in the annealing studies of the magnet system later in this paper.

Fig. 7: Kohler plots of copper with different irradiation and annealing histories [49].

F. Insulators:

The insulation of the magnet coils is extremely important, being that component which prevents turn-to-turn shorting of the windings. This function is put to the test when the magnet quenches, thus creating very large voltages across the normal regions (of the order of several to tens of kV). If the radiation environment were to seriously degrade the electrical or mechanical properties of the insulation turn-to-turn arcing could occur having disastrous consequences for the coil. It has been shown in numerous studies that the limiting criteria for insulation degradation are mechanical in nature as opposed to electrical [52-56]. Two insulation materials options exist: organic insulators and inorganic insulators (ceramics). Of all insulation materials, the ones most widely studied to date have been the organic insulators, while data on inorganic insulators are generally lacking (at least at low temperature irradiation facilities).

Organic insulators are composed of glass filled, vacuum impregnated epoxies and polyimides. The polyimides are far better suited to the radiation environment than the epoxies [57-60]. These materials, the best studied so far being S-glass polyimide, show no significant reduction in strength to 40 MGy (4×10^9 rads), while at 100 MGy they display a 35% decrease in mechanical strength [57]. The data for S-glass polyimide represents a factor of 5-10 increase in limiting dose over the epoxies studied. A disadvantage of the glass fiber reinforced polyimides (GFRP) is that they are more expensive than their glass fiber filled epoxy counterparts.

The fundamental effect of the neutron and gamma radiation upon organic insulation is chain scission, which induces a decrease in the elastic modulus [60]. At high doses (near 100 MGy) the mechanism of failure for the GFRPs has been demonstrated to be the deterioration of the glass-to-polyimide adhesion. As a representative figure of merit, it should be pointed out that the ITER blanket and shield

design has chosen the dose limit of 50 MGy in the insulator, though this limit is currently still under debate. According to the above data this dose limit should be quite appropriate. In a commercial reactor, such as ARIES II/IV, however, the insulation might possibly be exposed to a significantly higher dose, unless the blanket and shield are somewhat thicker. This situation may make the use of inorganic insulators quite appealing. Moreover, it has been shown that even room temperature warm-ups of irradiated organic insulation are quite detrimental to the strength of these materials (though a systematic study of the effects of cycling has not been performed) [57]. This fact has profound consequences in that, if it is found that the magnets must be placed on some sort of annealing schedule for the stabilizer, and if it is further found that cycling GFRPs is detrimental to their mechanical properties, the use of organic insulators will be precluded. Thus, it can clearly be seen that there is a most pressing need to look at inorganic insulators, since on the one hand ITER (and presumably most reactors after ITER) will adopt stabilizer anneals, and on the other hand the effects of post-irradiation thermal cycling upon organics have not been studied.

It is thought that inorganic insulators will allow much higher fluences. While on the surface it seems that the fluence limit on these materials will be quite high, there have been no low temperature irradiation experiments done on inorganics. It has been argued that α - Al_2O_3 quite possibly may not be a suitable material for low temperature insulation use [61]. However, in light of the fact that spinel (MgAl_2O_4) has been shown to perform somewhat better under extreme radiation environments than Al_2O_3 [55, 62-64], this author feels it is safe to conclude that spinel, at least, will be more suitable for use in a fusion reactor, whether it is experimental or commercial. Of course, the need for conclusive low temperature neutron irradiation data cannot be overemphasized.

After contacting a manufacturer of inorganic insulators, it seems clear that the use of these types of insulators can be accommodated in a reactor coil design. Inorganic insulators may be manufactured in either tape or sleeve form. The sleeve form seems most promising for use in the TF coils, having a thickness of about 1 mm, which is on the same order as the thickness of required organic insulation. A sleeve of a Nextel™-type aluminum oxide insulator of this thickness is rated to protect against voltages of about 20 kV, which is the maximum allowed voltages in the ITER CDA design. The insulator suffers no degradation of properties up to about 1500 kelvins, well above the required temperature for any superconductor anneals [65].

The only unanswered question concerns the mechanical rigidity of the conductor windings if the magnet were to be fabricated without vacuum impregnation. Any such magnet would probably require significantly more training (many rampings of the field to instability in order to force the windings into the mechanically most stable positions) than an epoxy impregnated magnet. This possibility is accounted for by setting the direct cost of performing a superconductor anneal of the magnet to be very much higher than it ordinarily would be. More on costing is given in a later chapter. Another possibility would be to use a polyimide in the vacuum impregnation instead of an epoxy. Polyimides have been shown to suffer little or no damage to a temperature of at least 630° C [66], which is about the same temperature as that of any possible superconductor anneal. If this is, indeed, the case, and if it can be shown that thermal cycling of the polyimide does not degrade its mechanical properties, then this option would be preferred. This possibility is accounted for by retaining the cost of impregnation in the total coil cost. Either option would need more experimental testing, obviously, since these annealing possibilities are only now beginning to be explored. This type of experimental program is definitely beyond the scope of the present work, however.

G. Radiation Damage Limits:

Since the purpose of this study is both to determine the effects of the radiation environment upon the superconducting magnet coils of a fusion reactor and subsequently to optimize the design of these coils based upon those effects, the proper specification of the radiation damage limits is essential. These limits can be set by the magnet designer, but how those limits are then accommodated is a matter of reactor design optimization. The ultimate optimization of a reactor design is, of course, not possible at the present time, nor will it ever be possible given two facts: technology is always advancing; and persons and groups always differ as to which variables in the reactor design are most important to optimize against. As an example of the complex nature of this problem consider a reactor that is designed, due to commercial viability considerations, to a much higher fluence than is allowed in the magnets. Should the magnets utilize only inorganic, ceramic insulators thus allowing high temperature superconductor anneals, or should the shield just be made larger? Both options present many advantages and disadvantages which must be weighed according to appropriate optimization criteria. These optimization issues will be dealt with in greater detail later in the paper.

Before asking how radiation limits can be accommodated, the limits must first be set. This is part of the job of the materials designer. Radiation limits must be set for all the materials from which the coils are fabricated: the structural alloy; the insulator; the stabilizer; the superconducting material. Moreover, the nuclear heating in the coil must be set low enough for adequate heat removal (which may, primarily be an economic constraint). Since the magnets are so far from the plasma, and the neutron flux is much less intense than in the front part of the blanket, there will be relatively few problems with the structural alloy of the coils. This has been demonstrated in

many studies, but especially with regard to the Incoloy alloys which have thermal coefficients of expansion particularly suited to minimizing the stresses in the cable due to cooldowns and warmups [67].

The radiation dose limit to the insulators depends upon the choice of insulator. This limit has been set in the ITER design to 5×10^9 rads (corresponding roughly to 5×10^{18} n/cm²) [6, 68, 69], although this particular limit has been subject to rather large debate and is likely to evolve through several other values before final specification. For ARIES-II/IV [70, 71] this limit has been set to 10^{11} rads (glass filled polyimide). The copper stabilizer damage limit is set by magnet protection considerations, which will be dealt with later in the paper. In order to keep the maximum hot spot temperature to 150 K, a limit of 5×10^{-4} dpa has been set in the ITER design. Although there was a discrepancy between this limit and the limit that had previously been adopted by the ITER blanket and shield group (which was 6×10^{-3} dpa, and which was pointed out to the ITER team first by this author), room temperature magnet annealing schedules have been adopted to easily remedy this problem.

In the ARIES-II/IV design, the copper damage has been set to a limit of 6×10^{-3} dpa (though this also may involve the same discrepancy pointed out in the ITER CDA design). The ITER CDA magnet group set the limit on the nuclear heating in the coils to 65 kW, and a peak heating of 2 mW/cc, while the ARIES group has set these limits to 50 kW and 2 mW/cc, respectively. These limits were set on the basis of reasonable cryogenic constraints as well as a low operating temperature, which impacts stability. Cryogenic constraints and considerations will be dealt with elsewhere in this paper. Finally, the fluence limit set in the ITER CDA and ARIES-II/IV designs for the Nb₃Sn superconductor is 1×10^{19} n/cm², with a recommendation in the ITER CDA design that this limit be halved if a ternary alloy is used. The limits to the superconductor also

Fig. 8: Neutron Spectrum (based upon both lethargy and damage energy) [6].

Fig. 9: Typical neutron energy spectrum at the TF coils of ITER (CDA) [69].

Fig. 10 : Comparison of the relative number of neutrons to produce equivalent damage to that produced by RTNS-II [6].

have been dealt with elsewhere in this paper. These are the limits to the coil that have been considered in the interpretation of *MagRad's* results (presented later in this paper) as well as the development of preliminary design conclusions. Again, it must be stressed that the method of handling any potential problems regarding these radiation damage limits will be the result of a reactor optimization strategy rather than a coil optimization strategy (neither strategy is independent of the other).

Two figures, Figs. 8 and 9, give the neutron spectrum which can be expected at the magnet coils. Fig. 8 gives the fusion spectrum at the coils in comparison to other typical spectrums which may be more familiar to the reader. Also given in this figure is the damage energy cross section of Nb. The damage energy criteria is used to compare the radiation effects (fluence) of different spectra. Shown in Fig. 9 is a typical neutron spectrum at the TF magnet coil case (this example happens to be that of the ITER CDA design). Based upon this damage energy criterion, Fig. 10 shows a comparison of the equivalent number of neutrons to produce the same damage energy for a number of neutron sources. In a more quantitative way, we can claim that the damage produced in Nb by about 4.3 ITER CDA (or ARIES) neutrons at the blanket is equivalent to the damage produced by 1 RTNS-II neutron [68]. With these types of conversions in mind, most data given in the literature can be properly interpreted on a more important scale, that is on the scale of the particular fusion device in question.

A final note concerning radiation damage is that not only are the limits mentioned above somewhat flexible and subject to both coil and reactor optimization, but also the final values for these radiation environment parameters are extremely design dependent. Since ITER is by far the front running candidate fusion device that may be constructed, the ITER TF coil has been selected as the most relevant and important coil to model and analyze. Additionally, it is the most thoroughly documented TF coil design. This, of course, has left this study open to the disadvantage that the

ITER design seems to be continually changing, both in respect to the coils and the reactor as a whole. One newer EDA design benchmark, for example, using a first wall/blanket/shield (FW/B/S) consisting of about 50% Li and 50% HT-9 and a water cooled double wall Inconel vacuum vessel (VV) with single size tungsten carbide (WC) balls, yields a harder spectrum at the coils than did the water cooled CDA design [6]. This, of course, changes the damage rate in the coils, and instead of designing to about 3.8 full power years (FPY) as in the CDA, this newer design only attains about 1.5 FPY. Since the flux has increased, the nuclear heating in the coils has increased, which implies at least a modification in the coil design.

The destabilizing effect of this kind of rapid redesign effort upon such an analysis as this one is obvious. However, one of the desired results of this thesis is the development of a tool whereby magnet design can be quickly and accurately optimized. The CDA base case is taken as the standard, unchanging design, and while we have endeavored to keep pace with the rapidly evolving EDA design in an effort to present important, meaningful, and relevant results to the fusion community this effort has been precluded by recent radical redesign efforts. It must be stressed that the goal of the thesis is not to assist in the design of ITER, but rather to develop a procedure for optimizing TF coil design such as in ITER. This procedure can also be taken as a convenient stepping stone to the ultimate objective of optimizing the magnet coils within a commercial fusion system, like ARIES. Tables 2 and 3 show some of the relevant differences between the ITER CDA, ITER EDA (a mid-1994 specification), ARIES-II, and ARIES-IV designs as far as the radiation history and environment are concerned.

Of particular note, it should be observed that the radiation damage and shielding performance characteristics of a typical experimental reactor, like ITER, are very different in some respects from those of a typical commercial power reactor, like

ARIES-II/IV. First, it must be noted that whereas actual design parameters are given for ITER, only limits are given for ARIES (since less comprehensive analysis has been performed on ARIES than on ITER). Several characteristics of the radiation environment in which the ARIES coils will operate are significantly more demanding than those in which the ITER coils will operate, such as the end-of-life damage to the copper stabilizer and insulation. The ITER designers have pursued a very conservative design with respect to insulation damage, while ARIES has not. Originally, the copper damage limits in both ITER and ARIES were 6×10^{-3} dpa, but it was demonstrated to the ITER designers that the magnet designers had, in fact, designed to a much lower limit. This limit was then accepted for the ITER design, with the understanding that periodic room temperature anneals of the coils would eliminate the need for extra shielding to meet the lower limit. On the other hand, it has apparently been assumed by the magnet designers in the ARIES studies that the magnets could be designed to accommodate the higher damage limit. Finally, the fluences achieved in the ARIES designs are significantly higher than those designed to in the ITER designs. This has a very significant impact upon the superconducting performance of the TF magnets, as will be demonstrated in the ARIES case studies section of the dissertation.

Table 2: Parameters of the ITER Radiation Environments [6].

	FW/B/S: VV:	CDA Li ₂ O/Water/316SS Water/316SS	EDA Li/HT-9 Water/Inconel/WC
Peak Nuclear Heating in Magnet (mW/cc)		1.4	3.1
Peak Fast Neutron Fluence in Magnet (10^{18} n/cm ²) @ 1 FPY		1.6	2.4
Peak Organic Insulator Dose (10^9 rads) @ 1 FPY		1.3	3.9
Peak Copper Damage in Magnet (10^{-4} dpa) @ 1 FPY		7.1	16
Peak Neutron Flux in Magnet (10^{11} n/cm ² ·s)		0.9	1.5
Peak Gamma Flux in Magnet (10^{10} gammas/cm ² ·s)		5.8	8.5
Total Nucleat Heating in Coils (16 coils for CDA, 24 for EDA) (kW)		47	45
Lifetime (FPY)		3.8	1.5

NOTES: FW refers to the composition of the first wall of the reactor,
B refers to the composition of the blanket, S refers to the composition
of the shield, and VV refers to the composition of the vacuum vessel.

Table 3: Limits for the ARIES Radiation Environments [71].

	FW/B/S: VV:	ARIES-II V/Tenelon/B ₄ C/Li Tenelon	ARIES-IV SiC/Li ₂ O/B ₄ C/Be Tenelon
Peak Nuclear Heating in Magnet (mW/cc)		2	2
Peak Fast Neutron Fluence in Magnet (10^{18} n/cm ²) @ 30 FPY		10	10
Peak Organic Insulator Dose (10^9 rads) @ 30 FPY		100	100
Peak Copper Damage in Magnet (10^{-4} dpa) @ 30 FPY		60	60
Lifetime		30	30

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CHAPTER 4: SUPERCONDUCTING MAGNET STABILITY CONSIDERATIONS

A. Stability at Zero Fluence:

Cable-in-conduit conductors (CICC) are gaining broader acceptance in cryogenic superconducting magnet designs. These conductors are made by twisting copper stabilized Nb₃Sn or NbTi superconductor wires into cables and enclosing them tightly in a conduit. Supercritical helium is forced through the interconnected spaces between the superconducting wires for cooling and stabilization. The work presented here represents the first step toward the goal of this thesis.

An important consideration in the design of a CICC is the maximum energy pulse that the conductor can absorb and still retain (or recover) its superconducting operating state, the coil's stability margin. The stability or energy margin has been determined experimentally for many CICC's. Definitive early experiments on model conductors were done by Lue, Miller, and Dresner [1-9]. Plots of the energy input to quench versus current typically display three stability regions for relatively short (this qualification will be explained below) conductors: high stability at low currents; low stability at high currents; and a steep transition at intermediate currents. The separation of the regions is understood in terms of differences in the heat transfer caused by induced flow. At low currents, strong energy pulses induce high helium velocities, and the conductor is said to be well-cooled, that is it makes efficient use of the helium enthalpy available to it by convective cooling. At high currents, weak energy pulses induce low helium velocities, and the conductor is said to be ill-cooled. The transition region can be multivalued with three changes between stable and unstable behavior at a

given current. The current at the intersection of the ill-cooled and transition regions is called the limiting current, I_{lim} , since it is desirable to limit currents in a magnet to less than I_{lim} and operate in the higher stability region.

This stability behavior has been predicted analytically. Dresner obtained a closed form solution for I_{lim} for conductors with initially stagnant helium by evaluating the convective heat transfer coefficient from the thermal expansion of the helium [4, 6, 8]. The quench occurs at the time when the transient heat transfer coefficient is minimum. Bottura, in his *HESTAB* code, expanded Dresner's work by including the effects of friction and forced flow [10]. Both analyses show agreement with experiments by Lue, et al. [8] near I_{lim} . E. Tada, et al. [11] solved the helium and conductor conservation equations by finite difference, obtaining agreement with their stability measurements. The present work reports the use of a new numerical analysis tool, the *CICC* computer code [12], for the study of a recent coil test and future coil designs where fusion relevant CICC were employed [13]. This code solves the conservation equations in the helium flow, conductor, and conduit.

The United States Demonstration Poloidal Coil (US-DPC) was built by the Massachusetts Institute of Technology and tested at the Japan Atomic Energy Research Institute in late 1990 as part of a cooperative fusion magnet development program [13]. Direct current testing to the 30 kA operating current (corresponding to 5.66 T at the inner turn of the winding pancake) resulted in no problems. However, the coil quenched unexpectedly during ac loss testing while attempting to reach 30 kA by ramping up in 1 second. To investigate this problem, the coil was tested to a range of currents by ramping up at increasing ramp rates until it quenched. All quenches occurred in pancake coil C (the middle coil of the experiment). The significant difference between pancake C and the other pancakes is that it has a larger helium fraction, because it is the only coil with no imbedded heater. It is thought that ac eddy

current heating losses are partially responsible for the quenches, since these losses increase with increasing ramp rate. The exact nature of the destabilizing heat pulse in the experiment has not been determined and is still under investigation. Possible sources are ac heating, magnetic flux jumping, mechanical work (e.g. interstrand friction), or some combination. A plot of ramp rate versus current does have the general shape of a stability curve (a curve with a sharp transition or multivalued region) with I_{lim} in the range of 20-26 kA, suggesting that the destabilizing energy release is related to the ramp rate. This initiated a detailed computational study into the stability behavior of the coil.

The cross section of the US-DPC conductor is shown in Fig. 11. The basic CICC conductor geometry is modified in this instance by adding an outer conduit, providing an additional helium path between the two conduits. The stability curves, calculated over a range of possible US-DPC parameters are shown in Fig. 12. The helium enters at 4.5 K and 0.6 MPa, and exits at 0.5 MPa. Since the outer helium flow provides a heat sink, a constant temperature of 4.5 K boundary condition is used outside the inner conduit. The calculations show that only 10% of the generated heat is removed by this outer helium flow during the first 0.1 s after heat pulse initiation, when stability is determined. Helium in the outer flow path contributes little to the stability of the conductor but may be useful for steady state heat removal. In the simulation, the square heat pulse is imposed at the middle of the inner coil turn where the magnetic field is highest and a quench is most likely to occur. The parameters chosen for the different stability curves are: helium fraction, f_{He} (representing conditions with and without the heater; heat pulse duration, τ ; and heated zone length, l). The choice of τ and l reflect uncertainties regarding the nature of the heat pulse. The stability curves show that the US-DPC performance is not totally unexpected. There is clearly a limiting current in the range of 21-26 kA. The quenches occur in

pancake C, since the I_{lim} is lower due to the greater f_{He} . A significant contributor to the depressed I_{lim} is the poor electrical conductivity of the copper stabilizer which was used. The effect of changing the stabilizer resistivity, ρ , is investigated in Fig. 13. These curves show that if a copper is used that has 4.63 times less residual resistivity, ρ_0 , than the copper assumed in the ITER design, I_{lim} is increased by 8 kA, or roughly one-third.

Fig. 11: US-DPC conductor geometry [14].

Fig. 12: CICC Simulation of US-DPC stability margins [14].

Fig. 13: Effect of copper resistivity upon stability [14].

Dresner predicts how I_{lim} should vary with changes in conductor and heat pulse parameters [6]. Based on experimental data, Miller and Lue [[15] changed the exponents of τ and l , respectively, from $1/5$ and $2/5$ to $1/15$ and $2/15$. The modified Dresner equation is

$$J_{lim} \propto \left[\frac{f_{Cu} \times (1 - f_{co})}{f_{co}} \right]^{1/2} \times \left[\frac{(T_c - T_b)^{1/2} \times l^{2/15}}{\rho^{1/2} \times P_{HT} \times \tau^{1/15}} \right], \quad (14)$$

$$I_{lim} \propto \left[\frac{A_{Cu} \times A_{He}}{A_{co}} \right]^{1/2} \times \left[\frac{(T_c - T_b)^{1/2} \times l^{2/15}}{\rho^{1/2} \times P_{HT} \times \tau^{1/15}} \right]. \quad (15)$$

where,

J_{lim} = Limiting current density in the conductor

= I_{lim}/A_{co}

A_{co} = Area of the conductor

A_{He} = Area of the helium

A_{Cu} = Area of the copper

f_{Cu} = Volume fraction of the copper in the conductor

f_{co} = Volume fraction of the conductor in the cable space

f_{He} = Void fraction, Volume fraction of helium in the cable space, $1 - f_{co}$

T_{cr} = Critical temperature of the conductor

T = Helium temperature

ρ = Magnetoresistivity of the copper

τ = Heat pulse duration

l = Heated zone length

and, P_{HT} = Heat transfer wetted perimeter.

To first order, A_{He} enters into the conservation equations only through the hydraulic diameter for friction, $D_{h,frict}$, and the heat transfer wetted perimeter, P_{HT} ,

$$P_{HT} = \frac{4 \times A_{He}}{D_{h,frict}}. \quad (16)$$

Also to first order, $D_{h,frict}$ can be expressed in terms of f_{He} and the wire diameter, d_{wire} , by,

$$D_{h,frict} = \frac{f_{He}}{1 - f_{He}} \times d_{wire}, \quad (17)$$

Moreover, it can be shown (and will be later in the proposal) that I_{lim} is inversely proportional to the hydraulic diameter for friction. I_{lim} can, therefore, be increased by either decreasing the void fraction or the wire diameter. The decrease in f_{He} from 45% to 38% was accomplished by changing A_{He} . If Eqn. (14) is combined with Eqn. (16), the effect on I_{lim} becomes,

$$I_{lim} \propto \frac{1}{A_{He}^{1/2}}, \quad (18)$$

The change in I_{lim} as calculated by Eqn. (14) and as read from the stability curves in Figs. 12 and 13 for the different curve parameters is shown in Table 4. The reference case is $f_{He} = 45\%$, $\tau = 10$ ms, and $l = 20$ cm. Table 4 lists the I_{lim} ratio resulting from each curve parameter ratio (with respect to the reference case).

Table 4: US-DPC limiting-current-ratio comparison.

<u>Parameter</u>	Parameter Ratio <u>Parm/Parm_{ref}</u>	Dresner Prediction <u>Ilim/Ilim_{ref}</u>	CICC Prediction <u>Ilim/Ilim_{ref}</u>
Hydraulic Diameter: $A_{He}/A_{He,ref}$	0.748	1.156	1.146
Heating Duration: τ/τ_{ref}	2	0.955	0.963
Heated Length: $l_0/l_{0,ref}$	2	1.097	1.146
Copper Resistivity: $\rho_0/\rho_{0,ref}$	0.216	2.152	1.352

The calculated changes for τ , l , and A_{He} show good agreement with Eqn. (16). This verifies the validity of the change to the smaller exponents for τ and l (at least for short coils). The calculated change for ρ is somewhat less than given in Eqn. (16) due to the increasing field with increasing I_{lim} , which affects both T_c and ρ .

Detailed output from the computational model allows insights into the thermal-hydraulic mechanisms that determine a quench initiation. Each of the three stability regions is characterized by its own specific mechanisms, but some general comments can be made. The heat pulse is imposed in the middle of the first or inlet turn, which is only 1.637 meters long. The 0.1 MPa pressure drop (with 45% void) results in a steady state flow of 60.1 kg/m²s (5.51 g/s). The inlet end of the coil is close enough for the heat pulse to cause a flow reversal, with helium discharging from the inlet. This is an important cooling mechanism since the initial transient conduction decays, and the heat transfer becomes dominated by forced convection after about 10 ms. The timing of the minimum heat transfer coefficient, which occurs when the helium flow is near zero, determines the location of the quench initiation. With these general comments in mind, consider the mechanisms in each of the three stability regions.

In the low stability, ill-cooled region (currents greater than I_{lim} in Fig. 12), the stability mechanism is simple. A marginal quench is initiated when the heat pulse is strong enough to heat the conductor to T_c (not the current sharing temperature, T_{cs} , as in Bottura's formulation) at the end of the pulse (as assumed by Dresner). This occurs at a location near the middle of the heated zone, where the heat transfer coefficient is minimum and the flow is near zero at the end of the pulse. Because the flow is near zero, there are significant conduction and laminar flow contributions; and the Dittus-Boelter [16] turbulent flow contribution is less than that assumed by Dresner. This is why the valid exponents of τ and l are smaller [15]. When a quench is initiated in this region, the pulse heating rate is always equal to or less than the electrical resistance heating rate. After the pulse, the induced velocities are too low to recover the quench. This is why the region is termed ill-cooled. At I_{lim} , the pulse heating rate equals the electrical resistance heating rate. In Fig. 12, the decrease in stability with increasing current in the ill-cooled region is the result of the increasing magnetic field and resulting decreasing T_c . The only parameter having a significant influence on the energy margin in this region is the pulse duration. Increasing the pulse duration lowers the pulse heating rate. This lowers the conductor-to-helium temperature difference more than the heat transfer coefficient, producing a higher energy margin.

The key component of the transition region mechanism is a strong flow reversal after the pulse. This provides the high heat transfer necessary to recover a quench. To create this flow reversal, the heating rate during the pulse must be greater than the resistance heating rate. This causes a step decrease in heating. In the transition region, not only is the pulse heating rate higher, but the resistance spike at the end of the pulse is longer than it is at I_{lim} . The increased convection from the higher energy pulse and the lengthening resistance heating spike is reflected in the steepness of the transition region. At 22 kA, the increase in cooling capability is sufficient to create a multiple

stability. The top of the transition region is typified by the resistance heating spike spreading over the downstream of the middle of the heated zone, where the flow is near zero at the end of the pulse.

At currents below 20 kA, in the well cooled region, the quench mechanism changes. Quench initiation shifts from near the middle of the heated zone to the downstream end. This change is due to the significant heating of the helium. Initially the induced velocities and heat transfer coefficients are high, allowing the high heat pulse to heat the helium. Then the flow decays to near zero, producing low heat transfer coefficients, allowing the resistance heating to heat the conductor. "Well-cooled" does not mean that the helium and conductor temperatures are the same. Temperature differences of 7 K and 4 K during and after the pulse are typical. This quench mechanism at the end of heated zone allows a smaller energy margin than does the mechanism in the transition region near the middle of the heated zone. Therefore the stability curve levels off in the well-cooled region (Fig. 12). Decreasing helium fraction decreases stability in the well-cooled region, since less helium heat capacity is available. Note in Fig. 12 that the 45% and 38% helium curves cross between I_{lim} and the well-cooled region. Decreasing helium fraction is beneficial in the transition region near I_{lim} , but is detrimental in the well-cooled region. Increasing heated length increases stability due to greater velocities (heat transfer coefficients) during the pulse.

Fig. 14: Numerically predicted stability margin curve for ITER CDA [17].

During the 10 ms pulse duration, the helium travels only 4 cm so the additional heat added to the helium by increasing the heated length from 20 cm to 40 cm increases the helium temperature only slightly. Increasing the pulse duration decreases the stability in the well-cooled region due to the lower velocities and heat transfer coefficients during the pulse. Again the stability curves cross, reflecting the opposite effects in the well-cooled and ill-cooled regions. A decrease in the copper resistivity results in increased stability throughout the well-cooled region due to the lower resistance heating.

The situation in long coils is quite different than that in short coils. The US-DPC coil represents a typical short coil, while the ITER TF coils would represent long coils. It is just these such long coils that will be utilized in magnetic fusion devices,

and as such require some fair degree of predictive analysis. What is meant by classifying some coils as long and some as short is just this: in long coils the quench does not receive any information about the end of the coil within the initial 100 ms period significant in determining stability. In effect, then, the heated zone does not "see" the end of the coil, unlike in short coils (such as the US-DPC) where the heated zone "sees" the end of the coil in that the helium is allowed to be expelled out the end rather than simply pressurizing (via the pressure pulse which is created in the heated helium). Toward the end of developing the capability of predictively analyzing long coils as well as short coils, CICC is next used to analyze the ITER CDA TF coils.

Fig. 13 shows the calculated stability curves for the ITER CDA TF coils. The conductor is a CICC design similar to that used in the US-DPC experiment. The quench is initiated in the middle of the inlet first turn where the maximum field and temperature occur during the operating cycle. In contrast to the US-DPC, the inlet ITER TF turn is 40 meters long, so as claimed above the quench does not "see" the end of the coil during the initial 100 ms period significant in determining stability. Vertical lines in Figs. 12 and 14 also show I_{lim} as calculated using Dresner's equation (Eqns. 17 and 18). These values of I_{lim} are calculated using helium and conductor properties and geometry. Stability curves are calculated for heated lengths of 20 and 200 cm, and for pulse durations of 10 and 20 ms.

As shown also in Fig. 14, increasing the pulse heated length from 20 cm to 200 cm decreases the stability slightly. This is in contrast to the US-DPC stability curve (Fig. 12), which shows an increase in stability due to increasing the heated length from 20 cm to 40 cm. The long coil length of the ITER TF suppresses the induced convective helium cooling. Dresner predicts a large increase in I_{lim} when increasing the heated length from 20 to 200 cm (Fig. 14), but his equations assume that there is no pressure rise in the heated helium.

The stability curves in Fig. 14 show that an increase in pulse duration from 10 ms to 20 ms causes a large decrease in energy margin. The US-DPC curves in Fig. 12 show a much smaller decrease. At high energy pulses, the long ITER TF coil length allows high heat transfer coefficients to be maintained to only about 10 ms. There is no coil end available to relieve the helium pressure pulse, thus the induced helium flows decrease after 10 ms. Also, the transient, conductive heat transfer coefficient rapidly diminishes after 10 ms. The heat transfer coefficient remains high for the duration of the 10 ms pulse, but it decreases after 10 ms for the 20 ms pulse. This prediction stands in contradiction to the prevalent assumption of most designers that the details of the pre-quench event are relatively unimportant to the conductor's stability margin.

Since the long ITER TF coil inhibits the sustaining of helium convection after the heat pulse, the stability curve (Fig. 14) does not have separate well-cooled, ill-cooled, and transition regions as found in the US-DPC coil (Fig. 12). As discussed previously, helium convection after the heat pulse is necessary for the formation of the transition region. The heat transfer phenomenon exhibited by the ITER TF coil is that of the well-cooled region at all currents less than that of the limiting current. The well-cooled region is characterized by being well-cooled during the heat pulse, allowing efficient transfer of heat from the conductor to the helium. The quench is then initiated after the heat pulse as the heated helium and the low heat transfer coefficients cause increased conductor temperatures. Typically, quench initiation occurs near the ends of the heated length, because the highest helium temperatures occur there.

Due to the high field (11.2 T at the 37.89 kA design current for ITER CDA) and the 3.6 K temperature margin, the ill-cooled region for the ITER TF coil is almost nonexistent. The limiting current at the beginning of the ill-cooled region occurs when the initiating pulse heating rate equals the electrical resistance heating rate (as already mentioned). For the 20 cm heated length, the value of I_{lim} agrees with that calculated

using Dresner's equation (Eqns. 16 and 17). The calculation of I_{lim} for the 200 cm heated length, however, does not agree, due to the friction suppressed helium flow.

These calculations show that the ITER CDA TF conductor experiences reduced energy margins for heat pulses with a duration of between 10 and 100 ms. The coil should be designed to avoid exposing the conductor to heat pulse disturbances of this time duration. Disturbances caused by flux jumping are in the millisecond range. Long duration, interwire, frictional heating can be eliminated by coil designs that minimize conductor movement, though designing for this, even if it is fabricationally feasible (an unproven point) may not assure that such a pulse of the critical duration will not occur. An electromagnetic quench of the plasma can result in heating in the critical range of tens of milliseconds. However, almost all of this heat can be removed and the duration increased to 100 ms or longer by carefully designing the first wall, blanket, and magnet cases. Moreover, magnet cases absorb electromagnetic energy in the form of eddy currents, so increasing the thickness will increase the duration of the heat pulse experienced by the conductor. Additional heat removal can be accomplished by active magnet case cooling.

Cooling is improved by conductor designs allowing increased induced convection. Design considerations favoring increased convection are those that lessen the flow frictional resistance: short flow length from a quench location to a vent; increased hydraulic diameter; and increased helium fraction. Design trade-offs are clearly required, since shortening the flow length may be difficult (and in any case would not affect the stability until the flow path approaches "shortness" as defined qualitatively above), and increasing the hydraulic diameter and the helium fraction may be contrary to what is required by heat transfer and heat capacity considerations in the steady state.

It can be concluded, then, that the stability of long conductors, such as the ITER or ARIES TF coils, is decreased due to the difficulty of sustaining an induced convective helium flow if a conductor end is not close enough to relieve the helium pressures generated by the initiating heat pulse. Well-cooled stability is decreased if the heat transfer coefficient decreases before the end of the heat pulse. The transition region is nonexistent due to the decreased helium convection after the heat pulse. For the ITER CDA TF coil, the energy margin has been calculated to be about 900 mJ/cc at the 37.89 kA operating point if the initiating heat pulse is 10 ms. If the heat pulse is 20 ms, the energy margin is decreased dramatically to about 500 mJ/cc. If the energy of the heat pulse is spread over 100 ms or longer, the energy margin increases, because the steady state convective heat transfer can handle the heat load. The heated coil length has little effect upon the stability of long conductors. Optimizing a coil design for transient stability requires trade-offs, as stated earlier, that consider the complex interactions between all of the thermofluid phenomena: fluid friction; heat transfer; and heat capacity. Such optimizations rightly fall under the general category of local optimizations, rather than global, reactor-wide optimizations. As such, these optimizations can be carried out semi-independently of the reactor optimizations, as opposed to, say, annealing studies. These optimizations will require trade-offs between coil protection, stability, and steady state heat removal. The procedure for such an optimization will be discussed later in this paper.

B. Stability Curve Geometry:

As can be readily seen from the previously presented ITER CDA CICC simulations in Fig. 14 [14, 17] the stability with respect to coil current is a curve with nearly constant slope. Recall that the stability of a coil is the maximum amount of energy the conductor can absorb without quenching. Moreover, as the limit is approached of long coil lengths being heated in the prequench phase, the slope of the curve (stability margin versus coil current) becomes even more nearly constant. The flowpaths involved for the ITER CDA simulations have already been seen to be very large compared even to the longer heated lengths. That the slope of this curve is very nearly constant is not a surprising result. This result stems from the fact that, in coils with long flowpaths, the heating induced convective heat transfer which was so evident (the double stability phenomenon) in the shorter coils (such as the US-DPC) is now quite suppressed by the extremely large mass of helium on either side of the prequench heated region. If the heated region were to occur very close to one of the ends of the flowpath we would expect helium expulsion out that end giving rise to a much higher and more sustained induced convective cooling, and thus the stability curve would be characterized by much more structure and not have a constant slope (similar, again, to the US-DPC simulations). However, if the heated region were to occur near the ends of the flowpath, it would no longer be at the point of lowest temperature margin, and thus the stability curve, while having structure, would be shifted to higher energies. This effect of the suppression of the convective cooling in the long ITER coil flowpaths does not, obviously, vary with fluence. The conclusion of the above argument then is that the stability curve is very well approximated as having a constant slope with respect to current both at start-up and all subsequent times.

Though the above argument shows that it is a very reasonable assumption that the stability curve will have a constant slope at all fluences, it does not rule out the possibility of the value of that constant slope changing with fluence (but it will remain constant with respect to current at any given fluence). In fact, as the fluence increases, it is safe to assume that the slope will be changing. In order to estimate this change in the constant slope of the stability curve, an expression explicitly relating the stability margin to the operating current is required. The Dresner formulation, upon which both the earlier *CICC* simulations and the later *MagRad* simulations were based, does not explicitly relate these two variables. A full *CICC* simulation series must be run for each set of parameters as they change with fluence. This would be extremely time consuming (somewhere in the vicinity of 30 cpu hours on the Cray machines would be required per stability curve, with stability curves being generated at each fluence increase). The next recourse, using the Dresner formulation for the limiting current, is to assume that, to first order in the region of the stability curve near the limiting current, the slope of the curve remains constant with fluence. This is what is done in *MagRad*.

