FEASIBILITY OF A SUPERCONDUCTING FED WITH 50 CM OF MAGNET SHIELDING

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ABSTRACT

The feasibility of the suggestion that the cost of a Fusion Energy Device (FED) could be substantially reduced by operating with a reduced duty factor and only 50 cm of magnet shielding is evaluated here. This report examines the effect of light shielding on insulation life, matrix- and superconductor properties, refrigerator cost and steady-state heat removal. With very careful design, it appears feasible to build a device with only 50 cm of shielding.

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The size and cost of a tokamak reactor is a strong function of the thickness of the magnet shield on the inboard side of the plasma. It has been recently suggested that the thickness of this shield could be reduced from the 1979 ETF reference design value of 80 cm to 50 cm, by changing the mission of a Fusion Energy Device to operation with a low integrated duty factor. This would alleviate problems associated with degradation of organic insulations, radiation-induced resistivity in the superconductor's normal matrix and degradation of superconductor critical properties. Low integrated duty cycle operation would be mixed with occasional high duty cycle operation to test impurity control and thermostructural integrity. The period of high duty cycle operation might be several hours. During this period, light shielding would lead to higher cryogenic refrigeration costs, higher pumping power in forced-flow systems and higher helium quality in pool-boiling systems. The feasibility of a Fusion Energy Device with superconducting toroidal field coils and only 50 cm of shielding will be evaluated below for a proposed set of reference parameters.

Three sets of reference parameters have recently been proposed in a communication from Rutherford to the ETF Physics Committee [RU80]. The set with the largest major radius (5.2 m) is selected for analysis here. This set, called column B, has an average neutron wall loading of .65 MW/m^2, a minor radius of 1.3 m, and a flux density on axis of 4.5 T.

The attenuation of energy and neutron flux density by 50 cm of shielding is taken to be 400. This number is taken from a comparison of the shielding effectiveness of several reference designs, including UWMAK III, NUWMAK, ANL TETF and a trade study by Abdou [AB78]. The three studies which postulated a stainless-steel shield predicted attenuations in the first 50 cm of shielding, ranging from 150 to 400. The NUWMAK design precedes a tungsten shield with 20 cm of blanket, which both attenuates and modifies the neutron spectrum. In the first 50 cm of tungsten shield, on the inside, following the blanket, an attenuation of 5,000 is achieved. It is not expected that 50 cm of tungsten shield would do quite as well, without the presence of a blanket to soften the neutron spectrum, but it would clearly do considerably better than a stainless steel shield. However, the cost of GSA tungsten, according to Metals Week, was $138.50/lb in September, 1980. Therefore, a 50 cm thick by 8 m high inner shield, with an inner radius of 3.2 cm and an outer radius of 3.7 cm would cost $130 M for raw material alone. Given the desire to keep total device cost under $1 B and the well-known problems of fabricability and availability of tungsten, the use of tungsten in shields will probably be judicious. We have assumed the best of the stainless steel attenuations as an achievable reference.

A. Radial Sizing

Before checking the neutron effects on the toroidal magnets, I checked the assumptions on radial sizing, since as little as a 1 or 2 tesla error in maximum field can have a decisive effect on the feasibility of the proposed shield. The sizing method is described below.

A1. Method

The assembly gap between the edge of the shield and the outer wall of the the TF system dewars is taken to equal 5 cm.

The outer dewar wall thickness is determined by the maximum permissible deflection of the wall. The wall is assumed to be rigidly supported at its edges. Rigid supports are optimistic, but the model is also pessimistic, since some intermediate supports may be used.

$$t = \left(\frac{5pl^4}{32E\delta_{max}}\right)^{\frac{1}{3}} \tag{1}$$

where p is taken to equal 210 kPa, E, Young's modulus is 210 GPa, $l = 2\pi R_1/N_{tf}$ and $\delta_m ax$ is the maximum permissible deflection. We also assume that $\delta_m ax = 0.6$ cm.