MagRad uses the geometry of the stability curve to backsolve for the stability margin at a given coil current from first solving for the limiting current. The limiting current at any fluence may be found by using Dresner's limiting current expression, given both the availability and validity of the expressions used for the fluence dependent variables such as the copper resistivity, the critical temperature, and the upper critical field. The geometry of the stability curve of a typical long *CICC* coil is shown in Fig. 15. From the previous studies in which the stability of *CICCs* was simulated it can be shown that the value of the limiting current as given by the Dresner equation was reached in the simulations at an energy margin of about 100 mJ/cc, which is a reasonable value for the energy margin in the ill-cooled regime. One way to compensate for this small discrepancy is simply to add this 100 mJ/cc offset to the

value predicted by Dresner. The new value would correspond very closely to that value which would have been obtained through a full *CICC* simulation set. Thus,

$$\Delta H(I_{\text{lim}}(E_d)) = m \times (I_{\text{op}} - I_{\text{lim}}(E_d)) + 100 \text{ mJ/cc} . \quad (19)$$

Fig. 15: Example of stability curve geometry.

where $\Delta H(I_{\text{lim}}(E_d))$ is the predicted stability margin (in mJ/cc) as a function of limiting current (which is a function of fluence), m is the slope of the stability curve (in mJ/(kA-cc)), I_{op} is the operating current (in kA), and $I_{\text{lim}}(E_d)$ is the fluence-dependent limiting current (also in kA). It should furthermore be noted that, due to the assumption in the Dresner formulation that the pressure of the heated zone causes helium expulsion from that zone, which is certainly not always the case in long conductors (such as ITER and ARIES), the Dresner expression for the limiting current will not hold at long heated

lengths (like 2 m). However, in the *CICC* simulations it was found that the stability curves of both long heated lengths and short heated lengths fell very nearly on the same line of constant slope (the shorter heated lengths had slightly more structure to them, just as Dresner would predict). Due to this limitation in the Dresner formulation, we assume in *MagRad* that all heated zones are nearly 20 cm in length, while fully recognizing that this assumption is only for mathematical purposes and that, in reality, all heated zone lengths have very nearly the same stability, as shown in the *CICC* simulations.

Having claimed that what is needed in this type of study is an explicit relation between the stability margin and the operating current, it now is proper to examine the validity of one such expression which has just been recently put forward, [18, 19]

$$J_{op} \leq \cos(\vartheta) \times \sqrt{f_{cu} \times (f_{cu} + f_{sc}) \times \frac{4 \times K_p \times h \times (T_c - T_{op})}{\rho \times d_{wire}}}, \quad (20)$$

$$\Delta E = \frac{f_{He}}{f_{\infty}} \times \left(1 - \frac{J_{op}}{\cos(\vartheta) \times J_c \times f_{sc}} \right) \times \rho_{He} \times C_p \times (T_c - T_{op}). \quad (21)$$

The primary value of Bottura's entire formulation in the above referenced paper is its ability to predict a good starting point for coil design in terms of design specified parameters such as copper fraction, helium fraction, etc. However, what is desired is to determine its value with respect particularly to stability (the above equation), and the fluence effects upon stability. At zero fluence the value of the stability margin, as given by using the limiting current formulation, can be up to about 30% lower than that predicted by using Bottura's prediction. This depends upon what is chosen as the assumed critical current of the conductor, that of the EDA/HP-I (higher value) or that of the EDA/HP-II (lower value) as specified recently by Montgomery [20]. For the sake

of comparison, the ITER CDA base case is close in specification to the EDA/HP-I conductor; the EDA/HP-II conductor is to be used only in the low field portions of ITER, if at all. It is to be expected that the formulation which uses limiting current as its basis will give lower stability margins, since Bottura's formulation involves the assumption that all of the enthalpy in the helium is utilized at 100% efficiency in transiently removing the heat. This, as he later points out in the same paper, is certainly an optimistic assumption. In reality, the amount of helium which can be efficiently used in carrying away heat depends also upon the heat transfer coefficient. It is this heat transfer coefficient which appears in Dresner's formulation, and which gives this limiting current formulation its advantage in yielding more realistic results, though it is certainly a more complicated method of determining the stability margin than Bottura's. It is interesting to note that this high value for the stability margin given by Bottura's formulation is nearly the value which is given by *MagRad* using a heating duration of about 10 ms. In our studies, however, we have used a heating duration of about 20 ms, this value being the worse of the two according to our previous stability simulations. It should also be noted that those previous studies show a significant (nearly a factor of two) difference in the stability margin between the two different heating duration cases. This fact demonstrates one of the shortcomings of the Bottura formulation for energy margin. Otherwise, Bottura has a good basis for coil optimization, given that certain weaknesses are noted, and some of his work will be modified to include fluence effects and incorporated into a coil optimization scheme.

Of greatest interest concerning Bottura's formulation must be its handling of radiation effects. It can be seen from inspection of Eqns. 20 and 21 that as fluence increases several parameters are effected: the maximum operating current density will increase slightly for low to moderate fluences, due to the establishment of a new operating point (although the critical temperature is decreasing); the critical current

increases considerably through a maximum; and the critical temperature is decreasing slowly. The outcome of these various effects is that the stability margin predicted by Bottura's formulation will decrease in much the same fashion as the stability margin predicted using the limiting current formulation, mirroring it with fluence though at somewhat higher absolute values of energy density. Moreover, it is also clear upon inspection that the slope of the stability curve will decrease slightly with fluence. These results, of course, agree with the results given by *MagRad*, even though *MagRad* uses a fluence-independent stability curve slope. This fact increases confidence in the soundness of the basic approach to stability in this work. One would naturally expect that, with the copper resistivity increasing and the critical temperature decreasing, the stability margin would decrease even though the critical current is increasing. In fact, this is what *MagRad* predicts, with the possibility of an initial increase depending upon the conductor parameters and expressions for fluence effects. For the reason that Bottura's formulation does not take into account any heat transfer characteristics, one would conclude that it is best not to use this formulation as the basis for *MagRad* (though it has been put into the code for comparison purposes). This does not, in any way, detract from the merit of either Eqn. 20 or 21 in their primary functional use, i.e. the initial design of a conductor.

Finally, it should be added that the heating duration is treated in *MagRad* by use of a rather empirical formula. It was shown in the previous *CICC* simulations that stability is quite dependent upon the value chosen for the duration of the heat pulse. Not only were the values of the stability margin decreased at every coil current, but also it was shown that there is a decrease in the slope of the stability curve. This slope can be expressed empirically as,

$$m = \gamma \times \tau + D. \quad (22)$$

where m is the slope, τ is the heating duration, γ was estimated to be 6490 (mJ·s)/(kA·cc), and D was estimated to be -180.9 mJ/(kA·cc). This expression for the slope of the stability curve is used in *MagRad* as a geometric parameter in the determination of the stability margin geometrically from the curve as explained above.

C. Coil Geometries:

It is to be expected that the geometry of the cable space will play a major role in the determination of the stability of the magnet. Obviously, the simplest geometry to analyze is one which has the strands cabled throughout the entire cable space with interstitial forced flow helium as shown for the US-DPC in Fig. 16. This geometry (Fig. 16) has no additional heat sinks which must be modelled, and the Dresner formulation may be directly applied to this geometry. However, a geometry with a core heat sink running down the center of the cable space has been recently designed (Fig. 15), and seems to have been the lead candidate for the base case design of the ITER EDA (the ARIES studies do not have the level of detail in them required to make determination concerning the geometry of the cable space). This lead ITER EDA design was questioned by many magnet designers, and has been consequently in peril of being abandoned. It is not at all clear how the stability of a conductor employing the EDA geometry will be affected by the central flowpath. The prediction of the stability of such a coil, will now depend more heavily upon the successful modelling of the cable space, in particular this extra heat sink. In order to more accurately determine the effect of this extra heat sink, a strategy has been developed in the attempt to successfully predict the stability of this new design. This attempt has been made in order to incorporate into *MagRad* the capability of handling such a design. It was first attempted to bracket the true stability margin between what can be shown to be worse than reality and what can be shown to be better than reality. Some of the basic input geometry parameters are compared in Table 5.

Fig. 16: Comparison of CICC geometries (top - ITER CDA, bottom - ITER EDA).

Table 5: Coil geometry comparison of the ITER EDA and CDA (after [20-22]).

Parameter	ITER/CDA	ITER/EDA
Number of Strands	675	1350
Strand Diameter (mm)	0.851	0.78
Cu:non-Cu Ratio	2.16	1.5
Maximum Magnetic Field (T)	11.2	13
Operating Current (kA)	37.89	43
Critical Current Density (A/sqmm)	> 700	> 700
I _{op} /I _c	< 0.6	< 0.6
I _{op} /I _{lim}	< 0.9	< 0.9
Copper Cross Sectional Area (sqmm)	262	387
Helium Cross Sectional Area (sqmm)	228	450
in Core Region (sqmm)	0	79
S/C Cross Sectional Area (sqmm)	121	258
Total Cross Sectional Area (sqmm)	611	1095
Copper Fraction	0.68	0.60
Void Fraction (in Annulus)	0.37	0.37
Hydraulic Diameter for Convection (mm)	1.80	2.76
Hydraulic Diameter for Friction (mm)	0.51	0.54

First, it must be noted that the stability margin is, to some power, inversely proportional to the hydraulic diameter for friction. To see this consider the following set of equations,

$$I_{\text{lim}} = \left(\frac{f_{\text{Cu}} \times A_{\text{cc}} \times P_{\text{HT}} \times h}{\rho} \right), \quad (23)$$

$$h = \frac{k_{\text{He}}}{D_{\text{h,fric}}} \times Nu \quad \text{and} \quad P_{\text{HT}} = \frac{4 \times A_{\text{He}}}{D_{\text{h,fric}}}, \quad (24)$$

$$Nu = C \times Re^{0.8} \times Pr^{0.4} \times \left(\frac{T_b}{T_c} \right)^{0.716}, \quad (25)$$

$$Re = \frac{\rho_{He} \times D_{h,frict} \times v_{He}}{\mu_{He}}, \quad (26)$$

$$v_{He} = 0.5 \times \frac{P_{irr} \times l \times \beta_{He} \times \dot{q}_{wall}}{f_{He} \times A_{sr} \times C_p \times \rho_{He}}, \quad (27)$$

where k_{He} is the thermal conductivity of the helium, Nu is the Nusselt number of the flow, Re is the Reynold's number of the flow, Pr is the Prandtl number of the helium, C is a numerical constant, μ_{He} is the viscosity of the helium, β is the thermal expansion coefficient of the helium, $\dot{q}_{wall}$ is the heat flux of the perturbing event, and A_{sr} is the total cross sectional area of the strand region. If one notes that the helium expulsion velocity goes inversely as $D_{h,frict}$, it follows that both the Reynold's number and the Nusselt number of the flow are independent of $D_{h,frict}$. This leaves the limiting current to be inversely proportional to the hydraulic diameter for friction.

It is clear from the above arguments that as the hydraulic diameter is decreased, the stability margin of the conductor (which is proportional to the limiting current) will increase, since both the ITER CDA and EDA coils are long, convective-flow-restricting coils. Thus, it would be wrong to say that the best case for a new design, in which full use is made of the helium, would be if the extra helium were modelled as evenly distributed among the strands (thus increasing the hydraulic diameter). In fact, this would be worse than the case which was chosen to represent the lower bound of the stability margin. The case that was chosen to be the lower bound case is one in which the entire cable space is modelled without the heat sink, but which has the same void fraction as was in the annular region before the void was removed from the model. This model results in a slightly smaller cable space, but the same small hydraulic

diameter as was originally in the strand (annular) region of the cable space. In order to model the upper bound case, it was assumed that full use is made of the heat sink,

$$Q_{Total} = \sum_{A_{sr}} h_{sr} (P_{HT,sr}, v_{He,sr}) \times A_{sr} \times \Delta T_{sr} + \sum_{A_{core}} h_{core} (P_{HT,core}, v_{He,core}) \times A_{core} \times \Delta T_{core}, \quad (28)$$

where Q_{Total} is the total heat transfer to the helium, h_{sr} is the heat transfer coefficient over the strand region, $P_{HT,sr}$ is the wetted perimeter in the strand region, $v_{He,sr}$ is the helium velocity in the strand region, A_{cs} is the total cable space cross-sectional area, ΔT_{sr} is the temperature drop across the strand region, h_{core} is the heat transfer coefficient in the core region, $P_{HT,core}$ is the wetted perimeter of the core region, $v_{He,core}$ is the helium velocity in the core region, A_{core} is the cross-sectional area of the core region, and ΔT_{core} is the temperature drop across the core region.

However, to put this model into *MagRad*, which uses the Dresner formulation for the limiting current, another approximation had to be made: across the cross section of the conductor, the temperature will follow a roughly linear profile. This assumption was made in order to obtain an estimate of the overall heat transfer coefficient, which is much easier to handle within *MagRad* than is a transient energy balance. This type of energy balance, though, is just the type of equation which would be necessary if we were to numerically simulate the quench using CICC. Thus, the equation which we used to model the upper bound on stability for this conductor was based upon a simple energy balance with a weighted average total heat transfer coefficient,

$$Q_{Total} = h_{Total} \times A_{cs} \times \Delta T_{cs}, \quad (29)$$

$$h_{Total} = \frac{h_{Dresner}^{sr} \times (A_{cs} - A_{core}) \times \Delta T_{sr}}{A_{cs} \times \Delta T_{cs}} + \frac{h_{D-B}^{core} \times A_{core} \times \Delta T_{core}}{A_{cs} \times \Delta T_{cs}}. \quad (30)$$

where h_{Total} is the weighted average heat transfer coefficient across the entire cable space, ΔT_{cs} is the temperature drop across the cable space, h_{Dresner} is the heat transfer coefficient predicted in the strand region by helium expulsion heat transfer, and $h_{\text{D-B}}$ is the heat transfer coefficient of the core region, given steady state flow conditions. It can be seen that a linear temperature profile will give higher values for the effective heat transfer coefficient (and thus stability margin) than a more realistic temperature profile which would probably look more parabolic, thus weighting the core heat transfer term less. In this equation then,

$$\Delta T_{\text{core}} = \frac{r_{\text{core}}}{R} \times (T_c - T_b), \quad (31)$$

$$\Delta T_s = T_c - \frac{r_{\text{core}}}{R} \times (T_c - T_b). \quad (32)$$

where r_{core} is the radius of the core region and R is the radius of the cable space. The critical temperature, T_c , is used in these equations, since it is at the critical temperature that all the current is passing through the copper, as is assumed in the Dresner limiting current formulation. This temperature is assumed to occur at the plasma-facing side of the innermost turn, as would be the expected position of lowest stability. In determining the effective heat transfer coefficient, then, all necessary variables are known. This model provided the upper bounding stability margin for the EDA-type conductor design.

Upon further reflection, however, it seems that it would be much more accurate to adopt the lower bound as a close approximation to reality. There are several reasons for this. The first reason is that one would be then left with a conservative design, which is an engineering advantage. Another reason, however, is that each of the

subcables (six in the case of ITER EDA) are wrapped with a very thin sheet of metal foil in order to diminish the chances for cable damage in the final steps of assembly. This metal foil will present an impassible barrier to the flow of core helium to the subcables, where instabilities develop. Moreover, even if this foil were to have holes in it designed to allow this type of flow from the core to the subcables, the timescale in which this would have to happen is much longer than the transient stability timescale. Earlier it was determined, using the code *CICC*, that the temperature differences between the strands and the local helium surrounding the strands during and after the destabilizing event are on the order of 4-7 K. The temperature difference between the strands and the core helium will be somewhat higher, separated not only by the foil but also the spring-like central helix, which itself has a thickness on the order of several millimeters. It is quite difficult to believe that there will be any kind of decent communication between outlying strands of a subcable and the central core flowpath. The strands farthest from the core flowpath will have the highest average temperature, hence being the least stable, and they will also be the strands least in communication with the core flowpath. Also, it is very difficult to predict how this core flowpath will effect the pressure profiles during the destabilizing event.

Given the previous considerations it seems most prudent to accept the worst heat transfer case as the case which best describes reality. This is what has been done in *MagRad*. Additionally, it would seem very desirable to do experimentation on this type of conductor geometry, both for short lengths and very long lengths. This is not to say that the core flowpath represents an unappealing geometry. Its appeal is based on the advantage of reducing the average steady state operating temperature of the conductor (which impacts stability - this effect is incorporated into *MagRad*).

D. Stability v. Fluence Results:

MagRad has been used to predict the stability of two specific coil designs as benchmark cases. Two different criteria for stability have been used: the stability margin as explained above, and the temperature margin. The temperature margin is the difference between the current sharing temperature at the operating current and field, and the actual operating temperature of the coil. As such, one would expect the temperature margin to qualitatively behave similarly to the stability margin (or limiting current) with a few significant differences. Whereas the stability margin, as given by Dresner, is quite dependent upon the thermal-hydraulic geometry of the case, the temperature margin is heavily dependent upon the critical parameters of the superconductor. This fundamental difference can have a significant effect upon the interpretation of the results, as will be shown below. In fact, in designing the coil, both stability criteria must be considered to insure that true stability will be achieved at a given fluence. In this way, the designer can make use of the advantages of both criteria, while compensating for the weaknesses of each.

The stability margin (more explicitly, the limiting current) has the advantage in that it takes into account the geometry of the problem (as well as the copper resistivity, etc.), which has been shown, as stated above, to be a significant factor in stability. On the other hand, the temperature margin has the obvious advantage of incorporating all of the critical parameter changes in the superconductor, while the stability margin only incorporates the critical temperature. These advantages will be discussed below.

It is important to note that, although the results are distinguished as either the ITER EDA design or the ITER CDA design, these labels must be taken cautiously. The CDA design has been published for some time now, but the EDA design is far from complete and the geometry and operating conditions used in *MagRad* for this

design have been obtained from the ITER Magnet Group meeting held at MIT early in 1993. Also, the ARIES designs do not specify any particulars concerning the geometry, so the generic geometry of the ITER CDA design was used (changing, of course, the cross sectional areas of the components to match those specified by the ARIES-II/IV studies). Stability with respect to fluence was not performed in the same systematic way for ARIES as it was for the ITER designs, since the ITER designs are quite adequate in demonstrating the general effects of fluence, superconducting material, conductor design, and initiating event parameters. Of course, stability performance of the ARIES designs will be given in the ARIES case study section later in the thesis. It must be reiterated that in using these labels, the author is in no way implying a set design or even design direction for the EDA design. These two ITER designs have the advantage, however, of being reasonably well documented (as opposed to the ARIES design) and relevant.

Of first importance for the designer, it can be seen from Fig. 17 that the $(\text{NbTi})_3\text{Sn}$ alloy has the best stability of any of the three candidate materials chosen. It must be pointed out, though, that the binary Nb_3Sn did quite well, and though it did not have as high a start-up stability, it retained its stability with respect to neutron fluence somewhat better than the ternaries, which was to be expected. Also, the predictions concerning the $(\text{NbTa})_3\text{Sn}$ alloy may be slightly off, since the data that was used was by necessity older for this material [23] than for the other two [24]. It is especially striking that, whereas the effect of a peaking B_{c20} and J_c can be readily seen for the $(\text{NbTi})_3\text{Sn}$ alloy and Nb_3Sn , the stability of the $(\text{NbTa})_3\text{Sn}$ alloy degrades much in the same manner as its B_{c20} degrades, nearly linearly with fluence. These same results can be seen if one were to examine the temperature margin curves as well. Additionally, on the temperature margin curves shown in Fig. 18 it can be seen that the

Fig. 17: Stability margin degrades with fluence (EDA base cases [17]).

Fig. 18: Temperature margin degrades with fluence (EDA base cases [17]).

Fig. 19: Stability margin for the ITER CDA degrades with fluence in much the same way as that for the ITER EDA [17].

Nb₃Sn has a rather broad peak; this is due in large part to the much slower decline in the critical temperature than in the ternaries. As benchmark criteria to determine when these conductors have degraded to dangerously low stabilities, the 100 mJ/cm³ and the 250 mJ/cm³ stability limits have been marked off (in Figs. 17 and 19) as well as the 1 K temperature margin (in Fig. 18). All of these limits seem to be somewhat common in designing coils. That the limits are relatively equivalent can be seen from the fact that, for the same design, they are approached at the same fluence, except in the case of the EDA non-base case scenarios (using Nb-Ta) as will be explained below.

It can further be seen by comparing Fig. 19 to Fig. 17 that there is apparently not much difference between the stability of the EDA design (in progress) and the CDA design. This result can be somewhat misleading unless it is kept in mind that for

practically the same stability, the EDA design is operating at a higher field and a higher temperature (though a lower current density, which does not enter into the stability margin equation directly). Reassuringly, though, it seems that both of these designs will be stable out to about 1.5×10^{19} ITER n/cm²; well past the designed end of life fluence for ITER. This result must be taken cautiously, since the superconductor database for fluence effects was constructed, as outlined previously, in order to meet the ITER Magnet Group's specifications of critical temperature, field, and current density. The true published data is quite lacking as far as the critical field values are concerned (and the critical temperature values for the ternaries). This is obviously an area to be concerned about. Thus, in using *MagRad* some technological advancement was assumed prior to the fabrication of the coils.

Exploring stability further, a couple of final predictions should be noted. In the EDA, (NbTa)₃Sn, non-base case scenarios (no core helium flowpath and a lower initiating pulse heating duration) shown in Fig. 21, it can be seen that the temperature margin formulation gives no difference between the cases whatever (assuming that the helium is at the same temperature in each case). This is to be expected, since the temperature margin is dependent only upon the bath temperature and the critical parameter values of the superconductor. However, the stability margin, based upon

Fig. 20: Stability margin degrades with fluence in a variety of ways depending upon the thermal-hydraulic characteristics of the case (various EDA non-base cases [17]).

Fig. 21: Temperature margins degrade with fluence uniformly for varying thermal-hydraulic cases (various EDA non-base cases [17]).

Fig. 22: Stability margin varies with fluence in very disparate ways depending upon the B_{c20} formulations (EDA (NbTa)₃Sn base cases [17]).

the limiting current formulation, shown in Fig. 20 shows a very significant difference in the behavior of the three cases, a behavior which one would naturally expect to find. This is one example of where it is much more desirable to use the limiting current / stability margin formulation rather than the temperature margin formulation for stability. Finally, in the curves predicting the stability (and temperature margin) of the EDA, Nb-Ta base case using three different formulations for determining the value of the critical field at fluence (Fig. 22), it can be seen that there is a very large disparity between the behaviors. The uppermost two curves follow the formulation given in Eqn. 1, but use different values for the initial normal state resistivity. These values are 170 nΩm and 400 nΩm, which bound previous experimentally determined values. Although all researchers, whose work has been examined to date, have only casually

looked at fluence effects and have used the 170 nΩm value (corresponding to the higher of the two uppermost curves), it is instructive to show that even a modification of the initial resistivity (a much 'dirtier' conductor, corresponding to the lower of the two uppermost curves) yields predicted stability margins that are much too high. For this reason, if one were to use this formulation instead of the data (lowest of the three curves) one would be inclined to conclude that near term reactors will not have any problems with stability. Obviously, this dangerous conclusion can be avoided by using a properly constructed set of consistent data.

E. Stability Conclusions:

It has been shown that the stability margin for long conductors is expected to decrease linearly with conductor current (see Fig. 14). It has also been demonstrated that this stability margin is very sensitive to the duration of the heat pulse [17]. Since the local helium enthalpy is the same irrespective of heat pulse duration, the assumption that the helium enthalpy is very effectively used as long as the limiting current is not exceeded in the conductor seems to be speculative. This is, however, precisely the assumption which goes into the energy margin formulation which uses helium enthalpies and the current sharing temperature (see Eqns. 20 and 21). Thus, it remains as one of the original elements of this work that the stability margin cannot, with accuracy, be predicted by an energy margin formulation, since such a formulation does not take into account any of the thermal-hydraulic information of the destabilizing event.

For this reason, namely that the energy margin formulation is inaccurate in some cases, the limiting current formulation (following Dresner [1]) was employed to calculate the stability margin. This formulation has been discussed in previous sections. Unfortunately, it has been common practice among magnet designers over the last several years to specify the stability of the conductor in terms of energy margin. Reiterating what has been stated above, this formulation can be very inaccurate in some cases. Nevertheless, in order to keep in line with all other magnet designers, *MagRad* has been modified to predict stability margin in terms of this formulation (energy margin). At the same time, *MagRad* also calculates the ratio between the operating current and the limiting current. This fraction is directly proportional to the stability margin as calculated by the limiting current formulation. *MagRad* effectively uses the limiting current formulation to predict stability; this prediction is just tied to a different

design constraint, namely the operating to limiting current ratio. By using both formulations, the designer should be able to more accurately predict the actual stability margin of the conductor in question.

In order to make proper use of the predictions of stability margin, it is suggested by this author that the limit set by the magnet designer should be increased by a safety factor of two. Typically, safety factors are employed in setting the stability limit, in order to account for unexpectedly high heat deposition rates in the conductor. The safety factor of two that has been proposed by this author, in order to account for particularly disadvantageous thermal-hydraulic parameters of the initiating event, would be in addition to any other safety factors. Thus, the result is an overall safety factor of about four. For example, then, if it is calculated that expected destabilizing heat loads might amount to about 125 mW/cc, the stability margin limit should be set to about 500 mW/cc rather than 250 mW/cc, as might be typically done in present designs. This limit also would not be sensitive to whether the conductor was operating in the well-cooled regime (operating to limiting current ratio of less than about 0.9) or in the ill-cooled regime (operating to limiting current ratio near 1.0 or higher).

Furthermore, it is suggested that a secondary stability limit also be set. This additional stability limit is already commonly set by magnet designers, and has to do with the operating to limiting current ratio. Typically, magnet designers set the limit on this ratio to about 0.9. It is suggested that, in conjunction with the above stability limit modification, this limit on the operating to limiting current ratio be set to about 0.95. The reason for this modification is simply that there does not appear to be a significant transition regime for long conductors (as was explained in previous sections). Thus, approaching the ill-cooled regime, the stability margin does not demonstrate the abrupt change, as it does in shorter conductors.

In conclusion, then, *MagRad* has been modified from predicting stability margin by using the limiting current formulation to predicting the stability margin by using the energy margin formulation. This has also been coupled to the prediction of the operating to limiting current ratio in order to give a more accurate overall prediction of the stability of the conductor. In essence, *MagRad* predicts stability by using both formulations, and proper limits to these design constraints have been rationally proposed. It seems clear that by using both formulations, the stability of any given conductor will be more accurately predicted than if only one of these formulations was used.

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CHAPTER 5: PROTECTION

A. Introduction to Protection:

Protection of the magnet coils against permanent damage due to the occurrence of fault conditions is of utmost importance, since the coils represent a substantial fraction of the initial capital cost of the reactor. Moreover, since there is a great deal of energy stored in the coils which may be liberated in the event of fault conditions and since the reactor comes under the jurisdiction of nuclear safety regulations, fault conditions and their consequences must be examined. Fault conditions may occur with respect to the magnet system in three distinct sub-systems: the coils themselves, the power systems, and the cryogenic systems. Of course, it is most desirable that the magnet system display passive safety. Additionally, it is of great importance to demonstrate that the consequences of any fault conditions arising internal to the magnet systems are contained within that system and do not interact with other reactor systems. This will have the impact of eliminating an entire fault path in which the reactor integrity as a whole is compromised, and thus contribute to the safety of the reactor system. It would be unadvisable to claim that the magnet system will not be affected by fault conditions existing in other systems of the reactor, because it is possible that more energy could be liberated by these other system failures. Nor is it possible to perform any sort of comprehensive safety analysis of a magnet system until such time as the magnet system details are worked out. Basic principles may, however, be developed by which the magnet designer can navigate the course of system optimization. It is beyond the scope of this work to fully analyze the safety of the magnet system as it affects the safety of working personnel and to optimize with respect to those variables. Rather, this dissertation focuses on the safety of the coils

themselves, assuming for the purposes of this present work that the safety of the other two sub-systems can be sufficiently decoupled from the safety of the coils and that optimization with respect to the safety of these sub-systems will involve only very minor, design-specific modifications in any of the other systems.

In a recent paper [1] three levels of operation were specified for each of the magnet system sub-systems. As only the safety of the coils is dealt with in this work, only the coil sub-system will be discussed here. The three levels of operation are normal operation, upset operation, and fault conditions.

Under normal operation the TF coils will be charged to their design operating current, the PF coils will be ramped to inductively drive and control the plasma, and account must be taken for the possibility of plasma disruptions. While operating normally, no quenches of the coils are allowed. It is clear that this constraint must be applied, because reactors may be designed to withstand a relatively large number of disruptions (up to thousands), while a coil cannot withstand very many quenches. Upset operation will be defined according to the following conditions: coil quench, loss of coolant to the coil, loss of power to the coil, loss of cryostat vacuum, or earthquakes. During operation in the upset level the coils must be shut down without damage to the coils. Finally, under fault conditions, damage must be contained to the magnet system. The occurrences under which fault conditions may arise are the following: electrical short circuit, electrical open circuit, mechanical failure, or control failure [1]. It must be pointed out, additionally, that the operating level of the coils is not only dependent upon those conditions prevailing within the coil sub-system, but is also dependent upon the operating levels of both the cryogenic sub-system and the power sub-system.

For obvious economic reasons coil quenches must be precluded under normal reactor operation. Quenches may be initiated by relatively small energy inputs (on the

order of 500 mJ/cc over a time of only 10 ms), which may be induced by any of a number of causes such as mechanical/frictional slip of wires or flux jumping. Coils optimized with respect to stability will give reasonable assurance that such potential initiating events will not initiate a quench, given that it is designed to an appropriate stability limit. Upon detection of a quench, however, resistive discharge of the coils is not necessary if the magnet system is designed in such a way as to allow sufficient quench propagation speed in order to spread out the stored energy rather than leave it confined to a highly localized area of the coil. Moreover, several design options exist whereby the speed of quench propagation may be increased [1, 2]. The only option which is contained within the scope of this work is the incorporation of extra copper within the cable. This will significantly decrease the maximum hot spot temperature in the event of a quench, although it also results in a lower cable space current density. Whatever criteria are imposed upon the protection of the magnet (such as the values of the maximum hot spot temperature and the maximum quench voltage), it will be assumed they can be met by implementing any of the other protection enhancement design options which may be required. Generally, for large CICC magnets, an acceptable value for the protection criterion of hot spot temperature is on the order of 150 K, and for the large systems envisioned for fusion TF magnets, an acceptable value for the protection criterion of maximum dump voltage is on the order of 20 kV. The protection analysis of the conductor has been performed using *RadQuench*, which can predict accurately the maximum hot spot temperature given inputs of the various quench parameters as well as the cable space current density, the copper fraction, etc.

RadQuench is a module of *MagRad* that calculates relevant protection characteristics of cable in conduit conductors (CICC), especially the maximum hot spot temperature in the event of a quench. The physical model employed by this module is based on a three phase quench process. The time scales of the three phases depend

upon the dump time constant for the coil, the quench detection and response time of the system, and the time at which the conductor jacket's heat capacity is added to the heat balance. This last parameter is fitted to give the best result with experiment, and experimentally divergent cases with respect to time scales are used to check this fit. The experiments that were used as benchmarks are the well documented Westinghouse LCP quench experiments [3]. The Westinghouse LCP coil serves as a suitable benchmark, being a very large CICC coil designed to simulate larger fusion TF coils, which are the primary objects to be analyzed with this module. This three phase model has demonstrated the ability to predict the quench temperatures for the two typical cases of varying time scales to within 7%, and thus should be safe to use for a variety of coils (with varying protection time scales) including ITER.

B. Introduction to RadQuench:

The verification of the applicability and accuracy of RadQuench has been accomplished by benchmarking the code to one of the only large coil experiments that has been performed to date, the Large Coil Program [3]. Specifically, we have used the results of quench experiments for the CICC Westinghouse toroidal field coil to compare to *RadQuench's* predictions concerning maximum hot spot temperature. The conductor characteristics have been modeled, and Table 6 gives all of the relevant parameters. The conductor flowpaths can be demonstrated to satisfy the criteria which define 'long conductor' [4, 5], with the total flowpath length being over 80 m for each of the parallel paths,

$$L^2 \gg 4V_q^2 t_m^2 \quad (33)$$

where L is the length of the flowpath, V_q is the quench velocity (which is reported in [3]), and t_m is the time at which the calculation is made. Basically this inequality shows that the quenching section does not 'feel' the end of the flowpath, given that the quench occurs near the center of the flowpath. Experimentally, the quenches occur close to the center of the flowpath for the Westinghouse coil, since they occur on the inner turn of the coil which is wound in double pancake fashion with each flowpath beginning on the outer turn. The quantity on the right hand side of Eqn. (33), in the case of the Westinghouse coil, has a value of about 256 m² in the downstream direction (only 23 m² in the upstream direction), and L^2 is over 6400 m² (at least 25 times greater than the right hand side). Thus, the Westinghouse coil can be taken as typical of a TF coil in all respects.

Table 6: Westinghouse - LCP Coil Parameters [3, 6-8]

Operating Temperature (K)	4.2
Field at the Coil (T)	11.2
Field on Axis (T)	1.72
Copper RRR	258
Operating Current (kA)	17.625
Cable Space Current Density (A/mm ²)	59.24
Number of Turns	424
Total NI (MA _t)	7.47
Wire Diameter (mm)	0.7
Void Fraction	0.32
Copper Fraction in Conductor	0.624
Cable Space Cross Sectional Area (mm ²)	297.5
Jacket Thickness (mm)	1.75
Number of Strands	486
Number of Pancakes	26
Winding Method	Four - in - Hand
Number of Turns per Flowpath	over 8
Approximate Length of Turn	about 10 m

C. Theory and Results:

First Model:

In order to determine the maximum hot spot temperature, the standard adiabatic approach has been taken. The quench is assumed to occur in two phases, one in which the current is held constant and the other in which the current is allowed to decay exponentially, as would happen when the quenching coil's current is dumped through an external resistor. The resistance of the normal zone is considered negligible in the first phase (thus the constant current assumption). It is quite important to distinguish between the two phases of the quench, because it has been found that the jacket of the conductor does not contribute to the heat capacity during the first phase, while it does contribute during the second phase. This, of course, is an oversimplification, but it does seem to be adequate for this study.

The analysis is initiated by performing a heat balance over the cable space volume during the first phase,

$$I_{Cu,0}^2(t) R_{Cu} \cdot dt = c_{p,cs}(T) V_{cs} \cdot dT \quad (34)$$

where $I_{Cu,0}$ is the initial current flowing through the copper (equivalent to the conductor operating current), R_{Cu} is the resistance of the copper in the quench region, $c_{p,cs}$ is the specific heat averaged over the cable space, V_{cs} is the cable space volume, t is time, and T is the temperature in the cable space (at the point of quench initiation, and assuming transverse thermal equilibrium). This equation cannot be readily solved analytically for the maximum temperature, since the properties of the component materials vary with temperature in a complicated way, but the equation can be solved very easily for the current density,

$$J_{op} = \left(\frac{1}{t_d} f_{Cu,cs} \sum_i^{cs} f_i I_i \right)^{1/2} \quad (35)$$

$$I_i = \int_{T_0}^{T_{max}} \frac{c_{p,i}}{\rho_{Cu}} \cdot dT \quad (36)$$

where J_{op} is the allowed operating current cable space current density, t_d is the delay time (the time between the initiation of the quench and the dump of the current), $f_{Cu,cs}$ is the fraction of copper over the cable space, the summation is taken over all components within the cable space (copper, superconductor, and helium), f_i is the fraction of the i th component over the cable space, T_0 is the operating temperature, T_{max} is the maximum hot spot temperature, $c_{p,i}$ is the heat capacity of the i th component, and ρ_{Cu} is the copper resistivity.

Similarly, during the second phase of the quench we perform a heat balance over the entire turn volume (ideally, this would include the insulation as well as the jacket, but due to the short time scales involved it has been neglected),

$$I_{Cu}^2(t) R_{Cu} \cdot dt = c_{p,turn}(T) V_{turn} \cdot dT \quad (37)$$

$$I_{Cu}(t) = I_{Cu,0} \exp\left(-\frac{t}{\tau}\right) \quad (38)$$

where $c_{p,turn}$ is the specific heat averaged over the turn, V_{turn} is the turn volume, I_{Cu} is the current through the copper at any given time subsequent to the dump, and τ is the dump time constant. The current density in this phase decays exponentially with a time constant of τ , dependent upon the coil inductance and the resistance of the dump resistor. Again, this heat balance may be solved for the current density,

$$J_{op} = \left(\frac{2}{\tau} \exp\left(\frac{2t_d}{\tau}\right) \frac{f_{Cu,cs}}{f_{cs}} \sum_i^{turn} f_i I_i \right)^{1/2} \quad (39)$$

where f_{cs} is the fraction of the cable space over the turn and the summation is taken over all components within the turn (copper, superconductor, helium, and steel). Note the extra exponential term as well as the cable space fraction term, both serving to reduce the allowed current density (when t_d is less than τ).

In both cases the current density has been derived by constraining it to match the actual operating current density while allowing the maximum hot spot temperature to vary. Thus, one is able to predict, using *RadQuench*, the maximum temperature. The results of these predictions are in good agreement with the experiment. The parameters of the quench case which was modeled were a dump time constant of 10 s and a delay time of 5.4 s. This time delay actually represents the delay between the time at which the quench begins to propagate and the time at which the current is dumped into the external dump resistor. Thus, at time zero in the energy balance, the temperature is not the original helium temperature but the critical temperature of the conductor. The effect of this slight modification is an addition of about 2 K to the temperature at the time of the dump and about 1 K to the maximum hot spot temperature.

The results of the predicted and measured temperature versus time are shown in Fig. 23. As can be seen the temperature versus time prediction for the case of the short delay time follows the experimental temperature versus time measurements quite well. For the case of the long delay time, the predicted temperature at the time of the dump is significantly higher than the measured temperature (22%). The most obvious and probable explanation of this discrepancy (which becomes a little smaller by the time the dump is finished) is that the heat capacity of the steel jacket is ignored during the entire first phase of the quench. This is probably not a good assumption, especially in cases

of long delay times. This value of time delay with respect to the addition of the jacket heat capacity (not with respect to the current dump) should be sensitive to the speed of the temperature rise, but this speed does not vary much from design to design since the component fractions do not vary significantly enough from design to design. The value of time delay should also be sensitive to the current density of the conductor, as this affects the temperature rise speed. For such a simplistic model as the adiabatic model, there is no simple expression determining the appropriate time delay after which the steel heat capacity should be added to the heat balance. For coils with a shorter dump delay time, such as ITER with a dump time delay of 1-2 s, this longer jacket heat capacity delay time must be taken into account.

Fig. 23: Two-phase prediction of temperature does not match well experimentally measured temperature for the Westinghouse Large Coil Program CICC TF coil.

Second Model:

The difficulty encountered using the two-phase model has been addressed in both a qualitative and a quantitative way by the use of a slightly more sophisticated quench model. The model retains the simplicity of the adiabatic model, but employs a three phase description of the quench phenomenon rather than the two phase description given above. Let $t_{d,heat}$ be the time at which the jacket heat capacitance should be added to the heat balance and $t_{d,dump}$ be the time at which the current is dumped. If $t_{d,heat}$ is less than $t_{d,dump}$ the three phases are as follows: 1) the time period between the time of initial quench propagation and the time at which the jacket heat capacitance adds to the heat balance; 2) the time period between the time at which the jacket heat capacitance is added to the heat balance and the time at which the current is dumped; and, finally, 3) the time period subsequent to the current dump. If, on the other hand, $t_{d,heat}$ is greater than $t_{d,dump}$ the three phases are the following: 1) the time period between the time of initial quench propagation and the time at which the current is dumped; 2) the time period between the time at which the current is dumped and the time at which the jacket heat capacitance is added to the heat balance; and, finally, 3) the time period subsequent to the addition of the jacket heat capacitance to the heat balance.

The analytical development of this model parallels the development of the two phase model. Omitting details, the results of the heat balances (solved for J_{op}) in the case that the jacket heat capacitance adds to the heat balance before the current is dumped are as follows,

$$Phase \ 1: \quad J_{op} = \left(\frac{1}{t_{d,heat}} f_{Cu,cs} \sum_i^{cs} f_i I_i \right)^{1/2} \quad (40)$$

$$\text{Phase 2: } J_{op} = \left(\left(\frac{2}{\tau} \right) \frac{1}{\exp\left(\frac{-2t_{d,heat}}{\tau}\right) - \exp\left(\frac{-2t_{d,dump}}{\tau}\right)} \frac{f_{Cu,cs}}{f_{cs}} \sum_i^{turn} f_i I_i \right)^{1/2} \quad (41)$$

$$\text{Phase 3: } J_{op} = \left(\frac{2}{\tau} \exp\left(\frac{2t_{d,dump}}{\tau}\right) \frac{f_{Cu,cs}}{f_{cs}} \sum_i^{turn} f_i I_i \right)^{1/2} \quad (42)$$

where $t_{d,dump}$ is the delay time for the dump and $t_{d,heat}$ is the delay time for the addition of the jacket heat capacitance. For the case that the current is dumped before the jacket heat capacitance adds to the heat balance the results are similar,

$$\text{Phase 1: } J_{op} = \left(\frac{1}{t_{d,dump}} f_{Cu,cs} \sum_i^{cs} f_i I_i \right)^{1/2} \quad (43)$$

$$\text{Phase 2: } J_{op} = \left(\left(\frac{2}{\tau} \right) \frac{1}{\exp\left(\frac{-2t_{d,dump}}{\tau}\right) - \exp\left(\frac{-2t_{d,heat}}{\tau}\right)} f_{Cu,cs} \sum_i^{cs} f_i I_i \right)^{1/2} \quad (44)$$

$$\text{Phase 3: } J_{op} = \left(\frac{2}{\tau} \exp\left(\frac{2t_{d,heat}}{\tau}\right) \frac{f_{Cu,cs}}{f_{cs}} \sum_i^{turn} f_i I_i \right)^{1/2} \quad (45)$$

In benchmarking the long time delay case, it was found that if the jacket heat capacitance began adding to the heat balance at about 8 seconds after initiation of quench propagation, the predictions obtained were in quite good agreement with the

experiment. Then, since $t_{d,heat}$ should be independent of $t_{d,dump}$, the same value can be used in the short time delay case. The predictions for the short time delay case, using the $t_{d,heat}$ calibrated to the long time delay case, are in even better agreement with experiment than the predictions using the two phase model. As seen in Fig. 24, the predictions for the short time delay case are still slightly better than those for the long time delay case, but no temperature prediction is more than 7% off from the experimental value. Thus, the three phase model for quench is quite adequate for initial design studies. Moreover, both the case in which $t_{d,heat}$ is greater than $t_{d,dump}$ and the case in which $t_{d,dump}$ is greater than $t_{d,heat}$ agree with experiment.

Fig. 24. Three-phase prediction of temperature matches very well the experimentally measured temperature for to Westinghouse Large Coil Program CICC TF coil.

D. Other Considerations:

Protection analysis includes not only maximum hot-spot temperature prediction, but also prediction with respect to the maximum quench voltage and the maximum quench pressure. Voltage considerations are necessary in order to preclude turn-to-turn or turn-to-ground arcing, either of which would seriously damage the coil. Upper bounds to the maximum voltage is commonly set to about 20 kV (as in both the ITER and the ARIES designs). The ITER CDA design was analyzed and found to have a maximum quench voltage of about 7 kV, well below the design limit. Any other type of coil design, besides the CDA-like pancake wound coil, is very difficult to analyze with respect to maximum quench voltage. The reason for this is two-fold. Firstly, the shell wound type design affords an advantage in that the magnet can be graded (as mentioned previously), and this grading makes the speed of the quench, and hence the maximum voltage, very difficult to predict. Quench-specific numerical codes are not even yet able to handle this complex problem, though they can sometimes give fair approximations. Secondly, the inductance of the TF system is also very difficult to calculate, with inductance matrices requiring time-intensive calculations. Further complicating the design consideration of maximum quench voltage is the fact that any analysis is very event-specific [9, 10]. That is, quench voltage is very dependent upon the current dump specifics of the system, including the order in which the coils are dumped, if any others are dumped at all (since the inductance of the system is dependent upon which coils are charged).

Besides the difficulty in the prediction of the quench voltage, it can be said that this design parameter does not, generally, receive full analysis until the coil is designed to meet all other design parameters. While it is very difficult to accurately predict the quench voltage, it is not difficult to overdesign the system to insure that the voltage

limit will not be exceeded. This is done very inexpensively by adequate design of the dump resistors, since the voltage of the quench will, to first order, go as the inductance divided by the dump resistance. Thus large dump resistors can be designed to meet voltage limit for any reasonably conceivable magnet design [9]. For the above reasons *MagRad* does not predict maximum quench voltages.

Finally, quench protection analysis includes predicting the maximum pressure attained in the coil. Due to the rapid heating and expulsion of the helium in the magnet, large pressures can be achieved in the conduit. It has been found [2], that experimentally measured quench pressures do not match analytic calculations. In fact, the pressures achieved experimentally are much lower than those predicted analytically. There do exist numerical codes to handle this design problem which match the experimental data fairly well, but they are very large, time-intensive codes. It also has been found that this design constraint (about 25 MPa for ITER CDA) is easily met by a reasonable thickness of the conductor jacket (on the order of greater than 1 mm). Jacket material is the most inexpensive component of the conductor (about twenty to fifty times less than the cost of the strand), giving rise to a magnet cost that is relatively insensitive to the thickness of jacket. For this reason, as well as for the reason that such predictions are very time consuming and not appropriate for an optimization code, quench pressure is also not treated by *MagRad*.

E. Conclusions:

The user - friendly computer code module, RadQuench, has been developed and benchmarked for use in predicting the protection characteristics of the large CICC TF coils under investigation in this dissertation. The physical model describing the quench phenomenon employs a three phase quench timescale distinction. The timescales represented in this model are the characteristic dump time of the coil, the quench detection and response time of the system, and the time at which the jacket heat capacitance is added to the heat balance. The inequality of the latter two timescales has profound impact upon both the temperature as a function of time and the maximum temperature, especially for the case in which the detection time delay is quite long (on the order of about 10 s). In the two typical cases studied, one in which the detection time delay was longer than the jacket heat capacitance time delay and the other being the converse of the first case, the three phase model predicts temperatures that do not deviate from the experimental temperatures by more than 7%.

The value of time delay with respect to the addition of the jacket heat capacity (not with respect to the current dump) should be sensitive to the speed of the temperature rise, but this speed does not vary much from design to design since the component fractions do not vary significantly enough from design to design. The value of this time delay would also be somewhat sensitive to the current density of the conductor, as this would effect the temperature rise speed. For such a simplistic model as the adiabatic model, there is no simple expression determining the appropriate time delay after which the steel heat capacity should be added to the heat balance. For coils with a shorter dump time delay, however, such as ITER with a dump time delay of 1-2 s, the three phase model does account for a longer jacket heat capacity time delay (about 8 seconds).

Furthermore, in analyzing the ITER CDA coil design with *MagRad* the predicted maximum quench temperature is 252 K. The predicted maximum quench temperature by the ITER Magnets Group is about 235 K [9]. The variance between the two predictions is only 7%. At the time of the current dump, *MagRad* predicts a temperature of 44 K, while that predicted by the ITER Group is about 40 K. These two predictions vary by about 10%. Thus, it can be seen that the predictions made by *MagRad* are quite consistent with those made by larger numerical codes, and can be relied upon with a high level of confidence. As a final note, the prediction of either quench voltage or pressure is beyond the scope of the thesis, since this predictive capability would not be consistent with the development of an optimization code.

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CHAPTER 6: ANNEALING STUDIES

A. Introduction:

In an effort to increase the useful lifetime of the magnet coils, one of two options must be employed in the overall design. Due to the significant and degrading radiation environment, either the blanket and shield component must be made thicker thus reducing the fluence at the coils or the magnets must be annealed thus partially (or fully) reducing the effects of the high fluence. Since the former of these options involves a great deal of capital, on the order of \$ 5 - 10 million per centimeter of increased thickness [1] (a good rule of thumb is that in order to achieve a reduction in the neutron fluence by a factor of two, five centimeters of shielding must be added), it behooves the reactor design team and the magnet designer to consider the implementation of an appropriate magnet annealing schedule. In determining an appropriate annealing schedule three components of the coil must be examined: the stabilizer (copper); the superconductor (a member of the Nb-Sn system); and the insulator (either organic or inorganic). The structural candidate best suited for use in the magnet coils is probably an alloy such as Incoloy, since radiation does not significantly degrade its mechanical properties and its coefficients of thermal expansion are particularly matched to that of the cable. Since the thermal expansion coefficients have been tailored to that of the coils, and since the coils will most probably be fabricated using the wind-and-react method, there should be very little thermal strain upon the cable in the annealing process. This, of course, must be experimentally confirmed.

B. Copper Stabilizer Anneals:

Copper annealing schedules have been studied by Sawan [2], and upon the discovery that the damage limit as used by the neutronics groups to the copper was too high, a suitable copper annealing schedule was incorporated into the design in 1992 using established methods. The annealing of the copper involves only room temperature anneals, and 85-90% of the damage produced by the radiation field can be recovered per anneal [3]. As stated above, it can be shown that the deviation from a single Kohler plot by pre- and post-annealed copper is insignificant at the high fields of fusion devices such as ITER or ARIES. The specifics of the annealing possibilities are given in Table 6, and the equations used to determine the annealing schedule are,

$$t_i = \Delta t_1 \times \frac{(1-r^n)}{(1-r)}, \quad (46)$$

$$\Delta t_n = r^{n-1} \times \Delta t_1, \quad (47)$$

$$n = \frac{\ln \left[1 - \frac{t_i}{\Delta t_1} \times (1-r) \right]}{\ln(r)} \quad (48)$$

where t_i is the reactor lifetime, Δt_1 is the time before the first anneal, r is the recovery fraction for the copper resistivity, n is the number of anneals, and Δt_n is the duration of the final irradiation cycle (n and Δt_n may also correspond to any other cycle which one may desire to analyze). Possible ITER CDA and EDA copper stabilizer annealing schedule parameters have been given in Table 7.

With respect to the superconducting material, data on the effectiveness of anneals is slightly less systematic than that for copper. It is clear, however, that since superconductor anneals become much more effective at elevated temperatures (on the order of 600° C), annealing of the superconductor definitely favors the use of inorganic insulators. Organic epoxies can begin to char at temperatures of about 400° C [4], and it is clear from all studies to date that damage in the superconductor is little affected until these dangerously high (with respect to organic insulators) temperatures are attained. Sweedler's data [5] on high temperature Nb₃Sn anneals seems to agree well with Francavilla's data [6] on V₃Ga anneals, and since Sweedler did not study the effects of low temperature anneals and Francavilla did we will take Francavilla's results at low temperatures as indicative of the effects of low temperature anneals upon Nb₃Sn [7-12]. As a further confirmation of this approximation, it can also be seen that Francavilla's V₃Ga data is very similar to Brown's Nb₃Sn data [6, 9]. This is much to be expected since the two alloys have the same microstructure. Thus, we shall assume a 10-20% recovery in the critical temperature (and other critical values) at low temperature (slightly higher than room temperature) anneals, and a full recovery after 2 h at 750° C. Thus, in the general case in which it is only possible to extend the lifetime of the coil by way of high temperature anneals, the impact of such an annealing schedule upon the overall reactor design must be weighed against the impact of adopting the option of increasing the shield thickness.

Table 7: Possible Copper Stabilizer Annealing Schedules (after [2]).

	ITER/CDA	ITER/EDA
End of Life Fast Fluence in the Coil ($1e18$ n/sqcm)	6.08	3.60
End of Life Damage to the Copper Stabilizer ($1e-3$ dpa)	2.70	2.40
Reactor Lifetime (FPY)	3.80	1.50
Availability	0.25	0.25
Damage Rate in the Copper Stabilizer ($1e-3$ dpa/FPY)	0.71	1.60
Damage Limit set for the Copper Stabilizer ($1e-3$ dpa)	0.50	0.50
Recovery Fraction per Room Temperature Anneal	0.88	0.88
Time Before First Anneal (mos.)	33.78	15.00
Number of Anneals Required	8.17	6.71
Time Between Final Two Anneals (mos.)	13.51	7.23

NOTE: CDA refers to the final design of the ITER Conceptual Design Activity, while EDA refers to the ongoing design of the ITER Engineering Design Activity. Recall that these designs are used only for the sake of convenience in benchmarking against reactor relevant designs.

C. Superconductor Anneals:

In the case of annealing the superconducting material there are a great many more uncertainties and concerns to be addressed than in the case of stabilizer annealing. In this study we have assumed the use of Nb₃Sn (either binary or ternary), and will continue to focus on this material for the annealing analyses. It is important to remember, however, that different superconducting materials will behave differently under annealing conditions, and the conditions necessary for the annealing of Nb based superconductors will not coincide with those necessary for other types of materials. The work presented here may be applicable with appropriate modification to other superconducting materials, the A-15 type superconductors in particular.

The data for the annealing of Nb₃Sn is rather sparse [5, 6, 8, 9, 13, 14]. Some general comments can be made from the outset, though, concerning the annealing of Nb₃Sn. From [5] it seems clear that a full recovery of critical temperature can be achieved in high irradiated (5×10^{19} n/cm²) Nb₃Sn at an annealing temperature of 750° C and an annealing duration of about 5 hours. This is in contrast to the behavior of Nb₃Ga, which achieves full recovery after only about 20 minutes at the same annealing temperature. The reason for this difference is probably due to the rather significant difference in the sizes of the atoms involved, namely Sn and Ga. This result for the Nb₃Sn agrees quite well with [13]. Of some interest to note is that V₃Ga (as well as V₃Si [8]) requires much lower annealing temperatures, on the order of about 475° C (possibly 400° C if the anneal is allowed to run about 100 hours), to achieve full anneal. It has also been observed in V₃Ga that partial annealing occurs at temperatures as low as about 200° C, though the amount of damage annealed appears relatively small.

Complicating the situation with respect to annealing of the superconductor is the experimental observation that whereas the values of critical temperature, upper critical field, and critical current density all recover their pre-irradiated values, the flux pinning details are left unchanged after annealing unless the anneal is performed at even higher temperatures [13, 14]. A plot of the volume pinning force as a function of the reduced magnetic field, as shown in Fig. 25, yields a predictable maximum for unirradiated Nb_3Sn . This maximum increases and shifts with fluence [14], thus the expression for J_c given in Eq. (4) changes and gives rise to the necessity of our allowing C_0 to vary with fluence. Upon annealing, the value of the maximum decreases (at least partially), but remains in its shifted position. Further obscuring the data is the fact that this observation was made for only a relatively low temperature anneal of 270 °C.

Fig. 25: Pinning force for Nb_3Sn does not relax to pre-irradiated maximum subsequent to low temperature anneal.

The interpretation of the results of what happens during the irradiation process seems reasonably clear. Namely, the irradiation of the superconductor produces defect cascades which act as additional pinning sites to the grain boundaries present in the unirradiated sample. The interpretation of what happens during the annealing process seems much less clear, however. It seems very clear that nearly all of the point defects, representing long range order perturbations, are annealed out at sufficiently high temperatures (750° C) yielding a full recovery in the critical temperature. Questions still remain, though, concerning the larger defect structures introduced into the lattice via defect cascade production. Are these structures also annealed out? If so, at what temperature? These questions can only be answered by a systematic experimental program. Until such a time as this has been done, it is safest to assume that these larger structures are annealed out, since this yields a somewhat conservative estimate of the upper critical field and critical current density subsequent to annealing as defect structures remaining would only enhance these characteristics. Concluding the discussion concerning the details of the annealing process for Nb₃Sn, it has been assumed that full recovery of superconducting characteristics is achieved after 5 hours at 750° C. This assumption can be modified for different superconducting materials, as well as for new data, but is consistent with the data currently available for Nb₃Sn.

Having discussed the physical basis of the annealing process for Nb₃Sn, the engineering of the annealing process now must be discussed. As has been pointed out in the chapter on protection, typically designers set a limit to the maximum hot spot temperature in the event of a quench. The reasoning behind this, as was stated previously, is that large temperature rises across relatively small distances in different materials will give rise to severe strains on those materials due to varying differential coefficients of expansion. These strains may seriously jeopardize the integrity of the magnet either mechanically or electrically. This constraint, obviously, will also apply

with equal force to the annealing process. A very significant difference between the two cases of quench and anneal, however, is that an anneal may be performed nearly isotropically upon the magnet whereas a quench is, by the nature of the case, a localized and highly anisotropic event. Generally magnet designers use as a criterion for temperature differentials a conservative value of 40 to 50 K temperature difference between any two sections of the magnet [15]. This criterion is usually relaxed for quench phenomena to maximum hot spot temperature of between 100 and 150 K. Our proposal, which has been confirmed as reasonable by J. R. Miller [16], is to ramp a current resistively through the TF magnet's stabilizer. Isolation from the cryogenic system obviously must take place for this option. Figure 26 shows such a ramped current that has been profiled in time using a suitable code developed for just this purpose. The current can be tailored to give any anneal temperature desired and for any duration desired. This current, of course, is quite power consumptive. This fact, coupled with the fact that the reactor has to be off-line during a magnet anneal, presents the reactor designer with a significant cost penalty. This penalty, as will be explained later in this dissertation, must be compared with other alternative option penalties in order to optimize the system as a whole.

Fig. 26: Typical current profile to give an anneal temperature of about 750° C for 5 hours for ITER EDA magnet design.

Additional concerns regarding high temperature anneals are met with respect to insulation. Both the commonly used electrical and thermal insulation materials are precluded in the event of superconductor anneals. Typically, glass fiber reinforced epoxies or polyimides are used for electrical insulation for the magnet, both turn-to-turn and turn-to-ground. Epoxies can only withstand temperatures of about 400° C before they begin to char [4]. This fact makes development of the vanadium based A-15 superconductors much more appealing due to their low anneal temperatures (which could be as low or lower than 400° C). These superconductors do not have a commercial base as yet, though. Thus, inorganic insulators or polyimides must be used for magnets intended for anneals. While inorganic insulators are not generally in use, they have been designed into the ITER EDA magnets to be used as both turn-to-turn and turn-to-ground electrical insulation. ITER EDA specifies, however, the use of

epoxy to "fill in the gaps" and give mechanical rigidity to the magnet. A question remains as to the necessity of this epoxy. No experimentation has been done to show that it is necessary, and it is far from clear how beneficial the epoxy would be to stability. We must assume the non-use of epoxy for coils suitable to be annealed. This assumption seems reasonable in the absence of any data to the contrary. If it is found in the future to be an inappropriate assumption, a penalty in the limit imposed upon the stability margin may be imposed. Otherwise it may be possible to use polyimides. Polyimides have been experimentally observed to resist damage to at least the melting temperature of aluminum (633° C) [4]. These insulators would eliminate any concerns regarding the mechanical rigidity of the magnet, since they are mechanically very similar to epoxies. Polyimides are more radiation resistant than epoxies by a factor of about five to ten. They are expected to be significantly less resistant than inorganic insulators, however. With regard to cost, epoxies are the least expensive, then polyimides, and finally inorganics are the most expensive [17]. More will be discussed concerning these costs later in the dissertation.

Not only must inorganic insulators likely be used for electrical insulation, but copper or aluminum shrouds must be used as thermal shields. Multilayer insulation (MLI) composed of aluminized mylar is typically used as a thermal shield for magnets, isolating the magnet from the room temperature atmosphere. MLI is inexpensive (about \$13/ft²) and fairly dependable. It may have a temperature tolerance suitable for use in a magnet system which will be undergoing anneals, but it is not known at this time whether or not this is the case [4]. Copper shrouds have been commercially used on large magnets, including the SMES PoP coil. Shrouds cost more than MLI due to the following factors: liquid nitrogen cryogenic system; time intensive design of tubing in order to achieve temperature uniformity; and time intensive labor costs of brazing (or welding) the tubes to the sheet. These costs are not to be viewed as

prohibitive, however, since there are certain systems which have employed them. Shrouds give the advantages of requiring a less demanding vacuum (10^{-3} atm as opposed to 10^{-4} atm for MLI) and being generally more dependable [4]. The costs will be discussed further later in the dissertation, but the main point is that such an annealing schedule (high temperature) is compatible with magnet design, as has been pointed out earlier in the thesis.

With respect to the ability of the stainless steel or incoloy structure to undergo a high temperature superconductor anneal without serious degradation to its mechanical characteristics. It is recommended that these materials not be used in high stress componentry at temperatures greater than about 600° C. This also happens to be the temperature at which the high temperature superconductor anneals would be carried out. At the time of the anneals, however, the coils will be under very little stress. The magnets will be uncharged, and the temperature gradients will be rather small since the temperatures will be relatively uniform across the coil during an anneal. Thus, from a materials properties viewpoint, high temperature superconductor anneals have been demonstrated to be thoroughly compatible with good magnet design.

Additionally, an intriguing potential exists for coils which will be subject to high temperature anneals. A special one to two turn grade representing the inner one or two turns of the TF coil may be designed significantly different with respect to conductor parameters. This single or double turn grade layer could have its conductor designed to take full advantage of the appealing features of superconductor annealing schedules. Such a design may further reduce the cost of the magnet. This option will be explored further later in the dissertation.

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PART III: REACTOR INTEGRATION

CHAPTER 7: CRYOGENIC CONCERNS

A. *Cryogenic Concerns:*

Another area which will impact the optimized magnet system design, and hence the cost, is the cryogenic refrigeration system. At first glance it would seem that this component of the magnet coil system would not play a very important role in optimization. However, as it was shown above, the stability of the coil itself is quite dependent upon the bath temperature. This bath temperature is a function of two primary variables among other lesser important variables. The two most important variables governing the bath temperature, which is the bottom-line purpose of the refrigeration system, are the helium mass flow rate at a given inlet temperature (or, equivalently, the refrigeration power, related to this mass flow rate through the refrigeration-liquefaction coefficient) and the nuclear heating rate. As one can readily discern, if the shielding is increased in order to give the magnets more protection, the nuclear heating rate also will drop, thus further increasing the stability margin of the coils by lowering the bath temperature of the forced flow helium coolant. Likewise, if one were to design a thinner shield, deciding for example to anneal the magnets and use inorganic insulators, not only is the stability margin degraded more quickly by the more rapidly changing critical superconducting parameters, but also the helium bath temperature is increased, further degrading stability. Therefore, it should be apparent

that the optimization of the magnet system will include the refrigeration system as an essential component to be optimized.

Additionally, the cryogenic system determines the cool-down time for the coils. The cool-down procedure is a very long one for such enormous coils. Recent estimates for the cool-down time of the ITER CDA TF coils have been given which are on the order of a several hundreds of hours [1]. Down times of nearly a month in the event of a quench will be judged to be unacceptable or at least unattractive not only by experimentalists (such as those working on ITER), but even more so by utility companies (using reactors such as ARIES). This large penalty associated for unexpected coil quenches must be incorporated into cost of electricity, and thus determining the cool-down time as accurately as possible is essential. Obviously, the more powerful a cryogenic system the coils have, the less the cool-down time is required in case of a quench, but higher capital costs are incurred. This economic penalty for this down-time due to cooling the magnet mass will also have to be included in any annealing study. For superconductor anneals to be attractive, the annealing schedule would have to at least partially overlap otherwise scheduled down-times, such as divertor replacement schedule or first wall replacement schedule. This requires a very detailed and machine-specific analysis, and will be performed by *MagRad*. Availability cost penalties will be discussed later in the work (Ch. 9).

MagRad has incorporated a modification of A. E. Long's code *Cable-in-Conduit Superconductor Design Tool*® as the basis for the determination of the steady state thermal-hydraulic characteristics of the magnets. Two of his original five modules have been incorporated into *MagRad* after suitable modification had been made. These modules, together with the appropriate input section in *MagRad* are collectively named *RadHeat*. *RadHeat* is primarily a heat transport code "specifically designed to simulate the dynamic thermal response of the supercritical helium coolant in one-dimensional

space and time to any given set of pulse-type heat loads" [2] . Values that are calculated include the mass flows, helium flow velocities, Reynold's numbers, etc. These values are then iterated, given appropriate hydraulic boundary conditions, to yield the temperature and pressure at any point along the length of the conductor. *RadHeat* is also able to handle geometries with or without central core helium flowpaths. In fact, it was for the purpose of modelling the central core helium flowpath of the tentative ITER EDA design that Long's original code was developed. *RadHeat* is designed to efficiently and accurately determine primarily the operating temperature at any point along the conductor, but especially at the point of highest instability. In the case of layer winding, adopted for both ITER and ARIES, this point of highest instability will be located at the hydraulically last position where the inner turn of the magnet intersects the inboard midplane of the machine. Thus the helium will have been exposed for the longest time (hydraulically last position) to the highest heat flux (inner turn, inboard midplane). There does exist the possibility, depending upon neutron streaming effects, that the point of highest instability may be located behind a divertor, but this will make very little difference unless the streaming is extreme.

Some assumptions have been made in the implementation of Long's modified code in *MagRad*. These assumptions are as follows: mass flow rates are at each location and for each time step constant (i.e. compressibility effects are ignored in this regard); the axial pressure gradient is linear; the heat transfer between the cable and the helium is infinite, thus giving $T_{\text{cond}} = T_{\text{He}}$ (temperature differentials will be insignificant except in the case of instability, which is analyzed elsewhere in *MagRad*); and the cable heat capacitance is negligible relative the the heat capacitance of the surrounding helium. These assumptions are consistent with a high degree of accuracy as evidenced by the use of this code for official ITER work. These assumptions, moreover, are

consistent with rapid calculation, which in the case of design optimization and subsystems trade studies is of great importance. Transverse heat transfer between central core helium and interstitial helium is taken into account, and represents a primary draw upon computing time. Joule-Thompson heating and cooling due to compressibility effects are also accurately accounted for in the code.

Finally, the input for *RadHeat* includes the nuclear heat load. This heat load, of course, varies quite dramatically along the poloidal direction. This is accurately accounted for by the use of a scaling rule relating the nuclear heating to the neutron/gamma flux coupled with an analytic expression designed to match the peaking factor of a generic blanket (the ITER CDA nuclear heating profile was used for this analytical expression). The poloidal variation of the nuclear heating along one turn of a typical TF coil is shown in Fig. 27. The values used for this plot were obtained from

Fig. 27. Plot of poloidal TF nuclear heating in W/m along one turn of the coil (using the double null plasma core configuration).

the ITER CDA, but the shape of the variation is representative of almost any tokamak machine with a double null, double divertor configuration. Clearly, a machine configured for single null, single divertor would have only one of the twin nuclear heating cusps on either side of the inboard heating peak. The analytical equation that represents this plot of the nuclear heating in the TF coil and is used in MagRad is,

$$Q_z = a \cdot \exp(-4 \cdot (x - x_1)^4) + a \cdot \exp(-4 \cdot (x - x_2)^4) + c \cdot a \cdot \exp(-y(x)) \quad (49)$$

where,

$$y(x) = 24 - 0.66 \cdot x - 0.0053 \cdot x^2 + 0.00024 \cdot x^3 - 0.0000012 \cdot x^4 \quad (50)$$

and where a is a constant dependent upon the shielding of the reactor and equal to the inboard peak nuclear heating (W/m), c is also a constant dependent upon the shielding and represents the ratio of the peak nuclear heating behind the divertor to the inboard peak nuclear heating (W/m), x is the fractional poloidal position with the origin defined as the midplane of the outboard section of the coil (in units of percent of the total coil turn), x_1 is the fractional poloidal position of the lower divertor (% of turn), and x_2 is the fractional poloidal position of the upper divertor (% of turn).

Depending upon the particular design of the coil, the warmest point along the conductor may be exposed either to a very low field or a very high field. The warmest point of the conductor will be exposed to the highest magnetic field in the case that the coil is shell wound. In shell type winding, the coil is wound one shell of turns at a time (toroidally), thus the flowpath for the helium is along one entire shell. In this case the conductor will experience a very high magnetic field (inner turn of coil) and a relatively warm operating temperature near the end of the helium flowpath. The

warmest point of the conductor will be exposed to a lower magnetic field in the case of a pancake wound coil. In pancake type winding, the coil is wound in such a way that each successive turn is radially wound onto the previous turn, thus the flowpath for the helium spirals out radially. In this case the conductor will experience the peak magnetic field in a relatively cool portion of the magnet, assuming that the helium inlet is at the inner turn. Both winding configurations are available in *MagRad* in the modified *RadHeat* module. In the shell wound configuration the peak nuclear heating drops off exponentially through successive shells (remaining constant along the flowpath of successive turns), while in the pancake wound configuration the nuclear heating drops off exponentially through successive turns (remaining constant for all pancakes).

The primary advantage of the shell wound configuration is that it becomes possible to grade the conductor. This grading of the conductor allows the designer to take advantage of the lower nuclear heating, and hence lower operating temperature, in the outer shells by adopting a lower grade wire. The lower grade wire has a higher fraction of copper, and thus costs less.

In addition to the design of the steady state forced flow cryogenic loop for the cooling of the TF conductors internally, there also is the matter of designing the heat shields to the external environment. These heat shields must, of necessity, be constructed out of copper or aluminum, since the heat shield itself must tolerate high temperature anneals. High temperature annealing precludes the use of aluminized mylar as superinsulation, thus radiative losses to the environment must be mitigated solely with the use of metal/helium heat shields. Such a shield might be composed of sheets of aluminum upon which would be welded aluminum tubes through which a side-stream of helium gas is passed. Since aluminum loses too much strength at the high temperature anneal (with a yield stress of about 5 MPa at 400° C), copper sheets can just as easily be used (at lower cost) with brazed copper tubing (with a yield stress

of about 30 MPa at 600° C). Copper is taken as the base case thermal shield material in direct analogy to the base case SMES design which uses aluminum as its material.

Between sheets is a rather low-grade vacuum (aluminized mylar requires a vacuum of 10^{-4} atm as opposed to 10^{-3} atm for the copper shroud). Included in the cost of a copper shroud of this type would be the design effort to maintain fairly even temperature profile across the sheets cooled by the tubes, the labor time to braze the tubes to the sheets, and the cryogenic cost of either utilizing a side stream of the helium from the forced flow cooling of the conductors or a secondary liquid nitrogen system. More on the costing of the heat shields will be given in the Ch. 9. The typical design of an aluminum heat shield was taken from a recent Superconducting Magnetic Energy Storage (SMES) proposal [3]. A typical copper heat shield would be design very similarly to the one given in that proposal. In the particular design given in the SMES report, two shields, one operating at 28 K and one operating at 80 K, are used, but a little mylar is also used. It is suggested that if a triple or quadruple shield design were employed, it would more than eliminate the need for the additional mylar.

Assuming that this thermally reinforced SMES-type shield design will give comparable results to the actual SMES design, with losses scaling according to the surface area of the shield, the radiative losses for the ITER CDA TF coils are calculated to be about 9.4 kW, as opposed to the specified 6 kW in the ITER documentation [3]. The conductive losses are calculated (with a safety multiplier of 4) to be about 2.5 kW, somewhat lower than the 4 kW specified by ITER (which uses mostly mylar to thermally shield the coil) [3]. Nevertheless, the ITER specified conductive load of 4 kW is used here, in order to be conservative in the design. These heat loads may be added to the other heat loads imposed upon the cryogenic system in order to determine the total heat load. The heat loads upon the cryogenic system include the following: radiation to the environment; conduction to the gravity supports (where the thermal

shield is attached to the machine); conduction to the thermal shield support rods (stainless steel with epoxy at the cryostat-end attachment base); heat dissipation at the current leads; neutron heating; AC heating in the PF magnets; AC heating in the PF structure; AC heating in the TF cases; AC heating in the TF windings; AC heating in the intercoil structure; pumping losses; and, finally, radiative and conductive losses in the cryogenic piping [3]. All of these heat loads are accounted for in *MagRad* and scale accordingly to the various machine component sizes.