The thickness of the structure and ground wall insulation is taken to equal

$$\Delta_{SGW} = 0.25m \tag{2}$$

The thickness of the helium-to-nitrogen thermal vacuum barrier is

$$\Delta_{HeN} = .03m \tag{3}$$

The thickness of the nitrogen-cooled radiation shield is

$$\Delta_{Nsh} = .01m \tag{4}$$

The thickness of the nitrogen-to-room temperature thermal vacuum barrier is

$$\Delta_{nrt} = .03m \tag{5}$$

The polygon correction accounts for the fact that the maximum field is at the smallest radius of the beginning of the winding. This correction calculates the difference between the largest and smallest major radius of the outside of the TF winding.

$$\Delta_{tc} = R_c(1 - \cos(\frac{\pi}{Ntf}) \tag{6}$$

A2. Results

The minimum radius of the shield is 3.2 m. The polygon correction for a ten-sided trapezoid is 14.9 cm. The radial build of assembly tolerances, case thickness and thermal insulation adds 25 cm, to give a radius of the toroidal field at the maximum field conductor of 2.8 m. This implies a maximum field at the conductor of 8.54 T, scaled as the inverse major radius to the field on axis. This method incorrectly and optimistically ignores high field ripple, which is not insignificant in a ten trapezoid system. It is possible with some difficulty to recover a maximum field of 8.5 T by using a circle-fitting, stepped insulation, as in STARFIRE, to reduce both the geometric polygon correction and the high-field ripple. However, the rectangular coil packages adopted in the ETF 1979 design could be expected to have maximum fields at the conductor of well over 10 T, for this set of reference parameters.

We will adopt 8.5 T as an achievable maximum field. This is exactly equal to the maximum field suggested in Rutherford's communication. (Either great engineers think alike, or the above algorithm used in the ORNL tokamak systems code remained the basis of the reference design size and was then copied as a typical radial build by Rutherford.)

B. Neutron Damage

The proposed concept suggests that the integrated availability of the FED should be a few per cent. We adopt 2 % as a reference value. It can be argued that integrated neutron damage to insulation, copper and superconductor is not a fundamental constraint, since the integrated duty factor can be reduced arbitrarily. However, we think that an order of magnitude reduction in availability, below 2 %, would severely compromise the worth of this machine as an experimental device. A 2 % integrated availability implies 6,000 long-pulse ($\approx 100s$) shots a year, which is comparable to the number of shots on present machines and is a worthwhile goal.

An MIT computer code, entitled NEUTRO, was used to calculate neutron damage to magnet materials, as well as neutron and gamma heating of the magnet. The algorithm used in this code is described below.

B1. Method

NEUTRO calculates neutron radiation damage parameters, relevant to magnet design. Instantaneous nuclear heating, hot-spot nuclear heating, change in superconductor critical properties, change in superconductor normal matrix resistivity and damage thresholds of organic insulations are calculated.

Inputs to NEUTRO include the plasma wall loading in MW/m^2, total effective plant duty factor (duty cycle x availability), shield thickness and material, insulation dose limits, superconductor and matrix materials and the unirradiated operating temperature at the hot spot location (assumed to be the high-field side on the equatorial plane).

The user is permitted to select one of three shield materials: stainless-steel pellets in borated water, tungsten or an optimized mix of tungsten and B_4C. The rules of thumb used here are:

Energy attenuation for s-s in borated H_2O = 1 decade/16 cm

Energy attenuation for tungsten = 1 decade/14 cm

Energy attenuation for optimized shield = 1 decade/13 cm

If a blanket is included, we assume, arbitrarily, that

Energy attenuation, blanket = 1 decade/25 cm

When blanket and shield thicknesses are chosen, and total fusion power and wall-loading are specified, instantaneous heating is calculated as follows:

$$a = attenuation = 10^{\frac{-t_{shield}}{\delta_{shield}}} * 10^{\frac{-t_{blanket}}{.25m}} \tag{1}$$

where t_{shield} is the shield thickness in meters, δ_{shield} is the thickness for a decade of attenuation and $t_{blanket}$ is the blanket thickness. The power deposited in the TF coils is

$$P_{TF} = a\, P_{fn} \tag{2}$$

where P_{fn} is the total plasma neutron power in MW.