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CHAPTER 8: SHIELDING CONSIDERATIONS

A. Shielding Considerations:

It is important in the optimization of the TF coils to accurately account for variations in the blanket and shield design. The radiation energy spectra, fluxes, and fluences are determinative of the degradation in magnet characteristics. The radiation energy spectra has already been shown to be very similar in all tokamak reactor designs utilizing the D-T fuel cycle. However, gamma and neutron fluxes and fluences differ greatly from design to design, depending upon how thick the blanket and shield components are and what materials are used for these components. These two parameters, namely blanket and shield thickness and composition, must be accounted for in optimization studies. The difficulty lies in the complexity of accurately predicting the fluxes and fluences incident upon the magnet, after having passed through the blanket and shield assembly.

Of course, the most accurate predictions require a full three dimensional neutronics analysis on the reactor as a whole. This type of analysis, in turn, requires knowledge of the plasma pressure, particle, and temperature profiles, in order to adequately model the neutron source itself. Such an analysis is certainly well beyond the scope of this thesis. In fact, even were the plasma profiles known exactly, three dimensional neutronics analyses incorporating all necessary variables and changes in design of the blanket and shield would be far too time consuming both in terms of real time and in terms of computer time. Three dimension neutronics analyses are performed only upon designs that are quite close final versions. During a period of rapid design change, such as the ITER conceptual design and engineering design phases, typically, one dimensional neutronics analyses are performed for the candidate

designs, comparing their shielding capabilities. It is not possible, though, to modularize a one dimensional neutronics package (like ONEDANT®) in order to incorporate it into this thesis. Neutronics packages are very large and time consuming, both attributes precluding the fast and simple thrust of the computer coding of this thesis.

Since these types of analyses are not available to use, what must be done is develop generic design scaling laws. Shown in Tables 8 and 9 are examples of such laws, used to predict the effects of shielding variations such as: blanket structural material option; blanket coolant option; coolant volume fraction in the blanket; vacuum vessel material option; special shielding layer materials options; and all thicknesses. Many combinations of these options, of course, are not self-consistent or have not been sufficiently analyzed in the past. Such combinations are not available for integration into the optimization process. It is, however, quite straightforward to see how these or other design options may be incorporated into the code in the future when specific data become available.

Input into *MagRad* must include a reference blanket/shield/vacuum vessel design in order to make accurate calculation of the first wall fluence. This also allows the direct comparison of fluences and nuclear heating rates between different designs of varying thicknesses. Neutron attenuation is normally calculated using,

$$\phi = \phi_0 \exp(-t \cdot \lambda) \quad (51)$$

where ϕ is the neutron flux (or fluence) at the back end of the component, ϕ_0 is the neutron flux (or fluence) at the front end of the component, t is the thickness of the component, and λ is the attenuation coefficient of the component. However, although the neutrons pass through the blanket and shield assembly radially outward (generally speaking), attenuation factors do not simply multiply to give a composite attenuation

coefficient. The reason for this non-linearity is due to the fact that the various materials in the blanket and shield have attenuation coefficients which are neutron spectra dependent; that is, a material may have one attenuation coefficient at a particular location in the blanket, and an entirely different coefficient in the shield (or even at different positions within either the blanket or the shield). Neutronic reflection in various materials also affects the overall shielding performance of the blanket and shield assembly.

Thus, determining the proper overall blanket and shield attenuation coefficient is not simply a matter of calculating the compositionally weighted ("smeared") attenuation coefficient. Rather, the composite values for attenuation coefficients are based upon specific designs of specific thicknesses. For example, a steel/water shield design (such as the ITER CDA design) obviously gives a different attenuation of neutrons than does a steel/sodium design, since water moderates the neutrons more than lithium does. More illustrative, though, is the case of a blanket and shield assembly of the ARIES-IV type. The ARIES-IV blanket and shield assembly is a rather complicated layered mix of materials. One cannot just add up the thicknesses of the various materials and, together with their respective attenuation coefficients, and develop a composite attenuation coefficient based upon compositional fractions of the various materials. Instead, one must simply rely upon numerical simulation to determine the overall attenuation coefficient, and specify that the blanket and shield design cannot deviate significantly with respect to the layered placement of the various materials. In other words, one must take the blanket and shield design as a lumped parameter, calculating the attenuation coefficient by using the first wall flux (or fluence) and the flux (or fluence) at the vacuum vessel. Adopting this method does, however, preclude the radical changing of any blanket and shield design. Since this

type of radical redesign of the reactor is not the object of *MagRad*, the adoption of this particular method for calculating neutronics characteristics is quite acceptable.

As an example of acceptable redesign as opposed to unacceptable redesign, consider the following scenario. A study has been performed [1] in which an HT-9/Li blanket and shield is compared to a V/Li blanket and shield (among other options as well). *MagRad* very accurately predicts fluences and nuclear heating rates in the two different designs, and does so even when the liquid metal fraction is varied. The two designs, however, are exactly alike except for the replacement of one structural material for another. When this is not the case, as in another recent study [2], predictions are much less accurate, and can be, in fact, very poor. In the case in which *MagRad* returns poor predictions, the placement of materials is very different, and some differing materials (i.e. Beryllium) are incorporated into one of the designs. Thus the two designs are not similar at all, including their using different structural materials. This case of a somewhat radical blanket and shield redesign, can only be handled with proper neutronics codes (such as ONEDANT®, etc.). The use of *MagRad* to predict differences in shielding performance in such cases is very ill-advised. With this limitation in mind, however, *MagRad* is a very useful tool when small design modifications are studied, such as when the blanket and shield design remains relatively unchanged except for the overall thickness. In such cases *MagRad* affords the designer a quick and accurate way to mesh magnet design with blanket and shield design.

Table 8: Neutron Attenuation Coefficients for Various Blanket and Shield Options
(after [1, 2]).

<u>Blanket/Shield Material</u>	<u>Attenuation Coefficient (cm⁻¹) with Water or Helium Coolant</u>	<u>Attenuation Coefficient (cm⁻¹) with Liquid Metal Coolant</u>
Copper	5.41	7.83
Inconel	6.19	8.99
Steel	7	9.13
V5Cr5Ti	9.15	11.44
Ti6Al4V	15.64	17.62
V/Li/Tenelon/B ₄ C	7.65	9.72
SiC/Li ₂ O/Be/B ₄ C	13.88	15.64

The values given in Table 8 for the attenuation coefficients with water or helium coolant seem to work well for the range of coolant percentages of 5% to 25%, while those with the liquid metal coolant are valid over the range of coolant percentages of zero to 50%.

Table 9: ARIES-II/IV Inboard Blanket and Shield Compositions [3, 4].

<u>Component</u>	<u>ARIES-II</u>	<u>ARIES-IV</u>
Blanket:	20 cm 10% Vanadium 90% Lithium	35 cm 17% SiC Structure 37% Lithium Oxide 10% Beryllium 36% Helium
Reflector:	15 cm 15% Vanadium 10% Lithium 75% Tenelon	N/A
Primary Shield:	47 cm 15% Vanadium 5% Lithium 80% Tenelon	64.5 cm 15% SiC Structure 5% Helium 80% SiC Filler
	23 cm 15% Vanadium 5% Lithium 80% Boron Carbide	30 cm 15% SiC Structure 5% Helium 80% Boron Carbide
Composite B/S:	105 cm 14% Vanadium 22% Lithium 47% Tenelon 17% Boron Carbide	129.5 cm 16% SiC Structure 40% SiC Filler 10% Lithium Oxide 3% Beryllium 18% Boron Carbide 13% Helium

B. References for Chapter 8:

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CHAPTER 9: COSTING CONSIDERATIONS

A. *Magnet System:*

Costing for the magnetic field coils consists of two components: the poloidal field coils (including the central solenoid) and the toroidal field coils. In costing the poloidal field coils, a base cost is assumed consistent with the size of the machine, which is then scaled accordingly as the size of the coils decrease or increase from the base case. The costing given is consistent with a code which is still in progress at Lawrence Livermore National Laboratory (LLNL) by J. Perkins, called *SUPERCODE* [1]. This code is a reactor costing code, and includes all machine components, buildings, etc. It is assumed by *MagRad* that the PF coils use NbTi as the superconducting material, while the CS coils use Nb₃Sn.

The toroidal field coil system is costed in much greater detail, since optimization of this system in particular represents the main object of the thesis. Costing of the TF coils is consistent with the costing rules used by J. Schultz [2] in many respects. Costs that are taken which agree with Schultz's costing scheme are: \$78 per kilogram of casing (assuming a stainless steel); \$0.25 per meter of conductor per strand of conductor for cabling; \$40 per meter of conductor per unit strand surface area for coating (for AC loss reasons); \$33 per kilogram of jacket material (assuming incoloy); \$60 per meter of conductor for encapsulation of conductor in foil; \$120 per kilogram of cable and conduit for winding of the magnet; \$2000 per joint; \$2 per Amp of operating current per lead; \$0.05 per meter of busing per Amp of operating current; \$78 per kilogram of intercoil structure (assuming a stainless steel); and \$5 per kilogram of coil for welding, potting, and electrical testing.

Some magnet costs have rather less straightforward scaling rules. Several of the costs scale with the 1/5 power of the total stored magnetic energy, which gives a good indication of the size of the fabrication supports and containment required. These costs would include: the cost of setting up for strand coating; the cost of setting up the tooling for conductor insertion into the jacket; the cost of setting up the tooling for the winding of the magnet; and the cost of the heat treatment of the magnet. Of these costs, the winding tooling cost is by far the most expensive being on the order of about \$1.8 M for an ITER-type toroidal field coil system. Transportation costs are accounted for, as well as the costs both to provide extra material and dummy coils for testing.

There are a few costs incorporated into *MagRad* which do not agree with Schultz's treatment of magnet costing. [2] The cost of the superconducting strand seems somewhat too low in his treatment. He takes the cost to be a flat \$450 per kilogram of strand, with pure copper strands being costed at \$20 per kilogram. Now while the \$20/kg is a reasonable cost for the pure copper strands, the \$450/kg seems too low for the superconducting strands. Moreover, the superconducting strands are a composite of copper and Nb₃Sn (basically), yet he shows no cost scaling with regard to the fraction of copper contained in these composite strands. However, in discussion with an industrial supplier of composite wire [3], the cost was claimed to be significantly higher and dependent upon the copper fraction. The scaling law which was proposed by this supplier is the following,

$$Cost_{strand} = \$20 / kg \cdot m_{pure\ Cu} + (\$900 / kg - \$500 / kg \cdot f_{comp\ Cu}) \cdot m_{composite} \quad (52)$$

where $Cost_{strand}$ is the overall cost of the cable, $m_{pure\ Cu}$ is the mass of the pure copper wire (in ITER CDA this is zero), $f_{comp\ Cu}$ is the fraction of the composite wire that is copper (usually around 60%), and $m_{composite}$ is the mass of the composite wire. This

scaling law had been derived for the ITER project, and is the scaling law which is used in *MagRad*.

Modifications had to be made in the present scaling laws of *MagRad* to incorporate high temperature anneals of the superconductor. In a magnet which will not undergo any superconductor anneals the cost scaling of the insulation matches that used by Schultz, namely \$600 k per cubic meter of insulation. If, however, superconductor anneals are employed, the cost scales as \$50 per linear meter of conductor plus \$600 k per cubic meter of insulation. Schultz's scaling is based upon the use of glass reinforced epoxy as insulation with a vacuum impregnation of the epoxy. The scaling in *MagRad* (for superconductor anneals) is based upon the use of a ceramic insulating sleeve wrapped around the conductor jacket and subsequently vacuum impregnated with polyimide. The vacuum impregnation in the annealing case is as costly as the entire insulation of the non-annealing case due to the fact that, though the annealing case does not have the glass reinforced epoxy cost, the polyimide used is more expensive than the epoxy used [4, 5]. The \$50 per linear meter cost for the annealing case represents the cost of the ceramic sleeve, as quoted by the industrial manufacturer [6].

By far the most expensive component (both absolutely and in \$/kg) in the magnet system is the actual composite strand that is used. This cost typically represents about a third of the total cost of the magnet system. More will be said of the economic trade-offs within the TF magnet system itself in the results sections of the thesis. If only the cost of magnets were taken into account, though, it would obviously be advantageous to employ the highest copper fraction permissible in the strand.

One very interesting cost scaling which has direct impact upon the cost of the magnets is that as the blanket and shield thickness is decreased, the size of the magnets decreases significantly. This result can be seen very clearly as the combined effect of

two distinct factors. The more obvious of the two effects is that as the size of the blanket and shield is decreased, the magnet size also decreases (simple geometry). The magnet size decrease is proportional in a general sense to the average radius of the coil, which decreases as the blanket and shield thickness decreases. The second effect stems from the fact that as the average radial distance from the center of the plasma is decreased, the number of turns required to achieve a given on-axis magnetic field is decreased. The decrease in the number of turns, for a given conductor current and on-axis magnetic field can be approximated by Ampere's Law to be roughly proportional to the average radius of the coils. These turn lengths may be taken off of either the radial back of the magnets (especially in the case of pancake wound coils) or the toroidal width of the magnets (especially in the case of shell wound coils), reducing the flowlengths of the coil. Thus, the size of the coils decreases by another factor proportional to the average radius of the magnets. Combining the two effects, it can be readily seen that the size of the TF magnets will scale with the square of the average radius of the magnets.

Although this may seem to be a small effect, given that the thickness of the blanket and shield will not decrease drastically (on the order of about ± 20 cm at most, as will be shown in the case studies chapter), this decrease in magnet size can actually be quite significant. As a simple example consider a perfectly circular coil (unlike the TF magnets) of a radius of about 4 meters (1 meter of which is blanket and shield). If the blanket and shield thickness were decreased by about 10 cm, the coil circumference will decrease over half a meter and the number of turns will decrease by almost 3 %. If the coil is composed of about 24 flowpaths and 10 turns per flowpath (as is the ITER CDA), these reductions translate into a reduction in the total length of conductor needed (for 16 coils) from about 96,573 meters to about 91,745 meters. At a cost of about \$10 k per meter of coil, this reduction of 10 cm in blanket and shield thickness

would correspond to a savings of about \$48 M for the TF magnets alone. Thus, the significance of overall reactor size can be seen.

B. Cryogenic System:

The costing of the refrigeration system has been carried out using suitable scaling laws. The most recent of these laws are given in [7]. The first of these laws relates the capital cost, C_r , in millions of dollars, to the refrigeration produced, R , in kW, for toroidal magnets, and is,

$$C_r = 1.51 \times R^{0.7} . \quad (53)$$

The second of these equations gives the maintenance cost, C_{main} , in millions of dollars per year, as a function of both the refrigeration, R , and the cost of electric power, P , in dollars per kWh, and is,

$$C_{\text{main}} = 2.72 \times R^{0.78} \times P^{0.56} . \quad (54)$$

It is also worth noting that scaling laws have been given in the referenced paper for the capital cost of the toroidal field magnets themselves, as well. They are as follows,

$$C_m = 2.499 \times E^{0.342} \quad \text{or} \quad C_m = 2.588 \times \Omega^{0.391} . \quad (55)$$

where C_m is the capital cost of the magnet in millions of dollars, E is the stored energy in MJ, and Ω is the field-magnetic volume product in Tm^3 . It can easily be shown that these scaling laws yield values for the cost of the magnets themselves that are almost an order of magnitude too low for systems such as TF fusion magnets. The probable explanation for this large discrepancy is twofold. In the first case, it is never remarked in the above paper whether NbTi, or Nb_3Sn , or both types of magnets are considered.

A Nb_3Sn magnet will cost much more than a comparable NbTi magnet. Then in the second case, the data from which the above scaling rules are taken include magnet data up to only 3 GJ of stored energy, whereas TF fusion magnets typically have stored energies of about 50-100 GJ. These serious limitations do not, however, invalidate in any way the scaling rules developed in the above paper for the cryogenic systems, since the authors use cryogenic system data up to about 40 kW of refrigeration, which is quite close to that required for fusion systems. Moreover, whereas the TF magnets for fusion systems will involve a very large step forward in actual magnet production technology, the cryogenic systems for these magnets do not involve such a large large step.

C. Blanket, Shield, and Vacuum Vessel Systems:

The costing of the blanket, shield, and vacuum vessel systems are quite straightforward. This costing is simply a matter of estimating the volume of each particular component, and using densities and costs per kilogram to determine the total cost of each component. Volumes are estimated by treating the blanket and shield structure and a single component (as they are in ITER CDA) and the vacuum vessel as another separate component. Each of these components are further broken down for volume calculations into inboard, outboard, top, and bottom sections. The inboard and outboard sections are modeled as cylinders of given thicknesses, while the top and bottom sections are modeled as discs of given thicknesses. In order to cut down on the amount of input required by the user of *MagRad*, the outboard, top, and bottom section thicknesses are scaled with the thickness of the inboard section. The bottom and top sections are taken to have thicknesses of about 0.6 times the inboard thickness, while the outboard section is taken to have a thickness of about 1.8 times the inboard thickness. These scalings are consistent with the ITER and ARIES designs.

Actual costs of the various materials which may compose a blanket, shield, or vacuum vessel can be found in Table 10. These costs have been taken from ITER and ARIES designers [8], in order to remain consistent with current reactor designs. Obviously, the more exotic the material used, the more difficult it is to manufacture in high tolerance, large segments, and the higher the cost. These types of materials, such as those used in the design of the ARIES reactors, will yield reactors with high economic sensitivity to blanket and shield thicknesses. Most vacuum vessels are fabricated out of some sort of steel. Thus, the cost of that particular component will remain relatively constant between various designs.

Table 10: Costs of Various Material Components of Blankets, Shields, and Vacuum Vessels.

<u>Material</u>	<u>% Theor. Density</u> (%)	<u>Theoretical Density</u> (kg/m ³)	<u>Unit Cost</u> (\$/kg)
316 Stainless Steel	100	7800	35
Tenelon	100	7800	25
HT-9 (modified)	100	7800	70
Incoloy	100	8900	55
V5Cr5Ti Alloy	100	6100	300
V5Cr5Ti Alloy Filler	100	6100	70
SiC/SiC Composites	95	3040	400
SiC Filler	100	3200	70
Ti6Al4V Alloy	100	4500	330
Boron Carbide Filler	90	2250	200
Lithium	100	500	50
Lithium Oxide	90	2000	70
Beryllium	90	1850	620
Copper	100	8960	45

Note: Costs given are as fabricated.

D. Annealing:

Costs associated with any type of annealing schedule include the following: the cost of the power required (if any) to raise the conductor's temperature to that necessary for the anneal; the cost of re-cooling the magnet; and the cost of lost time (if any). These costs will, in general, be quite different for an experimental reactor and a power reactor. In fact, for ITER-type experimental reactors, it is assumed that there is no economic penalty for down-time, as long as this down-time is not excessive. Excessive must be understood in light of the very low availability of such a machine (25% for ITER). Down-times of at least several consecutive weeks (and in some cases, months) are planned on a regular schedule for ITER in order to change out divertors/blanket modules and in between consecutive experiments. This type of relatively relaxed up-time schedule affords the possibility of at least one high temperature anneal to the coils per year. In fact, it is already assumed in the ITER CDA that there will be about 10 room temperature anneals of the TF coils over the scheduled 14 year life of the machine. Room temperature anneals are nearly as time consuming as high temperature anneals, since the dominating time factor for anneals is the re-cool time. Re-cooling from high temperature to room temperature can be done fairly quickly, as opposed to re-cool from room temperature to 4 K. This low temperature (300 K to 4 K) re-cool time has been calculated for ITER to be on the order of about 200 hours.

On the other hand, annealing times (dominated by re-cool times) are of crucial importance for power reactors. With availabilities on the order of 75%, longest scheduled down-times do not exceed about three weeks (and this, once per year) [9]. Any kind of annealing schedule must coincide with blanket module replacement schedules, or heavy economic penalties will result. In the above study, it has been

noted that the irreducible availability of any kind of reactor is about 20%: 13% for auxiliary plant unavailability and 7% for unscheduled unavailability. It has further been calculated that it requires 20 days to replace one blanket sector, and increasingly longer (non-linearly) to replace multiple blanket sectors. It is anticipated that D-³He reactors (like ARIES-III) may achieve availabilities of up to 80%, not ever having to change out blanket modules, whereas D-T reactors will probably only achieve about 70%, having to change out about four blanket sectors per year. In the D-T case (which is the one examined in this work), the down-time to change out a sector is at least three consecutive weeks per year. During this time, not only could the auxiliary plant maintenance be performed, but either high temperature or room temperature anneals could be performed in parallel. However, any more than one anneal per year (which includes both high temperature and room temperature anneals) would draw serious economic penalties. These penalties would correspond roughly to the re-cool time in days multiplied by the amount of revenue lost per day of reactor down time. It is difficult to envisage more than 37 anneals over the life of the reactor, though. Thus, economic penalties due to down time for annealing will, in all likelihood, be avoided for all but the most aggressive (with respect to annealing schedules) power reactor designs.

For room temperature anneals little or no power is required to bring the magnets up to the annealing temperature. In the case of experimental reactors, the extremely low availability precludes the necessity of quick warm-ups, and in the case of power reactors, at least a week is allowed for the magnets to warm up (since two weeks will be more than sufficient to re-cool the magnet to 4 K). If this time allowance is found to be restricting, a very small current could be driven through the (now normal) conductor to expedite matters. Such a small current, as will be seen later when compared to a

relatively large anneal current, will require very little power. The warm-up for room temperature anneals, then, will have virtually no cost associated with it.

The case is different for high temperature anneals, though. The power associated with the temporal current profile required to achieve the desired anneal temperature can be quite significant. The power required is calculated (as well as the current profile) by *MagRad*, given the proper annealing parameters which include: warm-up time; anneal duration; ambient and anneal temperatures; and the convective heat transfer coefficient to the surroundings (taken to be, in these studies, a standard $0.133 \text{ W/m}^2\text{-K}$ for case to air). This warm-up and anneal current electric power competes with the re-cool costs as the dominant cost. Other costs are taken to scale similarly to magnet costs. While scaling is similar to magnet costs, multipliers are introduced as safety factors due to the lack of data from which to benchmark. The engineering cost is taken to scale with the cost of annealing current power as the magnet engineering cost scales with the cost of magnet hardware, except that a factor of roughly three is introduced as a safety. A design cost for annealing is introduced which is taken to be equal to half of the engineering cost; this cost has no comparable magnet cost. A research and development cost of one million dollars (flat, one time cost, regardless of how many anneals are performed) is also included. Administrative costs are taken to be significantly higher than magnet administrative costs, again to include a safety factor into the costing. Finally, contingency costs are taken to scale like magnet contingency costs (30% of hardware or electric power cost plus 30% of engineering cost).

Re-cool costs are much more complicated to predict. The reason for this is that re-cool times are very sensitive to many variables. For example, the ITER CDA PF coils, as designed, would require over 600 hours to cool from room temperature to 4 K due to their long flowpaths [10]. However, if parallel cooling channels are introduced

(as is the case for the ITER EDA TF conductor design), that re-cool time decreases to about 200 hours. This extremely large impact is very difficult to model, and requires a full numerical treatment of its own with a code similar to LLNL's *CICC* code. Such extremely time consuming and numerically intensive calculations are not possible with MagRad, but good approximations are most frequently used by magnet designers who need such information [11, 12]. *MagRad* incorporates a separate spreadsheet in the workbook for these calculations, and the costing is based upon the output from this worksheet. Re-cool wall-plug power is calculated as a heat balance at each temperature step. Re-cool times are also given in this spreadsheet.

E. References for Chapter 9:

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PART IV: RESULTS

CHAPTER 10: SYSTEM STUDIES

A. ITER CDA Studies:

In analyzing the ITER CDA design using *MagRad*, it became apparent that there were two distinct areas of possible design modification with respect to the TF magnet system. In general, this two-fold distinction will hold for any reactor. The two areas of design modification may be classified as conductor design and reactor-level design. Included in the conductor design area are variations on the primary variables which specify the conductor:

- fraction of copper in the composite strands
- fraction of pure copper strands in the cable
- fraction of cable in the cable space (or annular region, for conductors with central helium flowpaths)
- geometry of the central helium flowpath

There are, of course, several other variables, which can be analyzed by *MagRad*, but the previous four are the primary design variables for the conductor. As for material choice, ternary Nb₃Sn is assumed to be the superconducting material, since its performance is better than binary Nb₃Sn at moderate fluences (less than 1×10^{19} n/cm²).

Included in the reactor-level design area are variations in:

- coil winding pattern
- high temperature annealing schedule
- blanket and shield thickness

Blanket and shield material options are taken to be the same as that specified for the CDA benchmark design, though the thicknesses are allowed to vary. Along these broad design principles the ITER CDA design was analyzed, with the primary objective of overall reduction of reactor cost. The cost scaling of the reactor (all costs not specifically covered in Chapter 9) is consistent with ITER EDA and SUPERCODE cost scalings, while the cost scalings for the ARIES design is consistent with the ARIES strawman analyses (again, except for those costs specifically covered in Chapter 9).

Conductor Design:

The first variables that come to mind in conductor design are the fraction of copper in the composite strands, $f_{\text{Cu,comp}}$ and the fraction of conductor in the cable space, f_{cond} . These variables will impact both magnet performance and reactor cost. For these two variables, the impact is relatively straightforward. Firstly, as $f_{\text{Cu,comp}}$ is increased, the energy margin of the conductor is decreased, since the current density in the superconducting material must increase proportionately. Also, the maximum temperature in the event of a quench is decreased, since copper has a higher specific heat than the ternary alloy and the resistance of the conductor in the normal state is decreased. The nuclear heating in the conductor, of course, remains constant, since the amount of metal in the conductor remains unchanged. On the other hand, as f_{cond} is increased ($f_{\text{cond}} = 1 - f_{\text{He}}$), the energy margin of the conductor is increased while the maximum quench temperature that is achieved in the magnet is decreased. Obviously, if more helium is put into the cable space (while holding the area of the strands

constant), the operating temperature of the conductor is decreased, thus increasing the energy margin. Additionally, as more helium is put into the cable space (again, keeping the conductor area constant), the larger the heat sink in the event of a quench, thus reducing the maximum quench temperature. The effect of the variation of these parameters upon conductor performance is seen in Figs. 28 and 29.

The impact upon reactor cost is a little less obvious. It must be noted from the start that any incremental change in the cost of a sub-system will result in double that incremental change in the reactor system as a whole. The reason for this doubling effect is that to the reactor direct cost must be added all indirect costs which include: construction services; home engineering; field engineering; project contingency; owner's cost (in some cases); and interest during construction. Adding in these costs amounts to nearly doubling the incremental cost impact to the reactor.

Fig. 28: Variation of Energy Margin with primary conductor parameters.

Fig. 29: Variation of maximum quench temperature with primary conductor parameters.

With those general thoughts in mind, a clearer picture of the impact of $f_{\text{Cu,comp}}$ and f_{cond} can be drawn. Increasing the fraction of copper in the composite strands reduces the cost of the magnet system, since a less expensive material is being used to replace a more expensive material (ternary alloy). Thus increasing $f_{\text{Cu,comp}}$ is favorable in reducing the reactor cost. The reduction is rather modest, though, amounting to about 3.5 % of the cost of the reactor for a increase in the copper fraction in the composite strands of 0.35 (from 0.45 to 0.80). Increasing the fraction of conductor in the cable space, however, increases the reactor cost. This increase is due to two separate effects: the cryogenic load is increased (due to more material in which there is bulk nuclear heating), thus increasing the cryogenic system cost (capital cost only, not operating costs, which do not impact the capital cost of the reactor, and have a minimal

impact in any event); and the conductor volume is increased, thus increasing the TF magnet system cost. These economic effects are shown in Figs. 30 - 34.

Variables which are less obvious than the two primary conductor design variables are the fraction of strands that are pure copper, $f_{\text{Cu,pure}}$, and the area of the central helium flowpath, A_{core} . Magnet designers will generally add pure copper strands to a conductor in order to reduce the anticipated maximum quench temperature by thus increasing the heat sink. A relatively new approach to reducing the operating temperature of a conductor is the inclusion of an internal, central helium flowpath, formed by use of a helical, steel, wire. The wire interface between the helium flowpath and the annular, strand region (which still has helium flowing interstitially) has a porosity of about 50%, which is minimized against physical deformity due to Lorentz forces. This design modification, as stated previously, was incorporated into a recent ITER EDA design. It is interesting to note, however, that the central helium flowpath does not have a very large impact upon the operating temperature of the conductor unless the coil is wound in shell fashion. Shell wound coils experience much higher nuclear heat loads in their inner-turn flowpaths. This is where the central helium flowpath has its greatest advantage.

Fig. 30: TF magnet system (ITER) cost as a function of $f_{\text{Cu,comp}}$ is linear, as expected.

Fig. 31: Total reactor (ITER) cost as a function of $f_{\text{Cu,comp}}$ is linear.

Fig. 32: Cryogenic system (ITER) cost as a function of f_{He} is slightly non-linear.

Fig. 33: TF magnet system (ITER) cost as a function of f_{He} is nearly linear, as expected.

Fig. 34: Total reactor (ITER) cost as a function of f_{He} .

However, even for pancake wound coils, the central helium flowpath adds two extra heat sinks which may be effectively used in the event of a quench: at short times (up to several seconds) the helium adds significantly to the heat balance; and at long times (after about 8 seconds) the steel adds significantly to the heat balance. So, for pancake wound coils, like the ITER CDA design, both the addition of pure copper strands and the addition of central helium flowpath area reduce the maximum quench temperature, T_{max} . This effect can be seen in Figs. 35 - 36. While both of these design modifications have the advantage of lowering the maximum quench temperature, since they add metal to the conductor design, they also increase the nuclear heating. For the central helium flowpath, any area increase more than offsets this increase in the nuclear heating, and the operating temperature is reduced in spite of the higher nuclear heating. However, the addition of pure copper strands to the design does reduce the operating

temperature, thus reducing the energy margin. This reduction in the energy margin is quite modest, though.

The effect upon the cost of the reactor in adding pure copper strands is twofold: the increased heat load increases the cryogenic system cost; and the increased copper volume increases the magnet system cost. In exactly the same way, the addition of central helium flowpath area increases both sub-systems, thus increasing the reactor cost as well. These effects can be seen in Fig. 37 - 42.

Fig. 35: Adding pure copper strands to the conductor reduces $T_{\max}$.

Fig. 36: Adding area to the central helium flowpath also reduces $T_{\max}$.

Fig. 37: Adding pure copper strands to the conductor increases the cryogenic system cost.

Fig. 38: Adding area to the central flowpath also increases the cryogenic system cost.

Fig. 39: Variation of TF magnet system cost with additional copper is non-linear.

Fig. 40: TF magnet system cost increases with increasing central helium flowpath area.

Fig. 41: Total reactor (ITER) cost increases with addition of pure copper strands.

Fig. 42: Total reactor (ITER) cost increases with addition of central helium flowpath area.

Conductor design modification can yield only modest cost reduction. Actually, for the case of the ITER CDA, if the design constraints are followed, changes would have to be made. Namely, the design constraint of $I_{op}/I_{lim} < 0.95$ is not met by the current design at the end of life limits set by the ITER Magnets group. In order to meet this constraint, the conductor design would have to be suitably modified.

Reactor-Level Design:

As stated above, reactor-level design includes: coil winding pattern; high temperature annealing schedule; and blanket and shield thickness. These design parameters tend to affect the overall total reactor cost in a more profound way than simple conductor design parameters. In the ITER CDA the coil winding pattern is a pancake configuration, while in the ARIES designs it is a shell configuration. The winding pattern will have a very large impact upon reactor cost, as will be pointed out later. The ITER CDA reference blanket and shield consists of 316 stainless steel, water coolant, and solid breeding material (Li_2O). The material composition of the blanket and shield assembly will not be varied, but their respective thicknesses will be.

The primary trade-off, then, in reactor-level design will involve a reduction of blanket and shielding material for increasing number of high temperature anneals. This trade-off has been explored for the pancake wound coil configuration, while the shell wound configuration will be explored in ARIES, and comparisons will be subsequently drawn between the two configurations. As can be seen from Fig. 43, the number of high temperature anneals has only a small impact upon the operating temperature, T_{op} , for a pancake wound coil. The reason for this small effect, as has been previously pointed out, is that the point of least stability is on the inner turn of the magnet, which experiences This is unlike the shell wound configuration, since in the latter the inner turn of the coil experiences the peak nuclear heating cyclically along the length of the flowpath. In the pancake wound configuration the flowpath is exposed to an exponentially decreasing nuclear heat load along the flowpath.

However, even for the pancake wound configuration, at a high number of anneals (thin blanket and, thus, high nuclear heating), the conductor must have a small central helium flowpath as an additional heat sink and as a temperature moderator. This

can be seen in the figures (especially Fig. 49) as a marked difference between two regimes: the regime up to about 6 anneals and the more aggressive beyond 6 anneals. The central flowpath allows for a more uniform temperature along the flowpath, since distant parts of the flowpath can communicate on a much quicker time scale. Without this central flowpath, while the inner turn continues to exhibit adequate performance, the performance of subsequent turns is greatly degraded due to the large nuclear heat input. Of course, it is desirable to have as thin a central helix (defining the central flowpath) as possible, since this amount of steel will also contribute to the nuclear heating. The total nuclear heating, the total heat load to the cryogenic system, and thus the cryogenic system cost, all increase nearly linearly with the number of high temperature anneals (see Figs. 44 - 46). The reason, of course, is that, as the number of anneals is increased, the blanket/shield/vacuum vessel (B/S/VV) assembly thickness is allowed to decrease in such a way that the performance (stability, primarily) of the magnet is held constant.

Fig. 43: Nuclear heating rate has only a small impact upon pancake wound coil T_{op} .

Fig. 44: Total nuclear heating in the pancake wound coil versus number of anneals.

Fig. 45: Heat load on the cryogenic system increases for increasing number of anneals.

Fig. 46: Cryogenic system (ITER) cost increases with increasing number of anneals.

The variation of the B/S/VV allowed thickness as a function of the number of anneals performed is shown in Fig. 47. The non-linearity of the data (as in Figs. 44 - 46) merely reflects the fact that the assembly thickness was varied using whole centimeter increments. If the thickness of the blanket and shield module was preferentially decreased over the thickness of the vacuum vessel, the effect upon the cost of the B/S/VV assembly would be a little greater. In Fig. 47, however, the blanket and shield thickness is kept at a constant 45 cm (with only the vacuum vessel thickness being varied).

A decrease of B/S/VV thickness, of course, yields a corresponding decrease in the cost of the B/S/VV. It also reduces the size of the TF bore, as well as bringing the TF turns slightly closer to the toroidal magnetic axis, thus reducing both the size and the cost of the TF magnet system. Nevertheless, the additional load to the cryogenic

system comes at a cost as well. Moreover, the cost to anneal the magnets increases nearly linearly with the number of anneals performed, with the dominant cost attributed to the re-cooling of the magnets. The incremental increase in the total cost of the reactor due to the increasing annealing and cryogenic system costs is much more than compensated for by the incremental decrease associated with the decreasing B/S/VV and magnet system costs. Thus, the effect of possible annealing schedules upon the overall cost of the reactor can be quite large. In fact, for 10 scheduled high temperature anneals the cost savings on the \$7.7B reactor is about \$0.5B, or about 6.5 % of the net (total) cost. The impact of this type of design modification upon sub-system and reactor costs can be seen in Figs. 48 - 50. As can be seen from Fig. 50, though, the economic gain per additional anneal decreases for increasing number of anneals. This economic advantage gained by adopting a suitable annealing schedule is achieved whether the reactor design is taken to have shielding characteristics according to the design limits (as in this study) or according to the three dimensional shielding performance analyses. Both cases have been analyzed, but for the purposes of this study it has been more convenient, due to some shielding uncertainties, to take the shielding performance to correspond to the radiation limits as given in Chapter 3.

Fig. 47: B/S/VV thickness as a function of number of anneals (performance constant).

Fig. 48: B/S/VV system (ITER) cost reduction with number of anneals.

Fig. 49: TF magnet system (ITER) cost reduction with number of anneals.

Fig. 50: Net (total) cost of the reactor (ITER) is very sensitive to number of anneals.

B. ARIES - II Studies:

Specific Design and Grading Issues:

As specified in the ARIES-II/IV Magnets final report [1], the current density in the superconducting material in grade five of the magnet (which includes the inner turn) is extremely high. It can be shown that this extremely high current density of about 1000 A/mm² cannot be sustained while keeping the magnet in a superconducting state (shown in Appendix B). By contrast, the current density in the superconductor in the ITER CDA magnet design (about 350 A/mm²) is about one third of that for the ARIES-II/IV design. Furthermore, it must be remembered that the ITER CDA conductor will operate at a much lower field of 11.2 T (the ARIES-II/IV TF coil experiences 15.9 T at the conductor).

It is assumed, of course, that some technological advancement will be made in the state of the art of superconducting material by the time an ARIES-type reactor is built (>2040). This advancement, however, must be reasonable. Advanced materials, such as Nb₃Al tapes, Nb₃(AlGe) tapes, and multifilamentary V₃Ga, were all rejected as the superconducting material of choice for this design in favor of multifilamentary Nb₃Sn (as in ITER CDA). The criterion for their rejection was a lack of technological experience with these materials, even though their superconducting characteristics were admittedly better than those of Nb₃Sn. No particular definition of the technological advancement for Nb₃Sn is given in the report for the TF magnets (the PF magnets apparently use the same current density scaling laws and superconducting characteristics as ITER CDA [2]), so advancement is assumed in such a way as to yield a conductor with acceptable stability performance. It turns out that the advancement required to achieve a superconducting state in the ARIES magnet design is

extraordinarily large. This study has taken the liberty to rework the TF magnets into a workable and believable design. The comparison of the level of technological advancement for the three designs (ITER CDA [2], ARIES-II/IV-final report [1], and ARIES-II/IV-Yeaw modified) is shown in Table 11. As can clearly be seen from this table, even the Yeaw-modified ARIES-II/IV superconductor characteristics are pushing realistic expectation, but the Final Report ARIES-II/IV characteristics are well beyond the realm of foreseeable technological advancement.

Table 11: Superconductor Characteristics of Various Magnet Designs.

Superconductor Parameter	ITER CDA	ARIES-II/IV Final Report	ARIES-II/IV Yeaw Modified
Critical Temperature	18 K	25 K	21 K
Upper Critical Magnetic Field	28T	36T	30T
Current Density Scaling Prefactor	13400 A/T·mm ²	31000 A/T·mm ²	20000 A/T·mm ²
Critical Current Density at 15.9 T and 5 K	233 A/mm ²	1660 A/mm ²	569 A/mm ²

It was claimed by the ARIES magnet designers [3] that the advancement assumed for their design was quite modest. In fact, the assumed advancement in the superconducting parameters was very close to the advancement assumed by ITER CDA, but the scaling law for the critical current density was very different (and dependent only upon the applied magnetic field). If the ITER CDA current density scaling law (standard for Nb₃Sn, and shown in Eqn. 4 in Chapter 3) is used in conjunction with the parameters given by the ARIES designers for the ARIES design (virtually the same as those given for ITER CDA), the resulting magnet is not in the

superconducting state. As was pointed out above, the advancement assumed in Table 11 for the ARIES final design was based upon an adequate performance of the conductor under ARIES conditions, and using the ITER CDA current density scaling law. The parameters that result are at large variance with those which were actually assumed in the ARIES study. If the ITER CDA parameters and scaling laws are actually used to design the ARIES-II/IV magnets, the magnets become much larger and more costly. In this study, the modified ARIES-II/IV design, the parameters used define the absolute boundary of realism, but the ITER CDA current density scaling law is still used.

In the interest of a thorough analysis, an economic analysis of the benefit of grading the ARIES-II/IV magnet has been performed. Grading, as has been previously explained, involves using the shell wound coil configuration to optimize the conductor design in subsequent shells, given that the magnetic field experienced by the conductor (as well as the nuclear heating, though grading usually does not take this into account) decreases for subsequent outer shells. To illustrate this principle, simply consider a magnet with many shells (say 10 turns by 10 shells). Each turn in a shell is, of course, of the same conductor design, being of the same flowpath. The inner shell requires a good deal of superconducting material in its strands, since the magnetic field is very high and the critical current density of the superconductor is much lower at high fields. Radially halfway through this magnet the magnetic field experienced by the conductor is considerably reduced. At this point, if the conductor design is identical to the inner shell conductor design, there is a great deal more superconducting material in the strands than is necessary to carry the current (the current being the same as in the inner turn, but the critical current density of the material being very much higher). Here it would be economically advantageous to design the conductor with much less superconducting material, since this material is the expensive component of the strand.

Thus, the 10 by 10 magnet has become a magnet of two grades, each 10 turns by 5 shells, and the magnet has become considerably less expensive.

A graded design has some subjectivity involved in it, since, against this advantageous exploitation of the decreasing magnetic field, there is also complexity of design to be considered (as well as an increased number of joints in the magnet). For ARIES-II/IV, five grades are specified. Grading of the magnet, however, is not considered in this study, due to the complexity and subjectivity of the analysis. Thus, no economic advantage is taken for grading. Moreover, in the annealing studies that have been performed, if the average cost of the strand over the entire magnet decreases (as in the case of a graded magnet), the economic impact of annealing is decreased. It would be beneficial to know how much the economic impact of annealing has been decreased. Such an analysis has been done for the ARIES-II/IV (final report) design. The relevant details of the grading of the magnet are given in Table 12. The costing analysis follows the costing rules given in Chapter 9.

Table 12: Economic Considerations of Grading for the ARIES-II/IV TF Magnets.

Grade	Fraction of Cu in the Composite	Cost of Composite (\$/kg)	Cost of Cable (\$/kg)	Fraction of Magnet of This Grade
1	0.91	446	276	0.25
2	0.98	412	255	0.16
3	0.86	470	290	0.24
4	0.76	522	321	0.16
5	0.67	563	346	0.19

As can be calculated, for the ARIES-II/IV magnet as designed (graded) the average cost of the conductor cable is \$297/kg, whereas for the ungraded magnet the average conductor cable cost is \$346/kg. This translates into a savings of a little less

than 15% for the graded design. Despite the fact that the ARIES-II/IV final version of the strawman has a cost of about \$200 M for the entire magnet system (TF, CS, and PF), more realistically, *MagRad* gives a cost of the TF magnets of about \$1.2 B. Using *MagRad*'s costing algorithms, the final cost of the cable for the TF magnets is about \$600 M. Thus, the 15% reduction in cable cost represents about a \$75 M reduction in the direct cost of the reactor, or about a \$150 M reduction in the net (total) cost of the reactor. This translates into a reduction of the cost of electricity from the plant of a little less than 1.5 mills/kWh_e. This reduction in the cost of electricity can be applied in full to a reactor which does not adopt an annealing schedule. For a reactor that does adopt an annealing schedule in addition to using a graded magnet design, it must be remembered that the economic advantage of annealing will be applied to each grade in the magnet. In incorporating both annealing and grading, then, while not all of the economic advantage of grading can be expected, at least a good portion of it can be realized. Incorporating both annealing and grading, it might be expected that another 1 mill/kWh_e reduction in the cost of electricity would be realized in addition to whatever reduction is given by annealing alone. The net impact of annealing upon the reactor cost of electricity, then, might be reduced by as much as about 0.5 mill/kWh_e (the 1.5 mill/kWh_e grading advantage less the advantage gained by annealing of 1.0 mill/kWh_e). While this is a small relative effect, the analysis has been here included for the sake of thoroughness. In all of what follows, however, grading is not taken into account, since *MagRad* does not treat this design modification.

Conductor Design:

Conductor design issues for ARIES-IV are generally the same as for the ITER CDA. Since both are CIC conductor designs, all conductor design trends are similar. Thus, for the sake of brevity, these issues will be passed over in this section. The

ITER CDA design represents a generic conductor design, and adequately demonstrates the various CICC design issues which need to be treated. The only conductor design issue which will be dealt with in the ARIES section of this thesis involves the trade-off between nuclear heating and the central helium flowpath area, both of which impact the operating temperature. This will be described in the reactor-level design sub-section which follows.

Reactor-Level Design:

As stated in the previous section, reactor-level design includes: coil winding pattern; high temperature annealing schedule; and blanket and shield thickness. While the ITER CDA coil winding pattern is pancake style, the ARIES-II design is shell wound. ARIES-IV and ARIES-II are claimed to have exactly the same magnet design. The basic difference between the two designs is that ARIES-IV assumes a greater degree of technological advance, the blanket and shield assembly being composed primarily of SiC with helium gas as the coolant, while the ARIES-II blanket and shield assembly is composed primarily of vanadium with lithium as the coolant. Since the two designs are very similar with respect to the magnets, only the ARIES-II reactor system has been analyzed for the sake of brevity. The material composition of the blanket and shield/vacuum vessel assembly will not be varied, but their respective thicknesses will be.

The primary trade-off, as in the case of the ITER CDA design, in reactor-level design will involve a reduction of blanket and shielding material for increasing number of high temperature anneals. The particular trade-off studied when examining ARIES, involves a shell wound coil configuration. In this configuration, the operating temperature is much more sensitive to the heat load than in the pancake wound configuration, as has already been explained. This sensitivity has been partially

compensated for by adding additional central helium flowpath area to the conductor design. Without this additional flowpath area (i.e. keeping the area constant at 300 mm²), the operating temperature is affected slightly more. However, when the flowpath area is increased for increasing number of anneals (and hence peak nuclear heating), the result is that this sensitivity of the operating temperature to the number of anneals is diminished, as shown in Fig. 51. The compensating increase in flowpath area required is shown in Fig. 53, while Fig. 52 shows the overall impact of decreased shielding (increased number of anneals) upon the total nuclear heat load. Fig. 53 shows the total cryogenic heat load as a function of number of anneals, and Fig. 54 shows the resulting capital cost increase of that sub-system (again, as in the ITER CDA analysis, the operating costs are neglected, as they are for all sub-systems).

Fig. 51: Nuclear heating rate has only a small impact upon the shell wound (ARIES-II) coil configuration T_{op} .

Fig. 52: Total nuclear heating in the shell wound coil versus number of anneals.

Fig. 53: Compensation for increased heat load by increased core helium flowpath area.

Fig. 54: Heat load on the cryogenic system increases for increasing number of anneals.

Fig. 55: Cryogenic system cost increases with increasing number of anneals.

The reason for the increased heat load with number of anneals designed into the system, as explained in the previous section, is that as the number of anneals is increased, the blanket/shield/vacuum vessel (B/S/VV) assembly thickness can be decreased in such a way that the performance (stability, primarily) of the magnet is held constant. The variation of the B/S/VV thickness with the number of anneals is shown in Fig. 56. Only the blanket and shield thickness was varied, not the vacuum vessel thickness, since the vacuum vessel is quite thin already, 10 cm. This decrease of B/S/VV thickness, of course, yields a corresponding decrease in the cost of the B/S/VV, also reducing the size of the TF magnets (geometrically and with respect to the number of turns required to give the proper on-axis magnetic field). As was shown in Fig. 55, though, there is a corresponding increase in the cryogenic system cost. The cost to anneal the magnets increases nearly linearly with the number of anneals performed, with the dominant annealing cost as the cost for re-cooling the magnets, just as for the ITER CDA design. As in the ITER CDA case, the incremental increase in the total cost of the reactor due to the increasing annealing and cryogenic system costs is much more than compensated for by the incremental decrease associated with the decreasing B/S/VV and magnet system costs. Thus, for the ARIES-IV power reactor design, the net impact upon the cost of the reactor is quite significant. The impact of this type of design modification upon sub-system and reactor costs can be seen in Figs. 57 - 60. As can be seen from Fig. 60, as with the ITER CDA case, the economic gain per additional anneal decreases for increasing number of anneals.

An important difference in the impact of the high temperature annealing schedule upon the cost of the reactor between the ITER CDA case and the ARIES-II case, is that in the latter case significantly fewer anneals are allowable. In fact, for ARIES-II the maximum number of high temperature anneals allowable is six over the life of the plant (about 38 calendar years). A greater number of anneals would result in

a very costly economic penalty due to the increased down-time of the reactor. The reason for this is that, while there are only six high temperature anneals performed, there are nearly 30 stabilizer anneals required over the lifetime of the reactor. These room temperature anneals are nearly as time-intensive as the high temperature anneals. This being the case, only one anneal (regardless of the type) is allowable without economic penalty per year. This is in contrast to the case for ITER, with an availability of about 25% (one third that of ARIES-II/IV). With such a low availability, it is expected that there will be at least three times as many opportunities as for ARIES throughout any given year to perform anneals, giving a total of about three allowable anneals per year before economic penalties accrue. This gives a total number of allowable high temperature anneals for ITER of 10 (with 24 room temperature anneals), ITER being scheduled to operate for about 12 years. Since ARIES-II is scheduled to operate for about 37.5 calendar years, a schedule of six high temperature and thirty room temperature anneals is the limiting schedule. Nevertheless, in adopting only five high temperature anneals and about 25 room temperature anneals, the cost of electricity from the reactor, the bottom line of all power reactor studies, decreases by over 10%.

It should be noted, furthermore, that the cost of electricity which is predicted by this study is very much larger than the cost of electricity of the ARIES-II/IV "strawman" [4]. The cost of electricity due entirely to the total cost of the plant (does not include fueling, B/S change-outs, etc.) predicted by the ARIES strawman is about 68 mills/kWhr., whereas that predicted by *MagRad* is about 150 mills/kWhr.. This large discrepancy is primarily due to the costing of the magnets. In the ARIES strawman the magnets are costed to about \$300 M, as stated above. In *MagRad*, however, the cost of the TF system alone is about \$2.5 B, with the cost of the remaining magnet systems a little less than \$1 B. The costs predicted by *MagRad* use

an extensively detailed costing algorithm for magnet systems, whereas that used by the ARIES strawman does not.

Fig. 56: B/S/VV thickness (ARIES-II) as a function of number of anneals, keeping conductor performance constant.

Fig. 57: B/S/VV system (ARIES-II) cost reduction with number of anneals.

Fig. 58: TF magnet system (ARIES-II) cost reduction with number of anneals.

Fig. 59: Net (total) cost of the reactor (ARIES-II) is very sensitive to number of anneals.

Fig. 60: COE (ARIES-II) decreases by over 10% by increasing the number of anneals.

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CHAPTER 11: SUMMARY AND RECOMMENDATIONS

A. *Summary:*

The development of the code *MagRad* has produced a fusion design tool that optimizes the TF magnet system with respect to the reactor as a whole. Design variables for the magnet include essentially all relevant thermal-hydraulic and conductor design variables. The thermal-hydraulic performance of the TF magnets has generally been regarded as the most difficult aspect of magnet design, with stability analysis being the most demanding of the thermal-hydraulic design. It has been shown in this work that the stability of the TF conductor can degrade significantly over the life of a D-T fusion reactor. This degradation has been unanticipated by the magnet community due to oversight of the relevant radiation damage data. Given the fact that this degradation occurs, design of the TF magnet system becomes much more coupled to the design of the reactor as a whole, especially the blanket and shield assembly together with the vacuum vessel (B/S/VV). Design trade-offs have dominated in the application of this code to the analysis of the two particular designs studied, ITER CDA and ARIES-II/IV. It should be recognized that *MagRad* has been developed as a rapid design optimization tool, and in no way is meant to replace more sophisticated, narrow-use, analytical codes, such as the code *CICC*. *MagRad* is a spread-sheet based, user-friendly computer code. It runs from the Microsoft® Excel platform, and is structured in the form of a workbook. *MagRad* consists of a main analysis module, to which the user is interfaced, together with several calculational macros.

Major Conclusions from this Thesis:

Several significant design trends have been isolated from the application of *MagRad* to the two systems analyzed, ITER CDA and ARIES-II/IV:

- The number of anneals designed into the system greatly impacts the thickness of the B/S/VV.
 - The B/S/VV assembly decreases about 5 cm (equivalent to about \$34 M for the ARIES-II design) per high temperature anneal.
- System design involves trade-offs between B/S/VV cost (decreasing about \$34 M/anneal), TF magnet system cost (decreasing about \$62 M/anneal), cryogenic system cost (increasing about \$6 M/anneal), and annealing schedule cost (increasing about \$5 M/anneal). This economic advantage of increasing the number of anneals continues until the availability of the reactor is impacted, corresponding to 10 anneals for the ITER CDA design and 6 anneals for the ARIES-II design.
- Conductor design involves trade-offs between economics and thermal-hydraulic performance.
 - A loss in the stability margin of about 4.5 mJ/cm³ accompanies every \$1 M saved on the TF system cost by adding copper to the super-conducting strands, and a loss of about 34 mJ/cm³ accompanies every \$1 M saved on the TF system cost by decreasing the helium fraction in the cable space.
- Conductor design involves trade-offs between stability and maximum quench temperature.
 - Though there is a 17 mJ/cm³ gain for every 1 K decrease in the maximum quench temperature by decreasing the helium fraction,

there is a 3 mJ/cm³ loss for every 1 K decrease in the maximum quench temperature by increasing the copper fraction.

- Conductor design and system design are coupled on both an economic basis and a performance basis.
 - In order to compensate for the higher nuclear heating (due to the thinner shield), an increase of roughly 10% in the area of the central helium flowpath in the conductor is required for each additional anneal designed into the ARIES-II system. Though this design modification increases the cost of the TF magnet system, the corresponding reduction in the cost of the B/S/VV system is greater.
- Stability margin criteria are not met in the ITER CDA at end-of-life fluences ($I_{lin}/I_{op}=0.99$ at end-of-life, but I_{lin}/I_{op} should be less than 0.9 for a properly designed conductor).
- The ARIES-II magnet design should be re-examined with respect to the ultra-high current density (> 1000 A/mm²) achieved in the base case design by the magnet group.
- The magnet cost scaling laws used in the ARIES designs should be examined in light of the fact that such scaling laws predict costs an order of magnitude less than present scaling laws.

The bottom line for such design trade-offs seems to be that annealing is an economically favorable design option until the reactor availability is impacted. Adopting such annealing schedules appears to reduce the net (total) cost of the reactor plant by 7-15% (for ITER this reduces the predicted cost from about \$7.7 B to about \$7.2 B). This large reduction in cost is translated into a reduction (about 10-15%) in the cost of electricity in the case of power reactors such as ARIES-II/IV.

Limitations of *MagRad*:

Certain limitations have resulted from the focused scope of the thesis. These limitations might be broken down into three categories: physics, neutronics, and magnet design.

Firstly, the physics of the reactor plasma core have been assumed fixed. This certainly is a limitation in the sense that no trade-off studies can be performed with *MagRad* which entail changing any of the plasma parameters.

Secondly, the neutronics analyses that can be performed by *MagRad* are rather limited. Generic neutron attenuation coefficients are used to calculate both the fluence and the nuclear heating at the TF coils. Major B/S/VV design modifications are not recommended, since *MagRad* has no way of treating placement of non-traditional shielding materials (such as beryllium) and no way of treating the modification of the radiation energy spectra resulting from the displacement of materials from the base-line design.

Finally, there are some limitations regarding the actual magnet design itself. No mechanical analysis is performed on the coil by *MagRad*, since this would involve an ANSYS™-like modeling of the coil. This type of solid modeling in three dimensions is well beyond the scope of the thesis. Additionally, *MagRad* only treats Nb₃Sn CIC (cable-in-conduit) conductors. The limitation that the superconducting material be Nb₃Sn could easily be relaxed by incorporating different material options into SuperOp besides those already included (namely, binary and ternary Nb₃Sn). *MagRad* would have to be significantly modified to treat tape-type conductor designs instead of CIC conductor designs. Entirely different thermal-hydraulic phenomena are involved for such conductor designs (as well as for monolithic conductor designs, such as are commonly used in SMES designs).

B. Recommendations:

Several recommendations can be made in light of the results of this thesis. Since it has been shown that high temperature annealing produces significant economic advantages for the reactor design, it is likely that further research into the viability of such anneals would be justifiable. This work has demonstrated that the thermal-hydraulic effects of such anneals are acceptable, but the mechanical analysis of such anneals is yet to be examined in more detail. Stresses during the anneal of the coils are not expected to be unacceptable, but the steady state operation of coils which utilize only ceramic insulators (or ceramic insulators in conjunction with polyimide vacuum impregnation) is still open to question. Mechanical analysis concerning this particular engineering question should be performed. A corresponding experimental program examining the feasibility of such high temperature anneals upon large magnet coils should also be initiated. It is expected that polyimides (in the place of epoxies) may be used in addition to ceramic insulation in the design of such anneal - grade coils. This expectation could be tested by the thermal cycling of a polyimide vacuum impregnated coil using ceramic insulation. In light of the preceding remarks three particular recommendations can be made which are critical in the further demonstration of the feasibility of high temperature superconductor anneals to be performed upon large coils:

- stress analysis for the coil under steady state operating conditions, assuming that all of the insulation is inorganic.
- stress analysis for the coil during high temperature anneals.
- experimental thermal cycling of polyimide insulation materials to high temperatures.
- perform re-irradiation and multiple anneal studies on Nb₃Sn cable.

Appendix A: References Listed Alphabetically

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Appendix B: The ARIES-II Magnet Design

It has been claimed that the ARIES-II/IV magnet design is perhaps inconsistent with the superconducting properties of any foreseeable technologically advanced superconducting material. As stated in the main text, the superconducting material option which was chosen in this design was Nb₃Sn. The engineering difficulty which has been encountered in this design follows. The analysis is very straightforward. All page references given refer to reference [29] in the previous appendix.

Current in the conductor: $I_{\text{pack}} = 75 \text{ kA}$ (p. 21, 24, 27)

Area of the superconducting material: $A_{\text{s/c}} = 74.9 \text{ mm}^2$ (p. 22, 23)

Thus, Current density in the s/c material: $J_{\text{s/c}} = 1000 \text{ A/mm}^2$

This value for the current density in the superconducting material is extremely high, especially given that the conductor is operating at 15.9 Tesla and 5 Kelvin. By comparison, the ITER CDA conductor has a current density in the superconducting material of only 313 A/mm², and operates under more favorable conditions (11.2 Tesla and 5 Kelvin). The superconducting characteristics given in the ARIES report match those given in the ITER CDA final report. The difficulty lies in the scaling law for critical current density prediction which was used in the ARIES design. According to the ITER CDA scaling law the ARIES conductor, at its operating point of 15.9 Tesla and 5 Kelvin, should have a critical current density of about 233 A/mm² (this scaling law can be found as Eqn. 7 in Chapter 3 of the main text). The scaling law used in the ARIES-II/IV design is as follows,

$$J_c(B) = 10^4 \cdot (c_1 B^5 + c_2 B^4 + c_3 B^3 + c_4 B^2 + c_5 B + c_6) \quad A/mm^2 \quad (B1)$$

where $c_1 = -9.115$, $c_2 = 432.292$, $c_3 = -7442.71$, $c_4 = 64645.83$, $c_5 = -4.548 \times 10^5$, and $c_6 = 2.54 \times 10^6$. Of particular note is that this critical current density scaling law has no temperature dependence. It is also at variance in its prediction with the much more well documented and accepted scaling law given in Eqn. (7). The scaling law given in Eqn. (B1) predicts an unbelievably high critical current density for the ITER CDA conductor of 2950 A/mm², whereas the scaling law used in the ITER CDA design (Eqn. 7) predicts a critical current density of 875 A/mm².

Again, it is important to note that the superconducting parameters of Nb₃Sn given in the ARIES report in no way differ from those given in the ITER CDA final report. This would seem to strongly imply that very little technological advancement in superconductor (Nb₃Sn) state-of-the-art is assumed in the ARIES design. Thus, the scaling law given in Eqn. (B1) seems somewhat inconsistent with the other design assumptions (namely, that the s/c parameters are not advanced). In the main text, since *MagRad* uses the standard, ITER CDA Nb₃Sn critical current scaling law given in Eqn. (7), the superconducting parameters given for the ARIES design are assumed to be technologically advanced. This advancement is calculated in such a way that the prediction of the critical current density using those parameters (the advance ones) matches that which would be obtained if the much lower parameters (found in the ITER CDA) were used in conjunction with the variant scaling law of Eqn. (B1).

In conclusion, there is no obvious way that an advancement in the superconducting characteristics of Nb₃Sn could be made in accord with that which has been assumed in the ARIES-II/IV magnet design. We would expect a small change to be possible in the critical current density scaling law, as well as fairly small changes in both the upper critical field, critical temperature. Instead, it has been assumed that there

will be an extremely large change in the scaling law with no corresponding change in either the upper critical field or the critical temperature. Given a scaling law such as the most commonly used one (Eqn. 7), the advancement necessary in the upper critical field and the critical temperature would have to be unbelievably large (as shown in the main text) in order to match the advancement inherently assumed in the adoption of the variant scaling law given above. At the operating point given for the ARIES-II/IV design, therefore, significant modification to the conductor design was necessary in this study, in order to achieve a conductor which would both operate in the superconducting state and incorporate reasonable technological advancement.

Appendix C: The ITER CDA Design Master Sheet

CICC Geometric Configuration Diagram:

Geometric Parameters of the CICC (Inner Turn):

Referring to the above diagram please input the following values. These values are the primary design parameters for the geometry of the CIC conductor.

Area of the core flowpath:	1.00 sqmm	(must keep at at least 0.7 sqmm)
Area of dead spaces and wrap:	0.00 sqmm	
Thickness of the central helix:	0.00 mm	
Thickness of the jacket:	3.00 mm	

Total number of strands:	750
Percentage of strands pure Cu:	10.00 %
Diameter of strands:	0.85 mm
Cabling pattern used:	3x2x5x5x5
Cabling coefficient of strands:	1.000
Porosity of the helix:	0.510
Wetting constant, Kp:	0.830

Now, input some additional design parameters in order to fully specify the CICC design.

Fraction of Cu in composite strands:	0.683
Fraction of helium in the strand region:	0.370

The CICC thus specified geometrically is the following:

Number of composite strands:	675
Number of pure Cu strands:	75
Area of the strands (uncabled):	426.59 sqmm
Area of the strands (cabled):	426.59 sqmm
Area of annular region:	677.13 sqmm
Area of steel / incoloy in the jacket:	305.21 sqmm
Area of steel in the helix:	0.00 sqmm
Area of helium in the non-strand region:	1.00 sqmm
Area of helium in the strand region:	250.54 sqmm
Area of Cu in the composite strands:	262.22 sqmm
Area of Cu in the pure Cu strands:	42.66 sqmm
Area of superconductor in the strands:	121.71 sqmm
Total area of helium:	251.54 sqmm
Total area of Cu:	304.88 sqmm
Total area of normal metal in the cable space:	304.88 sqmm
Total area of normal metal:	610.10 sqmm
Total cable space area:	678.13 sqmm
Total turn area (excluding insulation):	983.34 sqmm

Input: Operating Current:	37.89 kA
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Relevant Reactor Design Parameters: ReacDesign

First Wall / Blanket / Shield / Vacuum Vessel:

First column of choices is for the reference case,
and the second column of the choices is for the present case.

Input: ☐ ☐ Copper ☒ ☒ Water
☐ ☐ Inconel ☐ ☐ Liquid metal (Na/Li)
☐ ☐ V/Li/Tenelon/B4C ☐ ☐ Helium
☒ ☒ Steel
☐ ☐ SiC/Li2O/B4C/Be
☐ ☐ V5Cr5Ti
☐ ☐ Ti6Al4V

Caution: reference fluence and
nuclear heating must be man-
ually corrected due to variator
dependent upon volume of
conductor in the cable space.

☒ Shielding Calculation

		Reference	Present Case	
Input	Coolant percentage (B / S):	13.15	13.15	%
	Coolant percentage (VV):	38	38	%
	Misc. percentage (B / S):	47.4	47.4	%
	B / S Thickness:	50	45	cm
	VV Thickness:	45	51	cm
	Reference fluence:	10.00	x e18 n/sqcm	(ib safety incl.)
		Reference peak nuclear heating:	3.00	W/m (ib safety incl.)
		Number of s/c anneals:	0	
Output:	Present case fluence:	7.79	x e18 n/sqcm	
	Peak nuclear heating:	2.88	W/m	
		2.66	W/m - uncorrected for fcond	

Conductor Operational Parameters: RadHeat

Input: Number of flowpaths per coil: 24
 Is the coil wound in pancake fashion? y
 Is the nuclear heating poloidally uniform? n
 Is the high Qn region at the flowpath inlet? y
 Turns per shell or pancake: 10
 Elements per turn: 4
 Reference design flow length: 400 m
 Inlet temperature: 4.5 K
 Inlet pressure: 6 bar (ITER reference
 Outlet pressure: 3 bar pressure drop is
 Thermal resistance: 0.17 m-K/W 3 bar.)

Output: Flowpath length: 401.93 m
 Number of elements: 40
 Differential length, ds: 10.31 m
 Maximum solution time: 400 seconds
 Average helium density: 136 kg/cum
 Average helium specific heat: 3973 J/kg-K
 Core friction factor: 0.0508
 Annulus friction factor: 0.1267

Input:	AC Pulse E	AC Start t	AC t Span
	(mJ/cc)	(s)	(s)
	67	0	60
	67	1100	60
	-	-	-

			Output Times
(if uniform Qn)			(s)
Nucl. Pulse Q	Nucl. Start t	Nucl. t Span	200
(W/m)	(s)	(s)	400
0.35	80	1020	-
-	-	-

Thus, we have the pseudo-steady state operational constraints:

Input: Strain: -0.30 %
Magnetic field: 11.2 T

Output: Temperature: 5.01 K (in the low stability region)
Fluence: 7.79×10^{18} n/sqcm
Jop: 55.87 A/sqmm
Jop in the s/c: 311.32 A/sqmm

The superconducting characteristics of the conductor can now be quantified:

○ Binary Nb₃Sn

● Ternary Compound

Output: Critical temperature: 15.75 K
Upper critical field: 21.94 T
Critical current density: 824.29 A/sqmm
Current sharing temperature: 7.59 K
Tcs at start-up: 8.66 K

Stability Characteristics and TF Coil Parameters: RadStab

Input:	Initial copper resistivity:	12.5 nohm-cm
	Initial unstable heated length:	20 cm
	Initial instability timescale:	10 ms
	Magnetoresistivity limit:	1.10 nohm-m

Stability Module

Output:	Limiting current at start-up:	47298 A
	Limiting current at end of life:	38347 A
	Enthalpy margin at start-up:	1155 mJ/cc
	Enthalpy margin at end of life:	817 mJ/cc
	Resistivity (no anneals):	1.89 nohm-m
	Magnetores. (no anneals):	3.27 nohm-m
	Iop/Ic at start-up:	0.34 (Iop/Ic)
	Iop/Ic at end of life:	0.38 (Iop/Ic)
	Iop/Ilim at start-up:	0.80 (Iop/Ilim)
	Iop/Ilim at end of life:	0.99 (Iop/Ilim)

Annealing of Copper Stabilizer: StabAnneal

Input:	Reactor lifetime:	3.80 FPY
	Availability:	25.00 %
	Recovery fraction:	88 %

Stabilizer Annealing Module

Output:	EOL fast fluence to the coil:	7.79 x e18 n/sqcm
	EOL damage to the Cu:	3.69 mdpa
	Stabilizer damage limit:	0.85 mdpa (for stability reasons)
	Time of first anneal:	42.14 months
	Number of anneals required:	6.00 (exclusive of high T anneals)
	Time between last two anneals	23.02 months

Protection Characteristics and TF Coil Parameters: RadQuench

Necessary parameters (previously calculated):

(Nonsteel area in cable space)/(Cable space area):	1.000
(Copper area)/(Cable space area):	0.450
(Conductor area)/(Nonsteel area in cable space):	0.629
(Copper area)/(Nonsteel area in cable space):	0.450
(Superconductor area)/(Nonsteel area in cable space):	0.179
(Nonsteel area in cable space)/(Turn area):	0.690
(Copper area)/(Turn area):	0.310
(Metal area)/(Turn area):	0.744
(Normal metal area)/(Turn area):	0.620
(Superconductor area)/(Turn area):	0.124

Input: Dump time constant:	8 seconds	(instead of 10 s)
Time delay for detection:	1 seconds	
Time delay for turn cp:	8.4 seconds	

Quench Analysis Module

Output: Temperature at dump:	38 K
Temperature at turn cp:	132 K
Final hot spot temperature	162 K

Annealing of Superconducting Strands: SuperAnneal

Annealing Analysis Module (Single coil)

Anneal time:	60 minutes	Input
Anneal temperature:	900 K	
T (ambient):	300 K	
HT coefficient (convective):	0.133 W/sqm-K	
Emittance (gray):	0.6	
Steady state anneal time:	3 hours	

Volume of TF coil:	9.62 cum
Surface area of TF coil:	102.98 sqm
Area of cable space:	678 sqmm
Copper area in cable space:	305 sqmm
Coil flowlength:	9646 m

Output: Maximum annealing J:	8.86 A/sqmm
Final anneal ramping J:	3.38 A/sqmm
Steady state annealing J:	1.70 A/sq/mm
Total annealing energy:	448.81 GJ

Costing Considerations: CostComp

See comments and suggested values under the cells containing notes (marked).