The hot-spot power equals

$$P_{HS} = 100\, a\, P_w \tag{3}$$

where P_w is the wall-loading and the factor of 100 converts conductor surface heating to the more familiar units of W/cm^2.

Energy flux is converted to neutron/cm^2, rad (erg/g) and dpa (displacements per atom) by the following formulae. The multiplicative constants also include unit conversion factors.

$$Nuclear\ heating, Q_{NH} = \frac{P_{HS}}{a_s c}(W/cc) \tag{4}$$

$$Neutron\ flux, \Phi_n = 3.5\ \ 10^{20}\, Q_{NH}\, df \tag{5}$$

$$RAD_{epoxy} = .43\ \ 10^{-8}\, \Phi_n\ (rad) \tag{6}$$

$$DPA_{Al} = .67\ \ 10^{-13}\, Rad_{epoxy}\ (dpa) \tag{7}$$

where a_{sc}, a characteristic attenuation length, is taken to equal 10 cm in a superconducting magnet. df is the plant duty factor (duty cycle x availability) over a year. These ratioes are scaled from the reported values in the NUWMAK design [CH78].

Magnet insulation dose limits, RAD_{max}, are input, but should be limited to less than 10^9 rad, as justified in a previous memo [SC79].

$$Insulation\ safety\ factor,\ SF_{ins} = \frac{RAD_{max}}{Rad_{epoxy}\, Life} \tag{8}$$

where Life is the reactor lifetime in years.

The change in the resistivity of the matrix is expressed as [TU78]

$$\Delta\rho_{RAD} = 3.\,10^{-9}[1 - \exp(-563\, DPA_{Cu})]\ (\Omega - cm) \tag{9}$$

where DPA_{Cu} is set equal to DPA_{Al}, because I don't know any better.

The change in the resistivity of copper, due to lattice displacements can also be expressed as [SO78]

$$\Delta\rho_R(\Phi_n)_{Cu} = 4.4\,10^{-9}[1 - e^{-\Phi_n/7.5\,10^{18}}](\Omega - m) \tag{10}$$

For aluminum, it is

$$\Delta\rho_R(\Phi_n)_{Al} = 9.4\,10^{-9}[1 - e^{-\Phi_n/4.5\,10^{18}}](\Omega - m) \tag{11}$$

The change in superconductor parameters is calculated as follows [~~So~~-78]

For NbTi,

$$\frac{J_{c,NbTi}}{J_{c0}} = \exp^{-3.5\,10^{-20}\Phi_n} \tag{12}$$

up to a fluence of 5 $10^{18}\, n - cm^2$.

For Nb_3Sn, up to $10^{18}\, n - cm^2$, T_c is a constant.

From $10^{18} < \Phi_n < 10^{19}\, n - cm^2$,

$$\Delta T_c = -4x10^{19}K - cm^2x(\Phi_n - 10^{18} \tag{13}$$

and

$$\Delta B_{c2}(T = 0) = \Delta T_c\,(\frac{T}{K}) \tag{14}$$

The NEUTRO program can also be used for matrix material selection and includes algorithms for matrix residual resistivity and magnetoresistivity.

For copper, the following expression is used for residual resistivity at 4 K.

$$\rho_{res} = 1.7\,10^{-10}(\Omega - m) \tag{15}$$

Aluminum has a much wider range of residual resistivity, depending on conductor purity and the residual resistivity ratio, ρ_{273K}/ρ_{4K}, is input:

$$\rho_{res} = \frac{1.7\,10^{-8}}{RRR}(\Omega - m) \tag{16}$$

The following equation is used for the magnetoresistivity of copper [TU78]

$$\rho_{M,Cu} = 4.55\ 10^{-11}\, B_{max}\, (\Omega - m/T) \tag{17}$$

Another reference uses 3.5 10^{-11}.