Costing Computation Module

Input:	Number of TF coils:	16	
Output:	First turn nuclear heating:	13.55 W	
	Shell/turn nuclear heating:	50.95 W	
	Coil nuclear heating:	1.22 kW	
	Total TF nuclear heating:	19.56 kW	
	Other heat loads:	89.65 kW	
	Total cryogenic heat load:	109.22 kW	
	Cost of cryoplant:	80.69 M\$	(1995 dollars)
	Annual cryogenic system cost:	0.08 M\$/yr	
Input:	Insulation thickness:	1.00 mm	
Output:	Radial thickness of magnet:	378.84 mm	
	Toroidal thickness of magnet:	902.22 mm	
	Envelope area (each turn):	299.92 sqmm	
	Insulation area (each turn):	114.30 sqmm	
	Single winding area:	1397.56 sqmm	
	Case area of magnet (ib leg):	0.01 sqm	
	Total magnet area (ib leg):	0.34 sqm	
	Magnet costing (whole system):		
	Strand:	281026 k\$	(all in 1995 dollars)
	Case:	24963 k\$	
	Cabling:	28939 k\$	
	Coating:	12379 k\$	
	Coating set-up:	146 k\$	
	Cable:	322489 k\$	
	Jacket material	12601 k\$	
	Jacket tooling:	292 k\$	
	Encapsulation:	9260 k\$	
	Jacket:	22153 k\$	
	Extra material:	54620 k\$	
	Coil tooling:	1750 k\$	
	Winding:	112983 k\$	
	Dummy coils:	1432 k\$	

Heat treatment	211 k\$
Insulation:	10585 k\$
Joints:	192 k\$
Leads:	76 k\$
Bus:	152 k\$
Cases:	24963 k\$
Therm. Shield:	49 k\$
Intercoil:	3836 k\$
Welding, etc.:	6308 k\$
Transportation:	210 k\$
Hardware:	562009 k\$
Engineering:	112402 k\$
Contingency:	202323 k\$
Direct:	876734 k\$
G&A:	78906 k\$

Total cost of TF magnet coils: 955.64 \$M (1995 dollars)
 Total cost of PF magnet coils: 707.11 \$M

Input: Major radius of the machine: 6.00 m
 Minor radius of the plasma: 2.15 m
 Elongation of the plasma: 1.98

If blanket and shield structure is V/Tenelon/B4C give: (Calculated linearly)

Volume fraction of vanadium alloy:	0.14
Volume fraction of tenelon:	0.47
Volume fraction of boron carbide:	0.17

If blanket and shield structure is SiC/Li2O/B4C/Be give: (Calculated linearly)

Volume fraction of silicon carbide structure:	0.16
Volume fraction of silicon carbide filler:	0.40
Volume fraction of lithium oxide:	0.10
Volume fraction of beryllium:	0.03
Volume fraction of boron carbide:	0.18

Output: Blanket and shield thicknesses:

Inboard:	45.00 cm
Top:	27.00 cm
Bottom:	27.00 cm
Outboard:	81.00 cm

Major radius of the plasma:	4.26 m
Volume of the blanket/shield:	685.18 cum
Volume of the vacuum vessel:	616.56 cum
Cost of the blanket/shield:	133.82 M\$ (1995 dollars)
Cost of the vacuum vessel:	59.63 M\$

Output:	Cost of annealing electricity:	0.00 k\$	(1995 dollars)
	Engineering costs:	0.00 k\$	(includes training)
	Contingency costs:	0.00 k\$	
	R & D costs:	0.00 k\$	
	Design costs:	0.00 k\$	
	Direct annealing costs:	0.00 k\$	
	G & A costs:	0.00 k\$	
	Recool costs:	0.00 k\$	
	Superconductor annealing cost	0.00 M\$	
	Stabilizer annealing cost:	3.33 M\$	
	Total annealing schedule cost:	3.33 M\$	
	Annealing cost per kg strand:	5.94 \$/kg	
Input:	Land and land rights:	10.00 M\$	(1995 dollars)
	Auxiliary heating:	140.00 M\$	
	Structure and support:	60.00 M\$	
	Other vacuum systems:	70.00 M\$	
	Power supplies, etc.:	300.00 M\$	
	Fueling and Impurity control:	125.00 M\$	
	ECRH breakdown system:	140.00 M\$	
	I & C and Maintenance:	450.00 M\$	
	Heat transfer systems:	190.00 M\$	
	Turbine plant equipment:	0.00 M\$	
	Electric plant equipment:	100.00 M\$	
	Misc. plant equipment:	120.00 M\$	
Output:	Reactor equipment:	3415.22 M\$	(1995 dollars)
	Reactor plant equipment:	3674.78 M\$	
	Direct cost:	3904.78 M\$	
	Construction services:	590.19 M\$	
	Home office engineering:	203.05 M\$	
	Field office engineering:	234.29 M\$	
	Owner's cost:	722.38 M\$	
	Project contingency:	976.19 M\$	
	Interest during construction:	1073.81 M\$	
	Net (total) cost:	7704.69 M\$	(1995 dollars)
	Cost per full power day:	5.55 M\$/day	
	Cost per calendar day:	1.39 M\$/day

Record Relevant Output

Variable Listing:

Aannulus	=AHesr+Astrands
Acase	=(rmag*wmag-Nturns*Nflows*Awinding)/1000000
Acomp	=(1-fCupure)*Astrands
Acore	=SCS31
Acs	=Acore+Ahelix+Aannulus
ACu	=ACucomp+ACupure
ACucomp	=fCucomp*Acomp
ACupure	=fCupure*Astrands
ac_duration_vector	=SDS135
ac_energy_vector	=SBS135
ac_start_vector	=SCS135
Adead	=SCS32
Aenvelope	=4*(rturn+tins)^2-PI()*(rturn+tins)^2
AHe	=AHensr+AHesr
Aheatcs	=Aannulus+AHensr
Ahelix	=PI()*(rocore^2-ricore^2)
AHensr	=Acore+(1-Chelix)*Ahelix+Adead
AHesr	=fHesr*Asr
Ainsulation	=PI()*((rturn+tins)^2-rturn^2)
Amagnet	=rmag*wmag/1000000
Amagsurface	=2*(rmag/1000)*Lturn+2*(wmag/1000)*Lturn
Ametal	=Asteel+ACu+Asc
Ametals	=Asteelhelix+ACu+Asc
Anormmetal	=ACu+Asteel
Anormmetals	=ACu+Asteelhelix
aplasma	=SDS354
Areafactor	=(Ncoils/16)*(Nflows/24)*(Lpath/400)*(Aturn/904)^0.5
Arefmetal	=675.4
array_origin	=SGS126
Asc	=(1-fCucomp)*Acomp
Ashroud	=Ncoils*Amagsurface
Asr	=Astrands/(1-fHesr)
Asteel	=Asteelhelix+Asteeljacket
Asteelhelix	=Chelix*Ahelix
Asteeljacket	=PI()*(rturn^2-rcs^2)
Astrands	=Astrandsun*Cf
Astrandsun	=Nstrands*(dstrand/2)^2*PI()
Aturn	=Acs+Asteeljacket
availability	=SDS213/100
Awinding	=4*(rturn+tins)^2
Bctoprad	=21.9445655573501
Bop	=SCS170
bplasma	=aplasma*Elongation
BSCool	=SES96/100
BSDensity	=4156.6
BSMisc	=SES98/100
BSt	=SES99
Cf	=SCS40
Chelix	=1-SCS41
coe	=80/(1000*1000*3600)

CoilWind	=SES115
Costannealall	=Costannealtotal+Coststabannealrecool
Costannealcont	=0.3*(Costannealelec+Costannealeng)
Costannealdes	=0.5*Costannealeng
Costannealdir	=Costannealcont+Costannealelec+Costannealeng
Costannealelec	=(coe*Escanneal)/1000
Costannealeng	=2*nscanneals*(0.45+0.6*Ncoils^-0.5)*Costannealelec
CostannealGandA	=0.2*Costannealdir+0.15*CostannealRandD
CostannealRandD	=IF(nscanneals>0,1000000/1000,0)
Costannealrecool	=4*nscanneals*[MagRadBook]Cool-down Costs!SFS47/1000
Costannealtotal	=(Costannealdir+CostannealRandD+CostannealGandA+Costannealdes+Costannealrecool)/1000
CostBS	=46.9875
CostBSTotal	=(CostBS*VBS*BSdensity)/1000000
Costbuildings	=0.000325*50*Reactorsize+82+46+26+61+158+1.5*0.101*SUM(\$D\$329:\$D\$342)/1000+23*(QTotal/100)^0.7
Costbus	=50*Iop*0.000001*Ibus
Costcable	=Coststrand+Costcoatingset+Costcoating+Costcabling
Costcabling	=0.00025*Nstrands*Lsys
Costcase	=0.078*mcase
Costcoating	=0.04*(Nstrands*PI)*(dstrand/1000))*Lsys
Costcoatingset	=50*(Emagsys/200000000)^0.2
Costcoiltooling	=600*(Emagsys/200000000)^0.2
Costcomp	=Costcompperkg*mcomp/1000
Costcompperkg	=900-500*fCucomp
Costconduit	=Costjacket+Costencapsulation+Costsheathtooling
Costcontingency	=0.3*(Costhardware+Costeng)
Costcryoannual	=3*(2.72*(Refrig)^0.78*(coe/1000)^0.56)
Costcryoplant	=2*(1.51*(Refrig/1000)^0.7)
CostCupure	=20*mCupure/1000
Costdirect	=Costcontingency+Costhardware+Costeng
Costdummy	=0.12*(2*(mstrands+mconduit)/(Ncoils*Nturns))+20
Costencapsulation	=0.06*Lsys
Costeng	=(0.15+0.2*Ncoils^-0.5)*Costhardware
Costextramaterial	=(0.15+0.2/Ncoils)*Costcable+0.1*Costconduit
CostGandA	=0.09*Costdirect
Costhardware	=Costsubtotal+Costcase+Costintercoil+Costweldpotelec+Costtrans
Costheat	=100+50*(Emagsys/800000000)^0.2
Costinsulation	=IF(nscanneals>0,600*Volinsulation+52.5*Lsys/1000,600*Volinsulation)
Costintercoil	=0.078*45000*Volumefactor
Costjacket	=0.033*mconduit
Costjoints	=2*Ncoils*(Nturns/2+1)
Costleads	=0.002*Iop
CostPFMagnets	=700000*((Rmachine+2*aplasma+3*(BSt+VVt))/(Rmachine+2*aplasma+3*(RefBSt+RefVVt)))
CostRefBS	=46.9875
Costsheathtooling	=100*(Emagsys/200000000)^0.2
Coststabannealrecool	=(2*nscannealsreq*[MagRadBook]Cool-down Costs!SFS47/1000)/1000
Coststrand	=CostCupure+Costcomp
Costsubtotal	=Costcable+Costconduit+Costextramaterial+Costcoiltooling+Costwinding+Costdummy+Costheat+Costinsulation+Costjoints+Costleads+Costbus+Costbermo
Costthermo	=IF(nscanneals>0,100*(Ncoils/16)*Areafactor,0.03*Ncoils*Amagsurface)
CostTotalMagnets	=Costdirect+CostGandA
Costtrans	=50+10*Ncoils
CostVVTtotal	=(20*VVV*7800*(1-VVCool))/1000000
Costweldpotelec	=0.005*(mstrands+mconduit+mcase)
Costwinding	=0.12*(mstrands+mconduit)
cpHe	=3.97294744633605
D	=Kp*Nstrands*PI*(dstrand/1000)
deltaE	=1166.37480061503
deltaErad	=824.973897349498
deltaH	=1296.51907457964

deltaHrad	=260.715887857571
deltatime	=IF(dpaend=0,"No anneals",(1000*dpalimit)/dparatereal)
Dhann	=(4*Acs/(PI()*(Kp*Nstrands*dstrand+2*rcs+2*ricore)))/10
Dhcore	=(ricore*2)/10
Dhfrict	=4*(AHesr*0.000001)/D
dP	=Pinlet-Poutlet
dpaend	=fluence/2.11
dpalimit	=0.000853080568720379
dparateenv	=(1000*dpalimit)/RPM
dparatereal	=dpaend/RPM
ds	=Lpath/(Nele-1)
dstrand	=SCS38
dx	=SDS128
EffBSi	=BSi*(1-BSCool-BSMisc)
EffVVt	=VVt*(1-VVCool)
eg	=SCS258
Eintotal	=27781041457.1893
Elongation	=SDS355
Emagnet	=2640000000
Emagsys	=Ncoils*Emagnet
eo	=SCS169/100
Erecool	=nscanneals*(VolumeFactor*5000000000000)
Escanneal	=nscanneals*Ncoils*(SUM([MagRadBook]SuperAnneal!SOS3:SOS400)+tscanneal*Pssout)
Escannealo	=Ncoils*(SUM([MagRadBook]SuperAnneal!SOS3:SOS400)+tscanneal*Pssout)
fann	=SFS121
fanncalc	=5*0.046*4*Reann^-0.2
fcondheat	=(ACu+Asc)/Aheats
fcore	=SFS122
fcorecalc	=2*0.046*4*Recore^-0.2
fCucomp	=SDS46
fCucs	=ACu/Acs
fCuheat	=ACu/Aheats
fCupure	=SCS37/100
fCust	=Anormmetal/Aturn
fCturn	=ACu/Aturn
fheats	=Aheats/Acs
fheattum	=Aheats/Aturn
fHesr	=SDS47
finaltime	=IF(dpaend=0,"No anneals",recovery^(nscanneals-1)*deltatime)
Flowfactor	=(Nflows/24)*(Ncoils/16)
FlowInlet	=SES117
fluence	=fluencedesign
fluencedesign	=fluencewall*EXP(-EffBSi/Lambda)*EXP(-EffVVt/7)/(nscanneals+1)
fluenceend	=fluencedesign
fluenceVV	=Refffluence*EXP(ReffEffVVt/7)
fluencewall	=fluenceVV*EXP(ReffEffBSi/Reflambda)
fmetal	=Ametal/Aturn
FPD	=FPY*365
FPY	=SDS212
fractBe	=SES366
fractBinSiC	=SES367
fractBinV	=SES360
fractLiO	=SES365
fractLiOBe	=SES366
fractSiCfill	=SES364
fractSiCsolid	=fractSiCstruct+fractSiCfill+fractLiO+fractBe+fractBinSiC
fractSiCstruct	=SES363
fractTen	=SES359

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fractV      =SES358
fractVsolid =fractV+fractTen+fractBinV
frictionann =0.126583552816787
frictioncore =0.0507990351475369
fsc         =Asc/Aturn
fscheat     =Asc/Aheates
fscStab     =Asc/Asr
hconv       =SCS257
Ilim        =47213.4402981003
Ilimrad     =38252.3062152755
Iop         =SDS70*1000
Issanneal   =SQRT(Pssout*(ACu*0.000001)/(rhoBscanneal*0.000000001)/(Lpath*Nflows))
Jcop        =916.520644213408
Jcoprad     =824.997884725369
Jendanneal  =3.07029795191137
Jlimsc      =Ilim/Asc
Jlimscrad   =Ilimrad/Asc
Jmaxanneal  =7.78577415593406
Jop         =Iop/Acs
Jopsc       =Iop/Asc
Jssanneal   =Issanneal/(Acs*0.000001)/1000000
Kp          =SCS42
l           =SDS190
Lambda      =7
lbus        =2*40
Lpath       =Lpatho*(((bplasma+aplasma)/2*100+BSi+VVi)/((bplasma+aplasma)/2*100+RefBSi+RefVVi))^2
Lpatho      =SDS120
Lsys        =Lpath*Nflows*Ncoils
Lturn       =Lpath/Nturns
mann        =(((dP*100000)*2*rhoann*(AHesr/100)^2*Dhann/(100^2*1000))/(fann*Lpath))^0.5*1000
mcase       =Volcase*7800
mcoil       =mstrands+mcase+mconduit
mcomp       =mCucomp+msc
mconduit    =Volconduit*8106
mcore       =(((dP*100000)*2*rhocore*(Acore/100)^2*Dhcore/(100^2*1000))/(fcore*Lpath))^0.5*1000
mCu         =mCupure+mCucomp
mCucomp     =VolCucomp*8900
mCupure     =VolCupure*8900
msc         =Volsc*7500
mstrands    =mCu+msc
muann       =[MagRadBook]Thermo!ProfTP(Tinlet,Pav)*[MagRadBook]Thermo!kofTP(Tinlet,Pav)/[MagRadBook]Thermo!CpofTP(Tinlet,Pav)
mucore      =[MagRadBook]Thermo!ProfTP(Tinlet,Pav)*[MagRadBook]Thermo!kofTP(Tinlet,Pav)/[MagRadBook]Thermo!CpofTP(Tinlet,Pav)
N           =Points*Nturns
nanneals    =IF(dpaend=0,"No anneals",LN(1-RPM/deltatime*(1-recovery))/LN(recovery))
nannealsreq =IF(dpaend=0,"No anneals",IF(nscanneals>0,(nscanneals+1)*INT(nanneals)-nscanneals+1,INT(nanneals)+1))
Ncoils      =SDS297
Ncomp       =ROUND(Nstrands*(1-fCupure),0)
Nele        =Points*Nturns
Nflows      =SDS114
NpureCu     =ROUND(Nstrands*fCupure,0)
Nr          =IF(CoilWind="y",Nturns,Nflows)
nscanneals  =SDS106
Nstrands    =SCS36
Nturns      =SDS118
nucleate_duration_v =SDS141
nucleate_energy_ve =SBS141
nucleate_load_vectc =SBS141
nucleate_start_vectc =SCS141

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Nw          =IF(CoilWind="y",Nflows,Nturns)
Pav         =(Pinlet+Poutlet)/2/1.01325
Pinlet      =SDS122
Points      =SDS119
Poutlet     =SDS123
Print_Area  =SAS1:SFS764
profile_vector =SES138
Pssconv     =Amagsurface*hconv*(Tanneal-Tambient)
Pssout      =Pssrad+Pssconv
Pssrad      =Amagsurface*eg*sigma*(Tanneal^4-Tambient^4)
QcaseAC     =8*Volumefactor^0.7
Qintercoil  =8*(Ncoils/16)*Volumefactor^0.7
Qlambda     =5.24683770554914
Qleads      =13*Flowfactor^0.7
Qn          =SDS104
QnCoil      =Nflows*Qnflowpath/1000
Qndesign    =Qndesigno*(Ametal/Arefmetal)
Qndesigno   =Qnwall*EXP(-EffBSi/Qlambda)*EXP(-EffVVt/14.51)
Qnflowpath  =50.9792203946353
QnTotal     =Ncoils*QnCoil
QnTurn      =4.7025*Qndesign
QnVV        =Qn*EXP(RefEffVVt/14.51)
Qnwall      =QnVV*EXP(RefEffBSi/Refqlambda)
QPFcaseAC   =4
QPFwindingAC =15
Qpiping     =5*Flowfactor^0.7
Qpumping    =20*(Flowfactor*(dP/RefdP))^0.7
Qrad        =IF(nscanncals>0,9.4*Areafactor^0.7,6*Areafactor^0.7)
Qsupports   =4*Volumefactor^0.7
QTotal      =Qrad+Qsupports+Qleads+QcaseAC+QwindingAC+Qintercoil+Qpumping+Qpiping+QPFcaseAC+QPFwindingAC+QnTotal
QwindingAC  =5*(Areafactor*(tjacket/3))^0.7
rcs         =SQRT(Acs/PI())
Reactorsize =(2*Rmachine+2*aplasma+2*tBSob+2*rmag/1000+6)^2*(2*bplasma+2*tBStop+2*rmag/1000+6)
Reann       =mann*Dhann/(AHesr/100)/muann
Recore      =mcore*Dhcore/(Acore/100)/mucore
recovery    =SDS214/100
RefBSCool   =SDS96/100
RefBSMisc   =SDS98/100
RefBSi      =SDS99
RefdP       =3
RefEffBSi   =RefBSi*(1-RefBSCool-RefBSMisc)
RefEffVVt   =RefVVt*(1-RefVVCool)
Reffluence  =SDS102
Reflambda   =7
Refqlambda  =5.60463688594894
Refrig      =QTotal*1000*(4.5/Tinlet)
RefVVCool   =SDS97/100
RefVVt      =SDS100
Res         =SDS124
rhoann      =[MagRadBook]Thermo!rofTP(Tinlet,Pav)
rhoBanno    =100*SDS192
rhoBscanncal =MAX([MagRadBook]SuperAnneal!SFS2:[MagRadBook]SuperAnneal!SFS400)
rhocore     =[MagRadBook]Thermo!rofTP(Tinlet,Pav)
rhoHe       =0.135599925980755
rhozero     =SDS189
ricore      =SQRT(Acore/PI())
Rmachine    =SDS353
rmag        =Nr*Awinding^0.5+5

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rocore	=SQRT(Acore/PI())+thelix
RPM	=FPY/availability*12
rturn	=rcs+tjacket
sigma	=0.0000000567
Tambient	=SCS256
Tanneal	=SCS255
tau	=SDS191
taudump	=SDS240
tBSbottom	=0.6*BSI/100
tBSib	=BSI/100
tBSob	=1.8*BSI/100
tBStop	=0.6*BSI/100
Tcoprad	=15.7541828546398
TcsJc	=5.13559666259674
TcsJcrad	=5.01059708371533
Tcsop	=8.6639318901156
Tcsoprad	=7.59411587025162
tdelaydump	=SDS241
tdelayheat	=SDS242
Tempone	=SDS246
Tempthree	=SDS248
Temptwo	=SDS247
thelix	=SCS33
Timemax	=MAX(SSES139:SES140)
Tinlet	=SDS121
tins	=SDS309
tjacket	=SCS34
Top	=IF(CoilWind="y",MAX(SHS128:SLS132),MAX(SHS128:SLS250))
Toptemp	=IF(CoilWind="y",MAX(SHS128:SLS151),MAX(SHS128:SLS250))
tscanneal	=SCS259
UniformHeating	=SES116
VBS	=VBSib+VBSob+VBSbottom+VBSob
VBSbottom	=2*PI()*Rmachine*(tBSbottom*(2*aplasma+tBSib+tBSob+0.2))
VBSib	=2*PI()*Rmachine-aplasma-0.5*tBSib-0.1)*((2*bplasma+2)*tBSib)
VBSob	=2*PI()*Rmachine+aplasma+0.5*tBSob+0.1)*((2*bplasma+2)*tBSob)
VBSob	=2*PI()*Rmachine*(tBStop*(2*aplasma+tBSib+tBSob+0.2))
Volcase	=Acase*Lpath*Ncoils
Volcoil	=(Volcase+Volwp)/Ncoils
Volconduit	=(Asteel/1000000)*Lsys
VolCucomp	=(ACucomp/1000000)*Lsys
VolCupure	=(ACupure/1000000)*Lsys
Volinsulation	=(Ainsulation/1000000)*Lsys
Volsc	=(Asc/1000000)*Lsys
Volsteel	=(Asteel/1000000)*Lsys
Volsteelhelix	=(Asteelhelix/1000000)*Lsys
Volsteeljacket	=(Asteeljacket/1000000)*Lsys
Volstrands	=(Astrands/1000000)*Lsys
Volturn	=(Aturn/1000000)*Lsys
Volumefactor	=(Ncoils/16)*(Nflows/24)*(Lpath/400)*(Aturn/904)
Volwp	=(Amctal*0.000001)*Lsys
VVCool	=SES97/100
VVt	=SES100
VVV	=VVVib+VVVtop+VVVbottom+VVVob
VVVbottom	=VVVtop
VVVib	=2*PI()*Rmachine-aplasma-0.5*(VVt/100)-0.1)*((2*bplasma+2)*(VVt/100))
VVVob	=2*PI()*Rmachine+aplasma+0.5*(VVt/100)+0.1)*((2*bplasma+2)*(VVt/100))
VVVtop	=2*PI()*Rmachine*((VVt/100)*(2*aplasma+2*(VVt/100)+0.2))
wmag	=Nw*Awinding^0.5+5

Appendix D: The ARIES-II Design Master Sheet

CICC Geometric Configuration Diagram:

Geometric Parameters of the CICC (Inner Turn):

Referring to the above diagram please input the following values. These values are the primary design parameters for the geometry of the CIC conductor.

Area of the core flowpath:	300.00 sqmm	(must keep at at least 0.7 sqmm)
Area of dead spaces and wrap:	40.00 sqmm	
Thickness of the central helix:	2.00 mm	
Thickness of the jacket:	10.40 mm	

Total number of strands:	2000
Percentage of strands pure Cu:	40.00 %
Diameter of strands:	0.81 mm
Cabling pattern used:	5x5x2^6
Cabling coefficient of strands:	1.000
Porosity of the helix:	0.510
Wetting constant, Kp:	0.830

Now, input some additional design parameters in order to fully specify the CICC design.

Fraction of Cu in composite strands:	0.675
Fraction of helium in the strand region:	0.330

The CICC thus specified geometrically is the following:

Number of composite strands:	1200
Number of pure Cu strands:	800
Area of the strands (uncabled):	1022.98 sqmm
Area of the strands (cabled):	1022.98 sqmm
Area of annular region:	1526.84 sqmm
Area of steel / incoloy in the jacket:	1972.88 sqmm
Area of steel in the helix:	66.33 sqmm
Area of helium in the non-strand region:	409.04 sqmm
Area of helium in the strand region:	503.86 sqmm
Area of Cu in the composite strands:	414.31 sqmm
Area of Cu in the pure Cu strands:	409.19 sqmm
Area of superconductor in the strands:	199.48 sqmm
Total area of helium:	912.89 sqmm
Total area of Cu:	823.50 sqmm
Total area of normal metal in the cable space:	889.83 sqmm
Total area of normal metal:	2862.71 sqmm
Total cable space area:	1962.20 sqmm
Total turn area (excluding insulation):	3935.08 sqmm

Input: Operating Current:	75 kA
---------------------------	-------

Relevant Reactor Design Parameters: ReacDesign

First Wall / Blanket / Shield / Vacuum Vessel:

First column of choices is for the reference case,
and the second column of the choices is for the present case.

Input:	☐	☐	Copper	☐	☐	Water
	☐	☐	Inconel	☒	☒	Liquid metal (Na/Li)
	☒	☒	V/Li/Tenelon/B4C	☐	☐	Helium
	☐	☐	Steel			
	☐	☐	SiC/Li2O/B4C/Be			
	☐	☐	V5Cr5Ti			
	☐	☐	Ti6Al4V			

Caution: reference fluence and nuclear heating must be manually corrected due to variation dependent upon volume of conductor in the cable space.

☒ Shielding Calculation

		Reference	Present Case	
Input	Coolant percentage (B / S):	22	22	%
	Coolant percentage (VV):	38	38	%
	Misc. percentage (B / S):	0	0	%
	B / S Thickness:	105	115	cm
	VV Thickness:	10	10	cm
	Reference fluence:	7.50	x e18 n/sqcm	(ib safety incl.)
	Reference peak nuclear heating:	0.60	W/m	(ib safety incl.)
	Number of s/c anneals:	0		
Output:	Present case fluence:	3.36	x e18 n/sqcm	
	Peak nuclear heating:	0.74	W/m	

0.34 W/m - uncorrected for fcond

Conductor Operational Parameters: RadHeat

Input: Number of flowpaths per coil: 24
 Is the coil wound in pancake fashion? n
 Is the nuclear heating poloidally uniform? n
 Is the high Qn region at the flowpath inlet? y
 Turns per shell or pancake: 8
 Elements per turn: 4
 Reference design flow length: 210 m
 Inlet temperature: 4.5 K
 Inlet pressure: 6 bar
 Outlet pressure: 3 bar
 Thermal resistance: 0.17 m-K/W

Output: Flowpath length: 223.04 m
 Number of elements: 32
 Differential length, ds: 7.19 m
 Maximum solution time: 120 seconds
 Average helium density: 136 kg/cum
 Average helium specific heat: 3973 J/kg-K
 Core friction factor: 0.0184
 Annulus friction factor: 0.1133

Input:	AC Pulse E	AC Start t	AC t Span
	(mJ/cc)	(s)	(s)
	100	0	60
	100	1100	60
	-	-	-

			Output Times
(if uniform Qn)			(s)
Nucl. Pulse Q	Nucl. Start t	Nucl. t Span	20
(W/m)	(s)	(s)	120
0.35	0	1000	-
-	-	-

Thus, we have the pseudo-steady state operational constraints:

Input: Strain: -0.30 %
 Magnetic field: 15.9 T

Output: Temperature: 4.77 K (in the low stability region)
 Fluence: 3.36×10^{18} n/sqcm
 Jop: 38.22 A/sqmm (over cable space)
 Jop in the s/c: 375.98 A/sqmm

The superconducting characteristics of the conductor can now be quantified:

○ Binary Nb₃Sn

● Ternary Compound

Output: Critical temperature: 19.77 K
 Upper critical field: 27.34 T
 Critical current density: 946.05 A/sqmm
 Current sharing temperature: 7.95 K

Tcs at start-up: 7.29 K

Stability Characteristics and TF Coil Parameters: RadStab

Input: Initial copper resistivity: 12.5 nohm-cm
 Initial unstable heated length: 20 cm
 Initial instability timescale: 10 ms
 Magnetoresistivity limit: 1.50 nohm-m

Stability Module

Output: Limiting current at start-up: 81.0 kA
 Limiting current at end of life: 70.2 kA
 Enthalpy margin at start-up: 667 mJ/cc
 Enthalpy margin at end of life: 842 mJ/cc

Resistivity (no anneals): 1.08 nohm-m
 Magnetores. (no anneals): 2.01 nohm-m
 Iop/Ic at start-up: 0.54 (Iop/Ic)
 Iop/Ic at end of life: 0.40 (Iop/Ic)
 Iop/Ilim at start-up: 0.93 (Iop/Ilim)
 Iop/Ilim at end of life: 1.07 (Iop/Ilim)

Annealing of Copper Stabilizer: StabAnneal

Input: Reactor lifetime: 30.00 FPY
 Availability: 75.00 %
 Recovery fraction: 88 %

Stabilizer Annealing Module

Output: EOL fast fluence to the coil: 3.36×10^{18} n/sqcm
 EOL damage to the Cu: 1.59 mdpa
 Stabilizer damage limit: 1.35 mdpa (for stability reasons)
 Time of first anneal: 405.69 months
 Number of anneals required: 2.00 (exclusive of high T anneals)

Time between last two anneals 395.56 months

Protection Characteristics and TF Coil Parameters: RadQuench

Necessary parameters (previously calculated):

(Nonsteel area in cable space)/(Cable space area):	0.987
(Copper area)/(Cable space area):	0.420
(Conductor area)/(Nonsteel area in cable space):	0.528
(Copper area)/(Nonsteel area in cable space):	0.425
(Superconductor area)/(Nonsteel area in cable space):	0.103
(Nonsteel area in cable space)/(Turn area):	0.492
(Copper area)/(Turn area):	0.209
(Metal area)/(Turn area):	0.778
(Normal metal area)/(Turn area):	0.727
(Superconductor area)/(Turn area):	0.051

Input: Dump time constant: 8 seconds (instead of 10 s)

Time delay for detection: 1 seconds

Time delay for turn cp: 8.4 seconds

Quench Analysis Module

Output: Temperature at dump:	28 K
Temperature at turn cp:	132 K
Final hot spot temperature	160 K

Annealing of Superconducting Strands: SuperAnneal

Annealing Analysis Module (Single coil)

Anneal time:	60 minutes	Input
Anneal temperature:	900 K	
T (ambient):	300 K	
HT coefficient (convective):	0.133 W/sqm-K	
Emittance (gray):	0.6	
Steady state anneal time:	3 hours	

Volume of TF coil:	18.99 cum
Surface area of TF coil:	130.42 sqm
Area of cable space:	1962 sqmm
Copper area in cable space:	823 sqmm
Coil flowlength:	5353 m

Output: Maximum annealing J:	9.86 A/sqmm
Final anneal ramping J:	3.70 A/sqmm
Steady state annealing J:	1.46 A/sq/mm
Total annealing energy:	1786.28 GJ

Costing Considerations: CostComp

See comments and suggested values under the cells containing notes (marked).

Costing Computation Module

Input:	Number of TF coils:	32	
Output:	First turn nuclear heating:	3.47 W	
	Shell/turn nuclear heating:	27.74 W	
	Coil nuclear heating:	0.67 kW	
	Total TF nuclear heating:	21.31 kW	
	Other heat loads:	124.50 kW	
	Total cryogenic heat load:	145.80 kW	
	Cost of cryoplant:	98.77 M\$	(1995 dollars)
	Annual cryogenic system cost:	0.11 M\$/yr	
Input:	Insulation thickness:	1.00 mm	
Output:	Radial thickness of magnet:	1751.80 mm	
	Toroidal thickness of magnet:	587.27 mm	
	Envelope area (each turn):	1136.84 sqmm	
	Insulation area (each turn):	225.51 sqmm	
	Single winding area:	5297.44 sqmm	
	Case area of magnet (ib leg):	0.01 sqm	
	Total magnet area (ib leg):	1.03 sqm	
	Magnet costing (whole system):		
	Strand:	511909 k\$	(all in 1995 dollars)
	Case:	50676 k\$	
	Cabling:	85646 k\$	
	Coating:	34742 k\$	
	Coating set-up:	155 k\$	
	Cable:	632452 k\$	
	Jacket material:	93437 k\$	
	Jacket tooling:	310 k\$	
	Encapsulation:	10278 k\$	
	Jacket:	104025 k\$	
	Extra material:	109223 k\$	
	Coil tooling:	1862 k\$	
	Winding:	521175 k\$	
	Dummy coils:	4092 k\$	

	Heat treatment	218 k\$	
	Insulation:	23177 k\$	
	Joints:	320 k\$	
	Leads:	150 k\$	
	Bus:	300 k\$	
	Cases:	50676 k\$	
	Therm. Shield:	125 k\$	
	Intercoil:	9458 k\$	
	Welding, etc.:	24964 k\$	
	Transportation:	370 k\$	
	Hardware:	1482587 k\$	
	Engineering:	274805 k\$	
	Contingency:	527218 k\$	
	Direct:	2284611 k\$	
	G&A:	205615 k\$	
	Total cost of TF magnet coils:	2490.23 \$M	(1995 dollars)
	Total cost of PF magnet coils:	759.42 \$M	
Input:	Major radius of the machine:	5.60 m	
	Minor radius of the plasma:	1.4 m	
	Elongation of the plasma:	2.03	
	If blanket and shield structure is V/Tenelon/B4C give: (Calculated linearly)		
	Volume fraction of vanadium alloy:	0.14	
	Volume fraction of tenelon:	0.47	
	Volume fraction of boron carbide:	0.17	
	If blanket and shield structure is SiC/Li2O/B4C/Be give: (Calculated linearly)		
	Volume fraction of silicon carbide structure:	0.16	
	Volume fraction of silicon carbide filler:	0.40	
	Volume fraction of lithium oxide:	0.10	
	Volume fraction of beryllium:	0.03	
	Volume fraction of boron carbide:	0.18	
Output:	Blanket and shield thicknesses:		
	Inboard:	115.00 cm	
	Top:	69.00 cm	
	Bottom:	69.00 cm	
	Outboard:	207.00 cm	
	Major radius of the plasma:	2.84 m	
	Volume of the blanket/shield:	1310.74 cum	
	Volume of the vacuum vessel:	76.59 cum	
	Cost of the blanket/shield:	659.56 M\$	(1995 dollars)
	Cost of the vacuum vessel:	7.41 M\$	

Output:	Cost of annealing electricity:	0.00 k\$	(1995 dollars)
	Engineering costs:	0.00 k\$	(includes training)
	Contingency costs:	0.00 k\$	
	R & D costs:	0.00 k\$	
	Design costs:	0.00 k\$	
	Direct annealing costs:	0.00 k\$	
	G & A costs:	0.00 k\$	
	Recool costs:	0.00 k\$	
	Superconductor annealing cost	0.00 M\$	
	Stabilizer annealing cost:	2.37 M\$	
	Total annealing schedule cost:	2.37 M\$	
	Annealing cost per kg strand:	1.56 \$/kg	
Input:	Land and land rights:	10.00 M\$	(1995 dollars)
	Auxiliary heating:	175.00 M\$	
	Structure and support:	30.00 M\$	
	Other vacuum systems:	50.00 M\$	
	Power supplies, etc.:	50.00 M\$	
	Fueling and Impurity control:	5.00 M\$	
	ECRH breakdown system:	5.00 M\$	
	I & C and Maintenance:	5.00 M\$	
	Heat transfer systems:	140.00 M\$	
	Turbine plant equipment:	250.00 M\$	
	Electric plant equipment:	100.00 M\$	
	Misc. plant equipment:	50.00 M\$	
Output:	Reactor equipment:	4477.76 M\$	(1995 dollars)
	Reactor plant equipment:	4818.06 M\$	
	Direct cost:	5228.06 M\$	
	Construction services:	737.42 M\$	
	Home office engineering:	271.86 M\$	
	Field office engineering:	313.68 M\$	
	Owner's cost:	967.19 M\$	
	Project contingency:	1307.02 M\$	
	Interest during construction:	1437.72 M\$	
	Net (total) cost:	10262.95 M\$	(1995 dollars)
	Cost per full power day:	0.94 M\$/day	
	Cost per calendar day:	0.70 M\$/day	
Input:	Reactor power:	1000.00 MW	
Output:	Cost of Electricity:	150.74 mills/kWhre

Record Relevant Output

Appendix E: A Macro Example - RadStab

This appendix is a code listing for the RadStab macro. Computational details are given in Chapter 4 of the main text. The accurate prediction of conductor stability is the aim of this macro.

Stability

Commentation:

```
=MESSAGE(TRUE,"Calculating Ilim and energy margin at start-up.")
=SET.NAME("muIc",hdx:Hard Thesis:MagRad:[MagRadBook]Thermo!ProfTP(Top,Pav)*hdx:Hard
=SET.NAME("kIc",100*hdx:Hard Thesis:MagRad:[MagRadBook]Thermo!kofTP(Top,Pav))
=SET.NAME("littleto",0.5)
=SET.NAME("Ilimo",50000)
=SET.NAME("Ilim",55000)
=SET.NAME("diffIlim",ABS(Ilim-Ilimo))
=SET.NAME("Blimo",Ilimo/Top*Bop)
=SET.NAME("littlebo",Blimo/Bcto)
=SET.NAME("littlebto",(1-littleto^2)*(1-0.31*littleto^2*(1-1.77*LN(littleto))))
=SET.NAME("diffb",littlebto-littlebo)
=SET.NAME("continue",0)
** =FORMULA(littleto,!$A$190)
** =FORMULA(Ilimo,!$A$191)
** =FORMULA(Ilim,!$A$192)
** =FORMULA(diffIlim,!$A$193)
** =FORMULA(Blimo,!$A$194)
** =FORMULA(littlebo,!$A$195)
** =FORMULA(littlebto,!$A$197)
** =FORMULA(diffb,!$A$198)
** =FORMULA(continue,!$A$199)
=WHILE(continue=0)
** = IF(ABS(diffb)>0.2)
** =   SET.NAME("increment",0.05)
** = ELSE.IF(ABS(diffb)>0.1)
** =   SET.NAME("increment",0.01)
** = ELSE.IF(ABS(diffb)>0.01)
** =   SET.NAME("increment",0.002)
** = ELSE.IF(ABS(diffb)>0.001)
** =   SET.NAME("increment",0.00003)
** = ELSE()
** =   SET.NAME("increment",0.000005)
** = END.IF()
= SET.NAME("increment",10*(1.5*LOG(ABS(diffb))-0.7))
= IF(diffb>0)
```

This entire section of the module calculates all of the necessary values to predict the stability margin of the conductor at zero fluence.

Initialization of all parameters.

Display of interim values used for debugging purposes. These are no longer displayed.

Iterative procedure using b and t constraints.

```

= SET.NAME("litleto",litleto+increment)
= ELSEQ
= SET.NAME("litleto",litleto-increment)
= END.IFQ
= SET.NAME("Ilimo",(2*Ilimo+Ilim)/3)
= SET.NAME("Blimo",Ilimo/lop*Bop)
= SET.NAME("litlebo",Blimo/Beto)
** = FORMULA(litleto,!$A$200)
** = FORMULA(Ilimo,!$A$201)
** = FORMULA(Blimo,!$A$202)
** = FORMULA(litlebo,!$A$203)
= SET.NAME("x",Blimo/rhozero)
** = FORMULA(x,!$F$188)
= SET.NAME("rhoBzero",rhozero/(ATAN(0.7698*9.683*x)/(9.683*x)+ATAN(0.2049*153.5*x)/(1
** = FORMULA(rhoBzero,!$F$189)
= SET.NAME("Tc",Tco*litleto)
** = FORMULA(Tc,!$F$190)
= IF(Tc<Top)
= FORMULA("Tc<Top",!$D$199)
= GOTO($A$208)
= END.IFQ
= SET.NAME("deltaT",Tc-Top)
** = FORMULA(deltaT,!$F$191)
= SET.NAME("qwall",hsemiinf*deltaT)
** = FORMULA(qwall,!$F$192)
= SET.NAME("vIle",0.5*(D*I^(1/3)*betaIle*qwall/((AlIesr*0.000001)*cpIle*rhoIle)))
** = FORMULA(vIle,!$F$193)
= SET.NAME("Re",rhoIle*Dhfrict*vIle/mulIle)
** = FORMULA(Re,!$F$194)
= SET.NAME("Nu",0.0259*Re^0.8*Pr^0.4*(Top/Tc)^0.716)
** = FORMULA(Nu,!$F$195)
= SET.NAME("h",kIle*Nu/Dhfrict)
** = FORMULA(h,!$F$196)
= SET.NAME("Ilim",SQRT((fCucomp*(lcomp*0.000001)*D*(1-fCupure)*deltaT*h/(rhoBzero*0.6
** = FORMULA(Ilim,!$F$197)
= SET.NAME("litlebt",(1-litleto^2)*(1-0.31*litleto^2*(1-1.77*LN(litleto))))
** = FORMULA(litlebt,!$F$198)
= SET.NAME("diffIlim",ABS(Ilim-Ilimo))
= SET.NAME("diffb",ABS(litlebt-litlebo))
** = FORMULA(diffIlim,!$F$199)
** = FORMULA(diffb,!$F$200)
= SET.NAME("Test",ABS(diffIlim)<20)
** = FORMULA(Test,!$F$201)
= SET.NAME("Testtwo",ABS(diffb)>0.025)

```

More diagnostic values no
longer displayed.

Loop continues until the
iterative calculations on Ilim
are within 20 Amps (or about

```

= IF(Test)
=   SET.NAME("continue",1)
=   IF(Testtwo)
=     SET.NAME("continue",0)
=   END.IF()
= END.IF()
=NEXT()
=BEEO()

=MESSAGE(TRUE,"Calculating Ilim and energy margin at end of life.")
=SET.NAME("litletorad",0.5)
=SET.NAME("Ilimorad",50000)
=SET.NAME("Ilimrad",55000)
=SET.NAME("diffIlimrad",ABS(Ilimrad-Ilimorad))
=SET.NAME("Blimorad",Ilimorad/Iop*Bop)
=SET.NAME("litleborad",Blimorad/Bctorad)
=SET.NAME("litlebtorad", (1-litletorad^2)*(1-0.31*litletorad^2*(1-1.77*LN(litletorad))))
=SET.NAME("diffbrad",litlebtorad-litleborad)
=SET.NAME("continuerad",0)
** =FORMULA(litletorad,!$A$190)
** =FORMULA(Ilimo,!$A$191)
** =FORMULA(Ilim,!$A$192)
** =FORMULA(diffIlimrad,!$A$193)
** =FORMULA(Blimorad,!$A$194)
** =FORMULA(litleborad,!$A$195)
** =FORMULA(litlebtorad,!$A$197)
** =FORMULA(diffbrad,!$A$198)
** =FORMULA(continuerad,!$A$199)
=WHILE(continuerad=0)
** = IF(ABS(diffbrad)>0.2)
** =   SET.NAME("incrementrad",0.05)
** = IF(ABS(diffbrad)>0.1)
** =   SET.NAME("incrementrad",0.01)
** = ELSE.IF(ABS(diffbrad)>0.01)
** =   SET.NAME("incrementrad",0.002)
** = ELSE.IF(ABS(diffbrad)>0.001)
** =   SET.NAME("incrementrad",0.00003)
** = ELSE()
** =   SET.NAME("incrementrad",0.000005)
** = END.IF()
= SET.NAME("incrementrad",10^(1.5*LOG(ABS(diffbrad))-0.7))
= IF(diffbrad>0)
=   SET.NAME("litletorad",litletorad+incrementrad)
= ELSE()

```

0.05%)

This entire section of the module calculates all of the necessary values to predict the stability margin of the conductor at the given fluence.

Initialization of all parameters.

Display of interim values used for debugging purposes. These are no longer displayed.

Iterative procedure using b and t constraints.

```

= SET.NAME("littletorad",littletorad-incrementrad)
= END.IF()
= SET.NAME("Ilimorad",(2*Ilimorad+Ilimrad)/3)
= SET.NAME("Blimorad",Ilimorad/Top*Bop)
= SET.NAME("littleborad",Blimorad/Bctorad)
** = FORMULA(littletorad,!$A$200)
** = FORMULA(Ilimorad,!$A$201)
** = FORMULA(Blimorad,!$A$202)
** = FORMULA(littleborad,!$A$203)
= SET.NAME("rhorad",rhozero+drho)
** = FORMULA(rhorad,!$F$188)
= SET.NAME("xrad",Blimorad/(IF(rhorad>68,68,rhorad)))
** = FORMULA(xrad,!$F$189)
= SET.NAME("rhoB",rhorad/(ATAN((0.7698-0.0215*fluence)*9.683*xrad)/(9.683*xrad)+ATAN((
** = FORMULA(rhoB,!$F$190)
= SET.NAME("rhoBann",IF(rhoB<=rhoBanno,rhoB,rhoBanno))
** = FORMULA(rhoBann,!$F$191)
= SET.NAME("Tcrad",Tcorad*littletorad)
** = FORMULA(Tcrad,!$F$192)
= IF(Tcrad<Top)
= FORMULA("Tcrad<Top",!$D$200)
= GOTO($A$208)
= END.IF()
= SET.NAME("deltaTrad",Tcrad-Top)
** = FORMULA(deltaTrad,!$F$193)
= SET.NAME("qwallrad",hscmifn*deltaTrad)
** = FORMULA(qwallrad,!$F$194)
= SET.NAME("vIlerad",0.5*(D*I^(1/3)*betaIe*qwallrad/((AIlesr*0.000001)*cpIle*rholIe)))
** = FORMULA(vIlerad,!$F$195)
= SET.NAME("Rerad",rholIe*Dhfrict*vIlerad/mulIe)
** = FORMULA(Rerad,!$F$196)
= SET.NAME("Nurad",0.0259*Rerad^0.8*Pr^0.4*(Top/Tcrad)^0.716)
** = FORMULA(Nurad,!$F$197)
= SET.NAME("hrad",klIe*Nurad/Dhfrict)
** = FORMULA(hrad,!$F$198)
= SET.NAME("Ilimrad",SQRT((Cucomp*(Acomp*0.000001)*D*(1-fCupure)*deltaTrad*hrad/(rho
** = FORMULA(Ilimrad,!$F$199)
= SET.NAME("littletbrad",(1-littletorad^2)*(1-0.31*littletorad^2*(1-1.77*L\N(littletorad))))
** = FORMULA(littletbrad,!$F$200)
= SET.NAME("diffIlimrad",ABS(Ilimrad-Ilimorad))
= SET.NAME("diffbrad",ABS(littletbrad-littleborad))
** = FORMULA(diffIlimrad,!$F$201)
** = FORMULA(diffbrad,!$F$202)
= SET.NAME("Testrad",ABS(diffIlimrad)<20)

```

More diagnostic values no longer displayed.

Loop continues until the iterative calculations on Ilim are within 20 Amps (or about 0.05%)

```

**= FORMULA(Testrad,!$F$203)
= SET.NAME("Testradtwo",ABS(diffbrad)>0.025)
= IF(Testrad)
= SET.NAME("continuerad",1)
= IF(Testradtwo)
= SET.NAME("continuerad",0)
= END.IF()
= END.IF()
=NEXT()

=CALCULATION(1)
=DEFINE.NAME("Ilim",Ilim)
=DEFINE.NAME("Ilimrad",Ilimrad)
=SET.NAME("szero",((fIlesr*rholle*cplle*(Tc-Top))/((1-fIlesr)*(Jcop*1000)*fsc*(1/Cf)))
=SET.NAME("srad",((fIlesr*rholle*cplle*(Tcrad-Top))/((1-fIlesr)*(Jcoprad*1000)*fsc*(1/Cf)))
=SET.NAME("s","srad/szero)
=SET.NAME("sloperad",s*slope)
=SET.NAME("deltaI",((Top-Ilim)/1000)
=SET.NAME("deltaIrad",((Top-Ilimrad)/1000)
=SET.NAME("deltaII",slope*deltaI+offset)
=SET.NAME("deltaIrad",sloperad*deltaIrad+offset)
=DEFINE.NAME("deltaII",deltaII)
=DEFINE.NAME("deltaIrad",deltaIrad)
=SET.NAME("deltaE",AIlesr/Acomp*(1-fCupure)*rholle*cplle*(Tcsop-Top)/1000)
=SET.NAME("deltaErad",AIlesr/Acomp*(1-fCupure)*rholle*cplle*(Tcsoprad-Top)/1000)
=DEFINE.NAME("deltaE",deltaE)
=DEFINE.NAME("deltaErad",deltaErad)
=FORMULA(rhorad/100,!$D$202)
=FORMULA(rhoB/100,!$D$203)

=MESSAGE(FALSE)
=BEEP()
=CALCULATION(3)

=RETURN()

```

This section determines the actual stability margin from the limiting current.

Here we pass back the stability margins at zero fluence and at the given fluence. Here we pass back the resistivities and the enthalpy margin used by Bottura.

End of macro.

Variable Listing:

```

=!Acomp
=!AHesr
=-240
=!Astrands

```

```

Acomp
AHesr
alpha
Astrands

```

=Macintosh HD:Hard Thesis:MagRad:[MagRadBook]SuperOp"!Bcto	Bcto
=Macintosh HD:Hard Thesis:MagRad:[MagRadBook]SuperOp"!Bctorad	Bctorad
=0.09676	betaHe
=13.968563950327	Blimo
=11.6071125903863	Blimorad
=!Bop	Bop
=!Cf	Cf
=-180.9	constant
=1	continue
=1	continuerad
=1000*!cpHe	cpHe
=!D	D
=1153.58744543449	deltaE
=942.489563197823	deltaErad
=1303.68709093162	deltaH
=375.313394503396	deltaHrad
=-9.38523354251402	deltaI
=-1.39680343176985	deltaIrad
=5.59363518613392	deltaT
=5.6716791997729	deltaTrad
=4*(AHesr*0.000001)/D	Dhfrict
=0.0052273403500781	diffb
=0.00499121700743982	diffbrad
=19.0828212737324	diffIlim
=19.5269987573701	diffIlimrad
=fluence/2110	dpa
=300*(1-EXP(alpha*dpa))	drho
=!fCucomp	fCucomp
=!fCupure	fCupure
=AHesr/(AHesr+Astrands)	fHesr
=!Fluence	fluence
=!fscStab	fsc
=6490	gamma
=757.716342696974	h
=762.148568987164	hrad
=0.5*(pie*kHe*rhoHe*cpHe/tau^(1/3))^0.5	hsemiinf
=47275.233542514	Ilim
=47256.1507212402	Ilimo
=39267.2764330124	Ilimorad
=39286.8034317698	Ilimrad
=0.0000789968554373875	increment
=0.0000738216264346197	incrementrad
=!Iop	Iop
=Macintosh HD:Hard Thesis:MagRad:[MagRadBook]SuperOp"!Jcop	Jcop
=Macintosh HD:Hard Thesis:MagRad:[MagRadBook]SuperOp"!Jcoprad	Jcoprad
=0.0225492868737026	kHe
=!l/100	l

=0.500822304988064	littlebo
=0.419900987918328	littleborad
=0.506049645338143	littlebt
=0.620563151630017	littlebto
=0.620563151630017	littlebtorad
=0.424892204925768	littlebtrad
=0.598271343346772	littleto
=0.665994260049493	littletorad
=3.47883446795588E-06	muHe
=20.2342208058747	Nu
=20.3525799336428	Nurad
=215	offset
=!Pav	Pav
=3.1415927	pie
=0.7032	Pr
=1177.13143612725	qwall
=1193.55511389653	qwallrad
=9638.60598623741	Re
=9773.086600725	Rerad
=297.875697420478	rhoB
=110	rhoBann
=!rhoBanno	rhoBanno
=74.4848938162403	rhoBzero
=1000*!rhoHe	rhoHe
=172.957500566442	rhorad
=!rhozero	rhozero
=0.989410528459182	s
=gamma*tau+constant	slope
=-114.771621301265	sloperad
=9.58709777490146	srad
=9.68970664768601	szero
=!tau/1000	tau
=10.6001430452435	Tc
=!Macintosh HD:Hard Thesis:MagRad:[MagRadBook]SuperOp!'Tco	Tco
=!Macintosh HD:Hard Thesis:MagRad:[MagRadBook]SuperOp!'Tcorad	Tcorad
=10.6781870588825	Tcrad
=!Tcsop	Tcsop
=!Tcsoprad	Tcsoprad
=TRUE	Test
=TRUE	Testrad
=FALSE	Testradtwo
=FALSE	Testtwo
=!Top	Top
=0.410653848109949	vHe
=0.416383409201499	vHerad
=1.11748511602616	x
=0.170692832211564	xrad

Appendix F: A Macro Example - RadQuench

This macro, explained in Chapter 5 of the main text, calculates the temperatures achieved in the conductor in the event of a quench. The first part of the appendix is a code listing, followed by a listing of the variables defined, and the calculational workspace.

Protection Macro

```
=CALCULATION(3)
=ECHO(FALSE)
=WORKBOOK.ACTIVATE(FALSE)
=WORKBOOK.SELECT("[MagRadBook]RadQuench","[MagRadBook]RadQuench")
=WORKBOOK.ACTIVATE("[MagRadBook]RadQuench",TRUE)
=HIDE()
=ECHO(TRUE)
=WORKBOOK.SELECT("[MagRadBook]MagRad Master CDA","[MagRadBook]MagRad Master CI
=WORKBOOK.ACTIVATE("[MagRadBook]MagRad Master CDA",FALSE)
=ECHO(FALSE)
=WORKBOOK.ACTIVATE("[MagRadBook]RadQuench",TRUE)
=HIDE()

=MESSAGE(TRUE,"Initiating quench parameters")
=FORMULA.GOTO($C$10)
=FOR("i",1,Tupper/2,1)
= SET.VALUE(OFFSET(Temperature,i-1,0),2*i)
= IF(rhoB<rhoBanno)
=   SET.NAME("rhocon",rhoB)
= ELSE.IF(rhoB>=rhoBanno)
=   SET.NAME("rhocon",rhoBlimit)
= END.IF()
= SET.NAME("T",ACTIVE.CELL())
= SET.VALUE(OFFSET(rhoT,i-1,0),((T^4)/(17.1*T^3+10390000))+rhocon)
= SET.VALUE(OFFSET(CpCu,i-1,0),1/(0.00000030028+(1/(71.875*T+8.5673*T^3))))
= SET.VALUE(OFFSET(Cpcore,i-1,0),1/(0.0000004664965+(1/(903.2403*T+8.874552*T^3))))
= SELECT("R[1]C")
=NEXT()

=FORMULA.GOTO($D$10)
=FOR("i",1,Tupper/2,1)
= IF(flucence>2.11*dpalimit)
=   SET.NAME("flucencecheck",2.11*dpalimit)
= ELSE.IF(flucence<=2.11*dpalimit)
```

Commentation:

This section of the module makes RadQuench the active document, while keeping it hidden and displaying the MagRad Master.

This is the calculational body of the module. It calculates all of the relevant values necessary in order to return quench parameters.

Resistivity calculations.

Rho of T and dpa.

Copper heat capacity.

Superconductor heat capacity.

More resistivity calculations.

```

= SET.NAME("fluencecheck",fluence)
= END.IF0
= SET.NAME("rhoTc",ACTIVE.CELL0)
= SET.NAME("xc",Bop/(100*rhoTc))
= SET.VALUE(OFFSET(rhoBT,i-1,0),rhoTc/(ATAN((0.7698-0.0215*fluencecheck)*9.683*xc)/(9.4
= SELECT("R{1}C")
=NEXT0

```

x parameter for resistivity.
Rho of T, dpa, and B.

```

=FORMULA.GOTO($E$10)
=FOR("j",1,(Tupper/2-1),1)
= SET.NAME("magnetores",ACTIVE.CELL0)
= SELECT("RC{1}")
= SET.NAME("Cuheat",ACTIVE.CELL0)
= SELECT("RC{1}")
= SET.NAME("coreheat",ACTIVE.CELL0)
= SET.VALUE(OFFSET(Aone,j-1,0),0.14*3100/magnetores)
= SET.VALUE(OFFSET(Atwo,j-1,0),Cuheat/magnetores/1000)
= SET.VALUE(OFFSET(Athree,j-1,0),coreheat/magnetores/1000)

```

Quench material integrands:
Helium material integrand.
Copper material integrand.
S/C material integrand.

```

= IF(j>1)
= SELECT("RC{1}")
= SET.NAME("Ahelf",ACTIVE.CELL0)
= SELECT("R{-1}C")
= SET.NAME("Aheli",ACTIVE.CELL0)
= SELECT("RC{1}")
= SET.NAME("ACui",ACTIVE.CELL0)
= SELECT("R{1}C")
= SET.NAME("ACuf",ACTIVE.CELL0)
= SELECT("RC{1}")
= SET.NAME("Acoref",ACTIVE.CELL0)
= SELECT("R{-1}C")
= SET.NAME("Acorei",ACTIVE.CELL0)
= SELECT("RC{1}")
= SET.NAME("lheli",ACTIVE.CELL0)
= SELECT("RC{1}")
= SET.NAME("ICui",ACTIVE.CELL0)
= SELECT("RC{1}")
= SET.NAME("Icorei",ACTIVE.CELL0)
= SET.VALUE(OFFSET(lhelium,j-1,0),lheli+(Ahelf+Aheli))
= SET.VALUE(OFFSET(ICu,j-1,0),ICui+(ACuf+ACui))
= SET.VALUE(OFFSET(Icore,j-1,0),Icorei+(Acoref+Acorei))
= SELECT("R{1}C[-6]")
= END.IF0
= SELECT("R{1}C[-2]")

```

Here, if $j > 1$, we want to
the integrals to be $i-1$ th
integral plus $2*(A-A_{i-1})/2$, the
first factor of two being the T
differential and the second
factor of two being the means
of averaging.

Quench material integrals:
Helium material integral.
Copper material integral.
S/C material integral.

```

=NEXTQ
=BEEPQ

=MESSAGE(TRUE,"Calculating first phase maximum temperature")
=SELECT($N$11)
=SET.NAME("Irefone",$K$11)
=SET.NAME("Jrefone",$L$11)
=SET.NAME("Krefone",$M$11)
=WHILE(GET.CELL(2)<40)
= SELECT("RC[-3]")
= SET.NAME("Ione",ACTIVE.CELL())
= SELECT("RC[1]")
= SET.NAME("Jone",ACTIVE.CELL())
= SELECT("RC[1]")
= SET.NAME("Kone",ACTIVE.CELL())
= SELECT("RC[1]")
= SET.VALUE(ACTIVE.CELL(),SQRT((1/delaydump)*fheats*fCucs*((1-fcondheat)*(Ione-Irefc
= SELECT("R[1]C")
=NEXTQ

=SELECT($N$11)
=SET.NAME("Jcalcone",ACTIVE.CELL())
=WHILE(Jcalcone<Jop)
= SELECT("R[1]C")
= SET.NAME("Jcalcone",ACTIVE.CELL())
=NEXTQ
=SET.NAME("Ireftwo",ABSREF("R[1]C[-3]",ACTIVE.CELL()))
=SET.NAME("Jreftwo",ABSREF("R[1]C[-2]",ACTIVE.CELL()))
=SET.NAME("Kreftwo",ABSREF("R[1]C[-1]",ACTIVE.CELL()))
=SELECT("RC[-11]")
=SET.NAME("Tempone",ACTIVE.CELL())

=MESSAGE(TRUE,"First phase maximum temperature computed to be "&Tempone&" K")
=SELECT("R[1]C[11]:R[40]C[11]","RC")
=CLEARQ
=BEEPQ

=SELECT("RC[1]")
=SET.NAME("scraptwo",(GET.CELL(2))-4)
=SELECT(OFFSET(ACTIVE.CELL,-scraptwo,0,scraptwo,1))
=CLEARQ

=MESSAGE(TRUE,"Calculating second phase maximum temperature")
=SELECT(OFFSET(ACTIVE.CELL,scraptwo,0,1,1))

```

This section of the module calculates the current density which is required at the given quench temperature using the three phase equations for the current density as a function of the various quench parameters and the material integral values. The next phase of quench is assumed to begin when the required current density exceeds the conductor current density. In other words, when the joule heating is no longer sufficient for the required T rise.

Checks for when the first phase current density exceeds the operating current density.

Sets the second phase material integral reference values for smooth phase transition.

Passes back first phase max T.

Clears out trailing cells in the first phase.

Clears placeholder cells in the second phase.

Same as above, but for second

```
=WHILE(GET.CELL(2)<Tupper/4)
```

```
= SELECT("RC[-4]")
```

```
= SET.NAME("Itwo",ACTIVE.CELL())
```

```
= SELECT("RC[1]")
```

```
= SET.NAME("Jtwo",ACTIVE.CELL())
```

```
= SELECT("RC[1]")
```

```
= SET.NAME("Ktwo",ACTIVE.CELL())
```

```
= SELECT("RC[2]")
```

```
= SET.VALUE(ACTIVE.CELL(),SQRT((2/taudump)*(1/(EXP(-2*tdelaydump/taudump)-EXP(-2*tdelaydump/taudump))))
```

```
= SELECT("R[1]C")
```

```
=NEXT()
```

phase of the quench.

Sets value of the minimum current density needed to heat the conductor to the quench T.

```
=SELECT($O$11)
```

```
=SET.NAME("Jcalctwo",ACTIVE.CELL())
```

```
=WHILE(Jcalctwo<Jop)
```

```
= SELECT("R[1]C")
```

```
= SET.NAME("Jcalctwo",ACTIVE.CELL())
```

```
=NEXT()
```

Checks for when the second phase current density exceeds the operating current density.

```
=SET.NAME("Irefthree",ABSREF("R[1]C[-4]",ACTIVE.CELL()))
```

```
=SET.NAME("Jrefthree",ABSREF("R[1]C[-3]",ACTIVE.CELL()))
```

```
=SET.NAME("Krefthree",ABSREF("R[1]C[-2]",ACTIVE.CELL()))
```

```
=SELECT("RC[-12]")
```

Sets the third phase material integral reference values for smooth phase transition.

```
=SET.NAME("Temptwo",ACTIVE.CELL())
```

Passes back second phase T.

```
=MESSAGE(TRUE,"Second phase maximum temperature calculated to be "&Temptwo&" K")
```

```
=SELECT("R[1]C[12]:R[600]C[12]", "RC")
```

```
=CLEAR()
```

Clears out trailing cells in the second phase.

```
=SELECT("RC[1]")
```

```
=SET.NAME("scraphthree",(GET.CELL(2))-4)
```

```
=SELECT(OFFSET(ACTIVE.CELL(),-scraphthree,0,scraphthree,1))
```

```
=CLEAR()
```

```
=BEEP()
```

Clears placeholder cells in the third phase.

```
=MESSAGE(TRUE,"Calculating third phase maximum temperature")
```

```
=SELECT(OFFSET(ACTIVE.CELL(),scraphthree,0,1,1))
```

```
=WHILE(GET.CELL(2)<Tupper/2)
```

```
= SELECT("RC[-5]")
```

```
= SET.NAME("Ithree",ACTIVE.CELL())
```

```
= SELECT("RC[1]")
```

```
= SET.NAME("Jthree",ACTIVE.CELL())
```

```
= SELECT("RC[1]")
```

```
= SET.NAME("Kthree",ACTIVE.CELL())
```

```
= SELECT("RC[3]")
```

Same as above, but for third phase of the quench.

```
= SET.VALUE(ACTIVE.CELL(),SQRT((2/taudump)*EXP(2*tdelayheat/taudump)*fhcattum*fCui)
= SELECT("R[1]C")
=NEXT()
```

```
=SELECT($P$11)
=SET.NAME("Jcalcthree",ACTIVE.CELL())
=WHILE(Jcalcthree<Jop)
= SELECT("R[1]C")
= SET.NAME("Jcalcthree",ACTIVE.CELL())
=NEXT()
=SELECT("RC[-13]")
=SET.NAME("Tempthree",ACTIVE.CELL())
```

Checks for when the third phase current density exceeds the operating current density.

Passes back third phase max T.

```
=MESSAGE(TRUE,"Third phase maximum temperature calculated to be "&Tempthree&" K")
=SELECT("R[1]C[13]:R[1200]C[13]","RC")
=CLEAR()
```

Clears out trailing cells in the third phase.

```
=CLOSE(TRUE)
```

Closes RadQuench.

```
=FORMULA(Tempone,!$D$246)
=FORMULA(Temptwo,!$D$247)
=FORMULA(Tempthree,!$D$248)
```

Passes out maximum quench phase temperatures to MagRad Master.

```
=MESSAGE(FALSE)
=BEEP()
=RETURN()
```

End of macro.

Variable Listing:

=KS303	Acoref
=KS302	Acorei
=JS303	ACuf
=JS302	ACui
=IS303	Ahelf
=IS302	Aheli
=IS5	Aone
=KS5	Athree
=JS5	Atwo
=Macintosh HD:Hard Thesis:MagRad:MagRad Master CDA!SCS170	Bop
=HS303	coreheat
=HS5	Cpcore
=GS5	CpCu

=SGS303	Cuheat
=Macintosh HD:Hard Thesis:MagRad:MagRad Master CDA!SDS220	dpalimit
=Macintosh HD:Hard Thesis:MagRad:MagRad Master CDA!SES231	fcondheat
=Macintosh HD:Hard Thesis:MagRad:MagRad Master CDA!SES230	fCucs
=Macintosh HD:Hard Thesis:MagRad:MagRad Master CDA!SES232	fCuheat
=Macintosh HD:Hard Thesis:MagRad:MagRad Master CDA!SES237	fCust
=Macintosh HD:Hard Thesis:MagRad:MagRad Master CDA!SES235	fCuturn
=Macintosh HD:Hard Thesis:MagRad:MagRad Master CDA!SES229	fheats
=Macintosh HD:Hard Thesis:MagRad:MagRad Master CDA!SES234	fheatturn
=Macintosh HD:Hard Thesis:MagRad:MagRad Master CDA!SCS172	fluence
=1.782	fluencecheck
=Macintosh HD:Hard Thesis:MagRad:MagRad Master CDA!SES236	fmatal
=Macintosh HD:Hard Thesis:MagRad:MagRad Master CDA!SES238	fsc
=Macintosh HD:Hard Thesis:MagRad:MagRad Master CDA!SES233	fscheat
=301	i
=SNS5	Icore
=SNS302	Icorei
=SMS5	ICu
=SMS302	ICui
=SLS302	Iheli
=SLS5	Ihelium
=SLS41	Ione
=SLS6	Irefone
=SLS101	Irefthree
=SLS27	Ireftwo
=SLS301	Ithree
=SLS151	Itwo
=300	j
=SOS26	Jcalcone
=SQS128	Jcalcthree
=SPS100	Jcaletwo
=SMS41	Jone
=Macintosh HD:Hard Thesis:MagRad:MagRad Master CDA!SCS174	Jop
=SMS6	Jrefone
=SMS101	Jrefthree
=SMS27	Jreftwo
=SMS301	Jthree
=SMS151	Jtwo
=SNS41	Kone
=SNS6	Krefone
=SNS101	Krefthree
=SNS27	Kreftwo
=SNS301	Kthree
=SNS151	Ktwo

=SF\$303	magnetores
='Macintosh HD:Hard Thesis:MagRad:MagRad Master CDA'!SDS203	rhoB
='Macintosh HD:Hard Thesis:MagRad:MagRad Master CDA'!SDS192	rhoBanno
=SF\$5	rhoBT
=0.675410779149911	rhocon
='Macintosh HD:Hard Thesis:MagRad:MagRad Master CDA'!SDS202	rhorad
='Macintosh HD:Hard Thesis:MagRad:[MagRadBook]StabAnneal'!rhorad/100	rhoradlimit
=\$E\$5	rhoT
=\$E\$304	rhoTc
=95	scrapthree
=21	scraptwo
=\$D\$304	T
='Macintosh HD:Hard Thesis:MagRad:MagRad Master CDA'!SDS240	taudump
='Macintosh HD:Hard Thesis:MagRad:MagRad Master CDA'!SDS241	tdelaydump
='Macintosh HD:Hard Thesis:MagRad:MagRad Master CDA'!SDS242	tdelayheat
=\$D\$5	Temperature
=\$D\$26	Tempone
=\$D\$128	Tempthree
=\$D\$100	Temptwo
=600	Tupper
=0.00314035953822727	xc

Macro Calculation:

This section of the RadQuench macro calculates all of the relevant materials properties of the conductor, and calculates the quench temperatures as directed by the code section of the RadQuench macro.