The following equation is used to calculate the magnetoresistivity of aluminum [CO74].

$$H^{*} = 10^{-2}\, B_{t}\, RRR\ (MOe) \tag{18}$$

$$\frac{\rho_m}{\rho_{res}} = H^{*2}\,(1 + .00177H^{*})\,(1.8 + 1.6H^{*} + 0.53H^{*2}) \tag{19}$$

This equation appears to be controversial and should be reviewed.

The total matrix resistivity is taken to equal the sum of residual resistivity, magnetoresistivity and radiation-induced resistivity due to lattice damage. Radiation-induced resistivity due to transmutations has not been included.

B2. Results

A significant amount of neutron and gamma shielding can be accomplished by the warm TF coil case, which was estimated to require a thickness of 6.6 cm. If the e-folding distance of the case is 7 cm, two-thirds of the energy dissipated in the magnet will be dissipated in the case and one-third in the winding. We assume that the energy dissipated in either steel or copper is 1.3 times higher than the neutron energy flux into the material, due to transmutations. The energy flux into the case is .65 Mw/m^2, divided by the attenuation of 400, or 1.6 kW/m^2. The power dissipated in the inside leg of the magnet is approximately 1.6 kW/m^2 x 1.3 x 8 m x 2π 2.8 m or 330 kW, of which 205 kW is dissipated in the case and 125 kW is dissipated in the winding and cold structure. The surface heating at the magnet hot-spot on the machine equator is 10 mW/cc, which should not drive the magnet normal.

The radiation absorbed by the organic insulation is predicted to equal .2 x 10^9 rads per year. This would lead to 2 x10^9 rads being absorbed in 10 years. The main structural problem is believed to be cyclic compressive stresses on the insulation supporting centering loads and out-of-plane loads. At an irradiation of 2 x 10^9 rads, Coltman [CO79] measured a drop in compressive yield to 15 ksi (from an unirradiated yield of 130 ksi). This could conceivably be designed around with the distributed structure of a forced-flow design, such as that of the Westinghouse LCP coil. However, peak compressive stresses in the FED, using pool-boiling magnets, are believed to be in the neighborhood of 50 ksi for both centering and out-of-plane stresses. The centering stresses are helped somewhat by the fact that the force builds up toward the bucking post, moving away from the more highly irradiated regions of the magnet. However, this effect doesn't aid the take-up of out-of-plane stresses. A promising solution is the use of a material such as TGPAP with DDM resin, which is known to have a threshold to damage vs. γ-irradiation which is ten times higher than that of DGEBA, the resin for G-10 [EV70]. This resin has been used without failure for about a decade in British accelerators and has room temperature mechanical and electrical properties which are as good as those of G-10. It has no known disadvantages, but its properties under irradiation at 5 K are not yet known.

The radiation-induced resistivity in the copper matrix was calculated, assuming that there would be 4 room temperature anneals in the history of a magnet, including final reactor shutdown, and that each anneal removed 85 % of all damage due to lattice displacements. The increase in the 4 K resistivity of copper due to irradiation is predicted to equal .1 x $10^{-9}\ \Omega - m$. The residual resistivity of the copper is .167 x $10^{-9}\ \Omega - m$, while the magnetoresistivity of copper at 8.5 T is .33 x $10^{-9}\ \Omega - m$. Therefore, the effect of radiation is to raise the total matrix resistivity by 20 %, which is non-negligible, but not unacceptable. The critical current of NbTi after ten years of operation is .993 of its unirradiated value, which is a negligible effect.