Temp [K]	rho(T) [nohm·m]	rho(B,T) [nohm·m]	Cp,cu [J/cum·K]	Cp,core [J/cum·K]	He Material Integrand	Cu Material Integrand
2	0.675	1.082	212	1876	401.14	0.20
4	0.675	1.082	836	4173	401.13	0.77
6	0.676	1.082	2280	7311	401.10	2.11
8	0.676	1.082	4954	11705	401.01	4.58
10	0.676	1.083	9260	17759	400.82	8.55
12	0.677	1.084	15593	25858	400.49	14.39
14	0.679	1.085	24336	36369	399.94	22.43
16	0.682	1.087	35852	49626	399.10	32.97
18	0.685	1.091	50481	65923	397.89	46.28
20	0.691	1.095	68536	85509	396.22	62.57
22	0.698	1.102	90290	108575	394.01	81.97
24	0.707	1.110	115975	135251	391.15	104.53
26	0.718	1.120	145774	165600	387.57	130.18
28	0.733	1.133	179818	199609	383.19	158.77
30	0.750	1.148	218177	237198	377.95	190.00
32	0.771	1.167	260863	278212	371.81	223.48
34	0.796	1.190	307822	322431	364.74	258.70
36	0.826	1.217	358942	369577	356.75	295.06
38	0.859	1.248	414048	419318	347.89	331.90
40	0.898	1.283	472911	471287	338.22	368.54
42	0.942	1.324	535250	525087	327.82	404.30
44	0.992	1.370	600741	580305	316.82	438.54
46	1.047	1.421	669022	636526	305.34	470.68
48	1.108	1.479	739706	693343	293.52	500.28
50	1.174	1.542	812387	750364	281.52	526.97
52	1.247	1.611	886649	807223	269.47	550.52
54	1.325	1.685	962074	863584	257.51	570.83
56	1.410	1.766	1038254	919148	245.74	587.88
58	1.500	1.853	1114794	973653	234.27	601.77
60	1.596	1.945	1191317	1026875	223.19	612.64
62	1.697	2.042	1267474	1078630	212.54	620.70
64	1.803	2.145	1342945	1128769	202.37	626.20
66	1.915	2.252	1417439	1177177	192.71	629.38
68	2.032	2.364	1490700	1223774	183.57	630.51
70	2.152	2.481	1562504	1268505	174.95	629.85
72	2.278	2.601	1632662	1311340	166.84	627.63
74	2.407	2.726	1701014	1352274	159.23	624.09
76	2.540	2.853	1767433	1391316	152.10	619.42
78	2.676	2.984	1831818	1428493	145.43	613.82
80	2.815	3.118	1894097	1463844	139.19	607.45
82	2.957	3.254	1954220	1497417	133.35	600.47
84	3.101	3.393	2012158	1529267	127.90	592.99
86	3.248	3.534	2067900	1559458	122.80	585.13
88	3.396	3.677	2121452	1588053	118.04	576.99
90	3.546	3.821	2172836	1615121	113.58	568.65
92	3.697	3.967	2222082	1640731	109.41	560.18
94	3.850	4.114	2269232	1664954	105.50	551.64
96	4.004	4.262	2314335	1687857	101.84	543.07
98	4.158	4.410	2357446	1709509	98.41	534.53
100	4.313	4.560	2398628	1729975	95.18	526.05
102	4.469	4.710	2437942	1749321	92.15	517.65
104	4.624	4.860	2475457	1767608	89.30	509.35

106	4.780	5.011	2511240	1784895	86.62	501.19
108	4.936	5.161	2545360	1801240	84.09	493.16
110	5.092	5.312	2577885	1816696	81.70	485.28
112	5.248	5.463	2608885	1831315	79.45	477.57
114	5.403	5.613	2638426	1845146	77.31	470.02
116	5.558	5.764	2666576	1858235	75.30	462.63
118	5.713	5.914	2693397	1870626	73.38	455.42
120	5.867	6.064	2718953	1882359	71.57	448.39
122	6.021	6.213	2743304	1893474	69.85	441.52
124	6.174	6.362	2766510	1904008	68.21	434.83
126	6.327	6.511	2788625	1913993	66.66	428.30
128	6.479	6.659	2809704	1923463	65.18	421.94
130	6.631	6.807	2829799	1932448	63.76	415.75
132	6.782	6.954	2848959	1940976	62.41	409.71
134	6.932	7.100	2867231	1949074	61.13	403.83
136	7.081	7.246	2884661	1956767	59.90	398.10
138	7.230	7.391	2901289	1964078	58.72	392.53
140	7.378	7.536	2917159	1971029	57.59	387.09
142	7.526	7.680	2932307	1977641	56.51	381.80
144	7.673	7.824	2946771	1983934	55.47	376.64
146	7.819	7.967	2960586	1989924	54.48	371.61
148	7.964	8.109	2973784	1995630	53.52	366.72
150	8.109	8.251	2986397	2001067	52.60	361.94
152	8.253	8.392	2998454	2006250	51.71	357.29
154	8.397	8.533	3009983	2011193	50.86	352.75
156	8.540	8.673	3021012	2015910	50.04	348.32
158	8.682	8.813	3031564	2020412	49.25	344.01
160	8.823	8.952	3041664	2024711	48.48	339.79
162	8.964	9.090	3051334	2028818	47.75	335.68
164	9.105	9.228	3060595	2032743	47.03	331.67
166	9.245	9.365	3069468	2036496	46.34	327.75
168	9.384	9.502	3077972	2040086	45.67	323.93
170	9.523	9.638	3086124	2043521	45.03	320.19
172	9.661	9.774	3093942	2046810	44.40	316.54
174	9.799	9.910	3101441	2049959	43.80	312.97
176	9.936	10.045	3108638	2052977	43.21	309.48
178	10.072	10.179	3115546	2055868	42.64	306.07
180	10.209	10.313	3122180	2058641	42.08	302.74
182	10.344	10.447	3128552	2061301	41.54	299.48
184	10.479	10.580	3134674	2063853	41.02	296.29
186	10.614	10.713	3140558	2066302	40.51	293.16
188	10.748	10.845	3146215	2068654	40.02	290.11
190	10.882	10.977	3151656	2070914	39.54	287.12
192	11.016	11.108	3156890	2073085	39.07	284.19
194	11.149	11.240	3161927	2075172	38.61	281.32
196	11.282	11.371	3166776	2077179	38.17	278.51
198	11.414	11.501	3171444	2079109	37.74	275.75
200	11.546	11.631	3175941	2080966	37.31	273.06
202	11.677	11.761	3180274	2082754	36.90	270.41
204	11.808	11.890	3184449	2084476	36.50	267.82
206	11.939	12.020	3188474	2086134	36.11	265.27
208	12.070	12.148	3192356	2087731	35.72	262.78
210	12.200	12.277	3196100	2089271	35.35	260.33
212	12.330	12.405	3199713	2090756	34.99	257.93
214	12.459	12.533	3203199	2092187	34.63	255.58
216	12.589	12.661	3206565	2093568	34.28	253.26
218	12.718	12.788	3209815	2094901	33.94	250.99
220	12.846	12.916	3212954	2096187	33.60	248.77
222	12.975	13.043	3215987	2097429	33.28	246.58
224	13.103	13.169	3218918	2098628	32.96	244.43
226	13.231	13.296	3221751	2099787	32.64	242.32
228	13.359	13.422	3224491	2100906	32.34	240.24
230	13.486	13.548	3227140	2101989	32.03	238.20
232	13.613	13.674	3229703	2103035	31.74	236.20

234	13.740	13.799	3232183	2104047	31.45	234.23
236	13.867	13.925	3234584	2105026	31.17	232.29
238	13.993	14.050	3236907	2105973	30.89	230.39
240	14.120	14.175	3239157	2106890	30.62	228.52
242	14.246	14.300	3241337	2107777	30.35	226.67
244	14.372	14.424	3243448	2108637	30.09	224.86
246	14.497	14.549	3245494	2109469	29.83	223.08
248	14.623	14.673	3247476	2110275	29.58	221.33
250	14.748	14.797	3249399	2111057	29.33	219.60
252	14.873	14.921	3251262	2111814	29.09	217.90
254	14.998	15.044	3253070	2112548	28.85	216.23
256	15.123	15.168	3254823	2113260	28.61	214.58
258	15.248	15.291	3256525	2113951	28.38	212.96
260	15.372	15.415	3258176	2114621	28.15	211.37
262	15.496	15.538	3259778	2115271	27.93	209.80
264	15.620	15.661	3261334	2115902	27.71	208.25
266	15.744	15.784	3262845	2116515	27.50	206.72
268	15.868	15.906	3264313	2117109	27.28	205.22
270	15.992	16.029	3265738	2117687	27.08	203.74
272	16.116	16.152	3267124	2118248	26.87	202.28
274	16.239	16.274	3268470	2118793	26.67	200.84
276	16.362	16.396	3269778	2119323	26.47	199.42
278	16.486	16.518	3271051	2119837	26.27	198.03
280	16.609	16.640	3272288	2120338	26.08	196.65
282	16.732	16.762	3273491	2120825	25.89	195.29
284	16.854	16.884	3274661	2121298	25.71	193.95
286	16.977	17.006	3275799	2121758	25.52	192.63
288	17.100	17.127	3276907	2122206	25.34	191.33
290	17.222	17.249	3277986	2122642	25.16	190.04
292	17.345	17.370	3279035	2123066	24.99	188.78
294	17.467	17.491	3280057	2123478	24.81	187.53
296	17.589	17.612	3281052	2123880	24.64	186.29
298	17.711	17.733	3282021	2124272	24.47	185.07
300	17.833	17.854	3282965	2124653	24.31	183.87
302	17.955	17.975	3283885	2125024	24.14	182.69
304	18.077	18.096	3284781	2125385	23.98	181.52
306	18.199	18.217	3285655	2125738	23.82	180.36
308	18.320	18.338	3286506	2126081	23.67	179.22
310	18.442	18.458	3287336	2126416	23.51	178.10
312	18.563	18.579	3288145	2126742	23.36	176.98
314	18.685	18.699	3288934	2127060	23.21	175.89
316	18.806	18.820	3289703	2127370	23.06	174.80
318	18.927	18.940	3290454	2127673	22.91	173.73
320	19.048	19.060	3291186	2127968	22.77	172.67
322	19.169	19.180	3291901	2128256	22.63	171.63
324	19.290	19.300	3292598	2128537	22.49	170.60
326	19.411	19.420	3293279	2128811	22.35	169.58
328	19.532	19.540	3293943	2129079	22.21	168.57
330	19.653	19.660	3294592	2129340	22.08	167.58
332	19.774	19.780	3295225	2129595	21.94	166.59
334	19.894	19.900	3295844	2129844	21.81	165.62
336	20.015	20.020	3296448	2130087	21.68	164.66
338	20.135	20.139	3297038	2130325	21.55	163.71
340	20.256	20.259	3297614	2130557	21.42	162.77
342	20.376	20.378	3298178	2130783	21.30	161.85
344	20.497	20.498	3298728	2131005	21.17	160.93
346	20.617	20.617	3299266	2131221	21.05	160.02
348	20.737	20.737	3299792	2131433	20.93	159.13
350	20.857	20.856	3300306	2131640	20.81	158.24
352	20.977	20.975	3300809	2131842	20.69	157.37
354	21.097	21.095	3301300	2132039	20.57	156.50
356	21.217	21.214	3301781	2132233	20.46	155.64
358	21.337	21.333	3302251	2132422	20.34	154.80
360	21.457	21.452	3302710	2132606	20.23	153.96

362	21.577	21.571	3303160	2132787	20.12	153.13
364	21.697	21.690	3303600	2132964	20.01	152.31
366	21.817	21.809	3304031	2133137	19.90	151.50
368	21.937	21.928	3304452	2133307	19.79	150.69
370	22.056	22.047	3304865	2133472	19.69	149.90
372	22.176	22.166	3305269	2133635	19.58	149.11
374	22.296	22.285	3305664	2133794	19.48	148.34
376	22.415	22.404	3306051	2133949	19.37	147.57
378	22.535	22.522	3306430	2134101	19.27	146.81
380	22.654	22.641	3306801	2134250	19.17	146.05
382	22.774	22.760	3307165	2134396	19.07	145.31
384	22.893	22.879	3307521	2134539	18.97	144.57
386	23.013	22.997	3307869	2134679	18.87	143.84
388	23.132	23.116	3308211	2134817	18.78	143.11
390	23.251	23.234	3308546	2134951	18.68	142.40
392	23.370	23.353	3308874	2135083	18.58	141.69
394	23.490	23.471	3309195	2135212	18.49	140.99
396	23.609	23.590	3309510	2135338	18.40	140.29
398	23.728	23.708	3309819	2135462	18.31	139.61
400	23.847	23.827	3310122	2135584	18.21	138.93
402	23.966	23.945	3310418	2135703	18.12	138.25
404	24.085	24.063	3310709	2135820	18.04	137.58
406	24.204	24.182	3310995	2135934	17.95	136.92
408	24.323	24.300	3311275	2136047	17.86	136.27
410	24.442	24.418	3311549	2136157	17.77	135.62
412	24.561	24.537	3311818	2136265	17.69	134.97
414	24.680	24.655	3312082	2136371	17.60	134.34
416	24.799	24.773	3312341	2136475	17.52	133.71
418	24.918	24.891	3312595	2136577	17.44	133.08
420	25.037	25.009	3312845	2136677	17.35	132.46
422	25.156	25.127	3313089	2136775	17.27	131.85
424	25.275	25.246	3313329	2136871	17.19	131.24
426	25.393	25.364	3313565	2136966	17.11	130.64
428	25.512	25.482	3313796	2137058	17.03	130.05
430	25.631	25.600	3314023	2137150	16.95	129.46
432	25.750	25.718	3314246	2137239	16.88	128.87
434	25.868	25.836	3314465	2137327	16.80	128.29
436	25.987	25.954	3314680	2137413	16.72	127.72
438	26.106	26.072	3314891	2137498	16.65	127.15
440	26.224	26.190	3315098	2137581	16.57	126.58
442	26.343	26.307	3315302	2137662	16.50	126.02
444	26.461	26.425	3315502	2137742	16.42	125.47
446	26.580	26.543	3315698	2137821	16.35	124.92
448	26.698	26.661	3315891	2137898	16.28	124.37
450	26.817	26.779	3316080	2137974	16.21	123.83
452	26.935	26.897	3316266	2138049	16.14	123.30
454	27.054	27.015	3316449	2138122	16.07	122.77
456	27.172	27.132	3316629	2138194	16.00	122.24
458	27.291	27.250	3316806	2138265	15.93	121.72
460	27.409	27.368	3316979	2138335	15.86	121.20
462	27.528	27.486	3317150	2138403	15.79	120.69
464	27.646	27.603	3317318	2138470	15.72	120.18
466	27.764	27.721	3317482	2138537	15.66	119.67
468	27.883	27.839	3317644	2138602	15.59	119.17
470	28.001	27.956	3317804	2138665	15.52	118.68
472	28.119	28.074	3317960	2138728	15.46	118.19
474	28.237	28.192	3318114	2138790	15.39	117.70
476	28.356	28.309	3318266	2138851	15.33	117.21
478	28.474	28.427	3318415	2138910	15.27	116.73
480	28.592	28.545	3318561	2138969	15.20	116.26
482	28.710	28.662	3318705	2139027	15.14	115.79
484	28.829	28.780	3318847	2139084	15.08	115.32
486	28.947	28.897	3318987	2139139	15.02	114.85
488	29.065	29.015	3319124	2139194	14.96	114.39

490	29.183	29.132	3319259	2139249	14.90	113.94
492	29.301	29.250	3319391	2139302	14.84	113.48
494	29.419	29.367	3319522	2139354	14.78	113.03
496	29.538	29.485	3319651	2139406	14.72	112.59
498	29.656	29.602	3319777	2139456	14.66	112.15
500	29.774	29.720	3319902	2139506	14.60	111.71
502	29.892	29.837	3320024	2139555	14.55	111.27
504	30.010	29.955	3320145	2139604	14.49	110.84
506	30.128	30.072	3320263	2139651	14.43	110.41
508	30.246	30.190	3320380	2139698	14.38	109.98
510	30.364	30.307	3320495	2139744	14.32	109.56
512	30.482	30.424	3320608	2139789	14.26	109.14
514	30.600	30.542	3320720	2139834	14.21	108.73
516	30.718	30.659	3320830	2139878	14.16	108.31
518	30.836	30.777	3320938	2139921	14.10	107.90
520	30.954	30.894	3321044	2139964	14.05	107.50
522	31.072	31.011	3321149	2140006	13.99	107.10
524	31.190	31.129	3321252	2140047	13.94	106.69
526	31.308	31.246	3321354	2140088	13.89	106.30
528	31.426	31.363	3321454	2140128	13.84	105.90
530	31.544	31.481	3321553	2140168	13.79	105.51
532	31.661	31.598	3321650	2140207	13.74	105.12
534	31.779	31.715	3321746	2140245	13.68	104.74
536	31.897	31.832	3321840	2140283	13.63	104.35
538	32.015	31.950	3321933	2140320	13.58	103.97
540	32.133	32.067	3322025	2140357	13.53	103.60
542	32.251	32.184	3322115	2140393	13.48	103.22
544	32.369	32.301	3322204	2140428	13.44	102.85
546	32.486	32.419	3322291	2140463	13.39	102.48
548	32.604	32.536	3322378	2140498	13.34	102.11
550	32.722	32.653	3322463	2140532	13.29	101.75
552	32.840	32.770	3322547	2140566	13.24	101.39
554	32.958	32.888	3322629	2140599	13.20	101.03
556	33.075	33.005	3322711	2140631	13.15	100.67
558	33.193	33.122	3322791	2140664	13.10	100.32
560	33.311	33.239	3322870	2140695	13.06	99.97
562	33.429	33.356	3322948	2140727	13.01	99.62
564	33.547	33.474	3323025	2140757	12.97	99.27
566	33.664	33.591	3323101	2140788	12.92	98.93
568	33.782	33.708	3323176	2140818	12.88	98.59
570	33.900	33.825	3323250	2140847	12.83	98.25
572	34.017	33.942	3323323	2140876	12.79	97.91
574	34.135	34.059	3323394	2140905	12.74	97.58
576	34.253	34.176	3323465	2140934	12.70	97.24
578	34.371	34.294	3323535	2140961	12.66	96.91
580	34.488	34.411	3323604	2140989	12.61	96.59
582	34.606	34.528	3323672	2141016	12.57	96.26
584	34.724	34.645	3323739	2141043	12.53	95.94
586	34.841	34.762	3323805	2141070	12.48	95.62
588	34.959	34.879	3323870	2141096	12.44	95.30
590	35.077	34.996	3323934	2141121	12.40	94.98
592	35.194	35.113	3323998	2141147	12.36	94.66
594	35.312	35.230	3324060	2141172	12.32	94.35
596	35.429	35.347	3324122	2141197	12.28	94.04
598	35.547	35.464	3324183	2141221	12.24	93.73
600	35.665	35.582	3324243	2141245		

S/C Material Integrand	He Material Integral	Cu Material Integral	S/C Material Integral	First Phase J	Second Phase J	Third Phase J
1.73						
3.86	802	1	6	0.00		
6.76	1604	4	16	11.37		
10.82	2407	11	34	16.12		
16.40	3208	24	61	19.81		
23.86	4010	47	101	22.98		
33.52	4810	83	159	25.83		
45.64	5609	139	238	28.49		
60.44	6406	218	344	31.02		
78.07	7200	327	482	33.48		
98.57	7991	471	659	35.90		
121.90	8776	658	879	38.30		
147.88	9554	893	1149	40.71		
176.24	10325	1182	1473	43.14		
206.57	11086	1530	1856	45.61		
238.34	11836	1944	2301	48.10		
270.97	12573	2426	2810	50.63		
303.80	13294	2980	3385	53.20		
336.12	13999	3607	4025	55.80		
367.28	14685	4307	4729	58.41		
396.62	15351	5080	5492	61.04		
423.62	15996	5923	6313	63.68		
447.82	16618	6832	7184		0.00	
468.92	17217	7803	8101		8.08	
486.74	17792	8830	9056		11.54	
501.21	18343	9908	10044		14.25	
512.39	18870	11029	11058		16.58	
520.44	19373	12188	12091		18.66	
525.58	19853	13377	13137		20.56	
528.07	20310	14592	14191		22.31	
528.22	20746	15825	15247		23.95	
526.33	21161	17072	16301		25.49	
522.70	21556	18328	17350		26.93	
517.61	21932	19588	18391		28.30	
511.34	22291	20848	19420		29.60	
504.11	22633	22105	20435		30.84	
496.14	22959	23357	21435		32.01	
487.61	23270	24601	22419		33.14	
478.67	23568	25834	23385		34.21	
469.47	23852	27055	24333		35.23	
460.11	24125	28263	25263		36.22	
450.68	24386	29457	26174		37.16	
441.26	24637	30635	27066		38.06	
431.92	24877	31797	27939		38.93	
422.69	25109	32942	28794		39.77	
413.62	25332	34071	29630		40.57	
404.74	25547	35183	30448		41.35	
396.07	25754	36278	31249		42.09	
387.62	25955	37355	32033		42.82	
379.40	26148	38416	32800		43.52	
371.43	26336	39460	33551		44.19	
363.71	26517	40487	34286		44.84	

356.22	26693	41497	35006	45.48	
348.99	26864	42492	35711	46.09	
341.99	27029	43470	36402	46.68	
335.23	27191	44433	37079	47.26	
328.70	27347	45380	37743	47.82	
322.39	27500	46313	38394	48.37	
316.30	27649	47231	39033	48.90	
310.42	27794	48135	39660	49.41	
304.75	27935	49025	40275	49.91	
299.26	28073	49901	40879	50.40	
293.97	28208	50764	41472	50.88	
288.85	28340	51615	42055	51.35	
283.91	28469	52452	42628	51.80	
279.13	28595	53278	43191	52.24	
274.51	28718	54091	43744	52.68	
270.05	28839	54893	44289	53.10	
265.73	28958	55684	44825	53.51	
261.55	29074	56463	45352	53.92	
257.50	29188	57232	45871	54.31	
253.58	29300	57991	46382	54.70	
249.78	29410	58739	46885	55.08	
246.09	29518	59477	47381	55.45	
242.52	29624	60206	47870	55.81	
239.06	29729	60925	48351	56.17	
235.70	29831	61635	48826	56.52	
232.43	29932	62336	49294	56.86	
229.27	30032	63029	49756	57.20	
226.19	30129	63712	50211	57.53	
223.19	30225	64388	50661	57.86	
220.28	30320	65055	51104	58.18	
217.45	30414	65715	51542	58.49	
214.70	30506	66366	51974	58.80	
212.02	30596	67011	52401	59.10	
209.41	30686	67647	52822	59.40	
206.86	30774	68277	53239	59.69	
204.39	30861	68899	53650	59.98	
201.97	30947	69515	54056	60.26	
199.61	31032	70124	54458	60.54	
197.32	31115	70726	54855	60.81	
195.07	31198	71322	55247	61.09	
192.88	31279	71911	55635	61.35	
190.75	31360	72494	56019	61.61	
188.66	31439	73071	56398	61.87	
186.62	31518	73643	56773	62.13	
184.63	31596	74208	57145		0.00
182.68	31672	74768	57512		12.88
180.78	31748	75322	57875		18.17
178.91	31823	75871	58235		22.20
177.09	31898	76415	58591		25.57
175.31	31971	76953	58943		28.51
173.56	32044	77486	59292		31.16
171.85	32115	78014	59638		33.57
170.18	32187	78537	59980		35.81
168.54	32257	79055	60318		37.89
166.93	32326	79569	60654		39.84
165.36	32395	80078	60986		41.69
163.81	32464	80582	61315		43.44
162.30	32531	81082	61642		45.11
160.81	32598	81577	61965		46.71
159.36	32664	82068	62285		48.24
157.93	32730	82555	62602		49.71
156.53	32795	83037	62917		51.12
155.15	32859	83516	63228		52.49
153.80	32923	83990	63537		53.81

152.48	32986	84461	63843	55.09
151.17	33049	84927	64147	56.33
149.89	33111	85390	64448	57.53
148.64	33172	85849	64747	58.70
147.40	33233	86304	65043	59.83
146.19	33294	86756	65336	60.94
145.00	33354	87203	65628	62.01
143.82	33413	87648	65916	63.06
142.67	33472	88089	66203	
141.54	33530	88526	66487	
140.42	33588	88960	66769	
139.32	33646	89391	67049	
138.24	33703	89819	67326	
137.18	33759	90243	67602	
136.14	33815	90664	67875	
135.11	33871	91082	68146	
134.09	33926	91497	68415	
133.10	33981	91909	68683	
132.12	34035	92318	68948	
131.15	34089	92724	69211	
130.20	34143	93127	69472	
129.26	34196	93528	69732	
128.33	34249	93925	69990	
127.42	34301	94320	70245	
126.53	34353	94712	70499	
125.64	34405	95101	70751	
124.77	34456	95487	71002	
123.91	34507	95871	71250	
123.06	34557	96253	71497	
122.23	34607	96632	71743	
121.40	34657	97008	71986	
120.59	34707	97382	72228	
119.79	34756	97753	72469	
119.00	34805	98122	72708	
118.22	34853	98489	72945	
117.45	34901	98853	73180	
116.69	34949	99215	73415	
115.94	34996	99574	73647	
115.20	35044	99932	73878	
114.47	35091	100287	74108	
113.75	35137	100640	74336	
113.04	35183	100990	74563	
112.34	35229	101339	74788	
111.65	35275	101685	75012	
110.96	35320	102030	75235	
110.29	35366	102372	75456	
109.62	35410	102712	75676	
108.96	35455	103050	75895	
108.31	35499	103386	76112	
107.66	35543	103720	76328	
107.03	35587	104053	76543	
106.40	35630	104383	76756	
105.78	35674	104711	76968	
105.17	35717	105038	77179	
104.56	35759	105362	77389	
103.96	35802	105685	77597	
103.37	35844	106006	77805	
102.79	35886	106325	78011	
102.21	35928	106643	78216	
101.64	35969	106958	78420	
101.07	36011	107272	78622	
100.51	36052	107584	78824	
99.96	36092	107895	79025	
99.41	36133	108203	79224	

98.87	36173	108511	79422
98.34	36213	108816	79619
97.81	36253	109120	79816
97.29	36293	109422	80011
96.77	36333	109723	80205
96.26	36372	110022	80398
95.75	36411	110319	80590
95.25	36450	110615	80781
94.75	36488	110909	80971
94.26	36527	111202	81160
93.78	36565	111494	81348
93.30	36603	111783	81535
92.82	36641	112072	81721
92.35	36679	112359	81906
91.89	36716	112644	82090
91.43	36753	112928	82274
90.97	36790	113211	82456
90.52	36827	113492	82638
90.07	36864	113772	82818
89.63	36900	114051	82998
89.19	36937	114328	83177
88.76	36973	114604	83355
88.33	37009	114878	83532
87.90	37045	115151	83708
87.48	37080	115423	83883
87.06	37116	115694	84058
86.65	37151	115963	84232
86.24	37186	116231	84405
85.84	37221	116498	84577
85.44	37256	116764	84748
85.04	37291	117028	84918
84.64	37325	117291	85088
84.25	37359	117553	85257
83.87	37394	117814	85425
83.48	37427	118073	85592
83.10	37461	118331	85759
82.73	37495	118589	85925
82.35	37529	118845	86090
81.99	37562	119099	86254
81.62	37595	119353	86418
81.26	37628	119606	86581
80.90	37661	119857	86743
80.54	37694	120108	86904
80.19	37726	120357	87065
79.84	37759	120605	87225
79.49	37791	120852	87384
79.15	37824	121098	87543
78.81	37856	121343	87701
78.47	37888	121587	87858
78.13	37919	121830	88015
77.80	37951	122072	88171
77.47	37982	122313	88326
77.14	38014	122553	88481
76.82	38045	122792	88635
76.50	38076	123030	88788
76.18	38107	123266	88941
75.87	38138	123502	89093
75.55	38169	123737	89244
75.24	38199	123971	89395
74.93	38230	124204	89545
74.63	38260	124436	89695
74.33	38290	124667	89844
74.03	38320	124897	89992
73.73	38350	125127	90140

73.43	38380	125355	90287
73.14	38410	125582	90433
72.85	38440	125809	90579
72.56	38469	126035	90725
72.27	38499	126259	90870
71.99	38528	126483	91014
71.71	38557	126706	91158
71.43	38586	126928	91301
71.15	38615	127150	91443
70.88	38644	127370	91585
70.60	38672	127589	91727
70.33	38701	127808	91868
70.06	38729	128026	92008
69.80	38758	128243	92148
69.53	38786	128459	92287
69.27	38814	128675	92426
69.01	38842	128889	92564
68.75	38870	129103	92702
68.49	38898	129316	92839
68.24	38926	129528	92976
67.98	38953	129740	93112
67.73	38981	129950	93248
67.48	39008	130160	93383
67.24	39036	130369	93518
66.99	39063	130578	93652
66.75	39090	130785	93786
66.50	39117	130992	93919
66.26	39144	131198	94052
66.03	39171	131403	94184
65.79	39198	131608	94316
65.55	39224	131812	94447
65.32	39251	132015	94578
65.09	39277	132217	94709
64.86	39303	132419	94839
64.63	39330	132620	94968
64.40	39356	132820	95097
64.18	39382	133020	95226
63.95	39408	133219	95354
63.73	39434	133417	95482
63.51	39460	133615	95609
63.29	39485	133811	95736
63.07	39511	134008	95862
62.86	39536	134203	95988
62.64	39562	134398	96113
62.43	39587	134592	96239
62.22	39613	134786	96363
62.01	39638	134978	96487
61.80	39663	135171	96611
61.59	39688	135362	96735
61.39	39713	135553	96858
61.18	39738	135743	96980
60.98	39762	135933	97102
60.78	39787	136122	97224
60.58	39812	136310	97345
60.38	39836	136498	97466

Appendix G: Nb₃Sn Critical Parameters under Irradiation: Data Extrapolation:

First, we must define a few of the more important parameters. These parameters may vary from material to material.

$$\begin{aligned} B_{c2oet} &\equiv 24 \text{ Tesla} \\ T_{coi} &\equiv 17.19 \text{ Kelvin} \end{aligned}$$

$$\begin{aligned} \alpha &\equiv .063 \quad 1 \times 10^{18} \text{ cm}^2/\text{n} \\ T_{coset} &\equiv 16 \text{ Kelvin} \end{aligned}$$

Next, the data from Hahn, et al. (1991): TWC-247 must be input for manipulation,

$$i := 0..7$$

$$\phi_{\text{Hahn}_i} :=$$

0
.17
.427
.545
.795
2.005
3.5
5.0

$$B_{c2\text{Hahn}_i} :=$$

18.1
18.69
19.24
19.31
19.48
18.97

Tesla

$$J_{c\text{Hahn}_i} :=$$

162
259
338
379
378
292

A/mm²

$$1 \times 10^{18} \text{ RTNS-II n/cm}^2$$

Now we must construct the upper critical field at zero Kelvin and the critical temperature as functions of fluence (RTNS-II).

$$T_{co} := \overrightarrow{(T_{coi} \cdot \exp(-\alpha \cdot \phi_{\text{Hahn}}))} \quad T := 4.5 \text{ Kelvin}$$

$$j := 0..5$$

$$t_j := \frac{T}{T_{coj}}$$

$$B_{c2oext} := \left[\frac{B_{c2\text{Hahn}}}{((1 - t^2) \cdot (1 - 0.31 \cdot t^2 \cdot (1 - 1.77 \cdot \ln(t))))} \right] \text{ Tesla}$$

$$\begin{aligned}
 T_{co} &= \begin{bmatrix} 17.19 \\ 17.007 \\ 16.734 \\ 16.61 \\ 16.35 \\ 15.15 \\ 13.788 \\ 12.545 \end{bmatrix} \text{ Kelvin} & B_{c2oext} &= \begin{bmatrix} 20.931 \\ 21.675 \\ 22.41 \\ 22.538 \\ 22.837 \\ 22.766 \end{bmatrix} \text{ Tesla} \\
 \beta &:= \frac{T_{coset}}{T_{co0}} & \gamma &:= \frac{B_{c2oset}}{B_{c2oext0}} \\
 T_{coshift} &:= (T_{coset} \cdot \exp(-\alpha \cdot \phi_{Hahn})) \text{ Kelvin} & B_{c2oshift} &:= (\gamma \cdot B_{c2oext}) \text{ Tesla} \\
 B_{c2oshift_6} &:= 25.7 & B_{c2oshift_7} &:= 25.0 \\
 T_{coshift} &= \begin{bmatrix} 16 \\ 15.83 \\ 15.575 \\ 15.46 \\ 15.218 \\ 14.101 \\ 12.834 \\ 11.677 \end{bmatrix} \text{ Kelvin} & B_{c2oshift} &= \begin{bmatrix} 24 \\ 24.852 \\ 25.696 \\ 25.842 \\ 26.186 \\ 26.104 \\ 25.7 \\ 25 \end{bmatrix} \text{ Tesla}
 \end{aligned}$$

Next, we must find the leading coefficient in the expression for the critical current density, C_o . Since the critical current density was measured at 15 Tesla this is the field which will be used, and B_{c2Hahn} (data) may be used since this data is Hahn's Kramer extrapolation down to 4.5 Kelvin, which also was the temperature at which the J_c measurements were taken.

$$\begin{aligned}
 B &:= 15 \text{ Tesla} & b &:= \frac{B}{B_{c2Hahn}} \\
 C_{odataderived} &:= \frac{J_{cHahn}}{B_{c2Hahn}^{-\frac{1}{2}} \cdot (1-t^2)^2 \cdot b^{-\frac{1}{2}} \cdot (1-b)^2} \text{ AT}^{-1/2}/\text{mm}^2
 \end{aligned}$$

We must now determine J_c at a field of 12 Tesla and 4.2 Kelvin. These values have been chosen to correspond to the field and temperature specified by B. Montgomery at the ITER Magnets meeting held at the MIT Plasma Fusion Center, March 15-16, 1993.

$$T := 4.2 \text{ Kelvin}$$

$$B := 12 \text{ Tesla}$$

$$t_j := \frac{T}{T_{\infty j}}$$

$$B_{c2ITERspec} := \left[B_{c2oext} \cdot (1 - t^2) \cdot (1 - 0.31 \cdot t^2 \cdot (1 - 1.77 \cdot \ln(t))) \right] \text{ Tesla}$$

$$b := \frac{B}{B_{c2ITERspec}}$$

$$J_{cHahnITER} := \left[C_{odaladcrived} \cdot B_{c2Hahn}^{-\frac{1}{2}} \cdot (1 - t^2)^2 \cdot b^{-\frac{1}{2}} \cdot (1 - b)^2 \right] \text{ A/mm}^2$$

$$B_{c2ITERspec} = \begin{bmatrix} 18.409 \\ 19.015 \\ 19.585 \\ 19.661 \\ 19.844 \\ 19.379 \end{bmatrix} \text{ Tesla}$$

$$J_{cHahnITER} = \begin{bmatrix} 769.105 \\ 1.04 \cdot 10^3 \\ 1.201 \cdot 10^3 \\ 1.33 \cdot 10^3 \\ 1.287 \cdot 10^3 \\ 1.12 \cdot 10^3 \end{bmatrix} \text{ A/mm}^2$$

Finally, we must consider the critical current specifications, themselves. The previous calculations show what the most recent data on commercial wires give as the critical parameters as functions of fluence, including the critical current density. These values are quite close to the values which have been specified by the ITER Magnets group. The ITER Magnets group has specified a critical field of 24 or 28 Tesla depending upon whether binary or ternary Nb_3Sn is used, respectively, a critical temperature of 16 Kelvin, and a critical current density of 550 or 700 A/mm², dependent upon whether HPII (central solenoid) or HPI (TF coils) is under consideration, respectively. Since these values have not been experimentally achieved in fluence tested, commercial wires, we must assume that these values will be achieved through further research and development. Thus, for our purposes we will proceed upon the slightly optimistic assumption that these critical parameter values may be simultaneously increased and further that no penalty of increased susceptibility to radiation induced degradation will be incurred.

Toward the end of constructing a consistent set of fluence dependent data, it must be assumed, in the absence of experimental data, that the shapes of the critical curves versus fluence remain unchanged under shifts to higher critical values. This has already been done in both the critical temperature and the critical field cases above, and now this philosophy of data construction will be used for the critical current density. Moreover, the fluence dependence of the critical current coefficient, C_o , tacitly assumed to be fluence independent in previous studies (Summers, et al., 1991), can be extracted from the constructed data. There is no physical explanation for this dependence, but since a scaling law has been chosen containing only one arbitrary coefficient, we are forced to lump all unexplained dependences into that coefficient in order to accurately construct a data set in which good values are essential. By using the previously scaled up critical field and temperature data sets we will determine a value at each fluence for C_o , thus shifting the curve up and deforming its shape. The fluence dependent multiplier necessary to conform the shape of this curve to the curve derived from the data contains within it the fluence dependence of the coefficient, C_o .

$$J_{cITERset} := 700 \quad A/mm^2$$

$$\xi := \frac{J_{cITERset}}{J_{cHahnITER_0}}$$

$$\text{And we must add:} \quad J_{cHahnITER_6} := 1000 \quad J_{cHahnITER_7} := 840 \quad A/mm^2$$

Still,

$$T := 4.2 \quad \text{Kelvin} \quad B := 12 \quad \text{Tesla}$$

$$t := \frac{\overrightarrow{T}}{T_{cshift}}$$

$$B_{c2shift} := \left[\overrightarrow{B_{c2shift} \cdot (1 - t^2) \cdot (1 - 0.31 \cdot t^2 \cdot (1 - 1.77 \cdot \ln(t)))} \right] \quad \text{Tesla}$$

$$b := \frac{\overrightarrow{B}}{B_{c2shift}}$$

$$J_{cshift} := \left[\overrightarrow{C_{odataderived_0} \cdot B_{c2shift}^{-\frac{1}{2}} \cdot (1 - (t^2)^2)^{-\frac{1}{2}} \cdot (b)^{-\frac{1}{2}} \cdot (1 - b)^2} \right] \quad A/mm^2$$

$$C_o := C_{\text{odataderived}_0} \qquad C_{\text{datafluence}} := \frac{C_{\text{odataderived}}}{C_o}$$

$$C_{\text{relativeshift}} := \left(\frac{J_{\text{cHahnITER}}}{J_{\text{cshift}}} \right) \qquad C_{\text{shift}} := (C_o \cdot C_{\text{relativeshift}})$$

$$C_{\text{shiftfluence}} := \left[C_{\text{relativeshift}} + (1 - C_{\text{relativeshift}_0}) \right]$$

$$C_{\text{relativeshift}} = \begin{bmatrix} 0.702 \\ 0.875 \\ 0.946 \\ 1.04 \\ 0.991 \\ 0.945 \\ 1.001 \\ 1.086 \end{bmatrix} \qquad C_{\text{shiftfluence}} = \begin{bmatrix} 1 \\ 1.173 \\ 1.244 \\ 1.338 \\ 1.289 \\ 1.243 \\ 1.298 \\ 1.384 \end{bmatrix} \qquad C_{\text{datafluence}} = \begin{bmatrix} 1 \\ 1.207 \\ 1.271 \\ 1.392 \\ 1.314 \\ 1.26 \end{bmatrix}$$

Here are some other output data of interest,

$$\beta = 0.931$$

$$\gamma = 1.147$$

$$\xi = 0.91$$

$$C_{\text{odataderived}} = \begin{bmatrix} 2.465 \cdot 10^4 \\ 2.975 \cdot 10^4 \\ 3.132 \cdot 10^4 \\ 3.432 \cdot 10^4 \\ 3.24 \cdot 10^4 \\ 3.106 \cdot 10^4 \end{bmatrix} \qquad C_{\text{shift}} = \begin{bmatrix} 1.731 \cdot 10^4 \\ 2.157 \cdot 10^4 \\ 2.331 \cdot 10^4 \\ 2.564 \cdot 10^4 \\ 2.444 \cdot 10^4 \\ 2.329 \cdot 10^4 \\ 2.466 \cdot 10^4 \\ 2.678 \cdot 10^4 \end{bmatrix}$$

$$J_{\text{constructed}} := (J_{\text{cshift}} \cdot C_{\text{shiftfluence}})$$

$$J_{\text{cshift}} = \begin{bmatrix} 1.095 \cdot 10^3 \\ 1.189 \cdot 10^3 \\ 1.27 \cdot 10^3 \\ 1.278 \cdot 10^3 \\ 1.298 \cdot 10^3 \\ 1.185 \cdot 10^3 \\ 999.486 \\ 773.373 \end{bmatrix} \text{ A/mm}^2$$

$$J_{\text{cconstructed}} = \begin{bmatrix} 1.095 \cdot 10^3 \\ 1.394 \cdot 10^3 \\ 1.58 \cdot 10^3 \\ 1.711 \cdot 10^3 \\ 1.674 \cdot 10^3 \\ 1.473 \cdot 10^3 \\ 1.298 \cdot 10^3 \\ 1.07 \cdot 10^3 \end{bmatrix} \text{ A/mm}^2$$