C. Refrigeration requirements

The only significant refrigeration losses on the inner legs of the toroidal field systems will come from nuclear radiation and ac losses. Previous studies have predicted that the cost of a cryogenic refrigeration system for expected ac losses and for well-shielded toroidal field coil systems should not be dominant. The cost of a total refrigeration system is calculated here, using the method of Stobridge [ST74].

C1. Method

REFCOS uses unit costs of cryogenic refrigeration equipment in order to estimate the installed equipment cost of a large cryogenic refrigeration system. The requirements of separate refrigerated systems, such as neutral beam cryopanels or toroidal field coils are accumulated in a separate subroutine in order to define an overall refrigeration system requirement. Most unit costs are taken from NBS Technical Note 655 by Stobridge[ST74]

Conductor lead losses are treated differently from neutron absorption, conductor ac losses and thermal radiation losses at liquid helium temperature. Lead losses are considered liquefier losses, while other losses are considered to be refrigerator losses.

Losses at liquid helium and liquid nitrogen temperatures are sized and costed separately. The ideal efficiencies are :

$$\eta, Carnot_{refrigerator,4K} = \frac{1}{70} \tag{1}$$

$$\eta, Carnot_{refrigerator,77K} = \frac{1}{2.88} \tag{2}$$

$$\eta, Carnot_{liquefier,4K} = \frac{1}{326} \tag{3}$$

The realizable per cent of the ideal Carnot efficiency, as a function of refrigerator power, is a correlation developed by Schultz to fit the published curves of Stobridge [ST74].

$$\% \, \eta, Carnot = \frac{R^{.4}}{\frac{R^{.36}}{20} + .4} \tag{4}$$

where R is the refrigeration or liquefier requirement at the working temperature in (W).

The predicted refrigerator compressor shaft power for the helium refrigerator, nitrogen refrigerator, helium liquefier and combined helium liquefier/refrigerator, respectively, are

$$P_{shaft} = \frac{100\,R}{\eta, Carnot\ \eta, \%\ \eta, Carnot} \tag{5}$$

The electrical input power necessary to run the helium and nitrogen refrigeration systems is

$$P_{elec} = 1.4\,P_{shaft} \tag{6}$$

The costs of the helium and nitrogen refrigeration systems are taken to equal

$$Cost_{helium\,refrig} = 11\,(10^{-3}\,P_{shaft})^{.7}\ (k\$) \tag{7}$$

$$Cost_{nitrogen\,refrig} = 11\,(10^{-3}\,P_{shaft})^{.7}\ (k\$) \tag{8}$$

In order to size and cost the helium and nitrogen storage dewars, a one day inventory is assumed for full-time machine usage. This models the mission, which includes infrequent bursts of high-duty factor operation. The rate of helium and nitrogen usage in (l/hr) are

$$Rate_{He} = 1.38\,(R_{refrig} + R_{liquef})\ (l/hr) \tag{9}$$

$$Rate_{N_2} = .022\,R_{refrig}\ (l/hr) \tag{10}$$

The storage dewar capacities are simply

$$C_{Hestorage\,dewar} = Rate_{He}\,\frac{24 hours}{day}\,1 day \tag{11}$$

$$C_{lN_2 storage\,dewar} = Rate_{N_2}\,\frac{24 hours}{day}\,1 day \tag{12}$$

The piping run of the helium and nitrogen transfer lines is set arbitrarily to 1000 m. The unit costs of the dewars and piping are taken from Schulte [SC79], a standard costing reference in preparation.

$$Cost_{piping} = 1000\ m\ \frac{\$120}{m} \tag{13}$$

C2. Results

The ac losses can not be calculated very exactly without a reference design and scenario. However, a crude estimate can be made as follows. Assume the average case thickness in the nose region is 10 cm, the average dimension of the nose cross-section is 2.0 m and the resistivity of stainless steel at cryogenic temperature is 50 x $10^{-8}\,\Omega - m$. For Design B, $\frac{\beta_p}{\beta_t} = 100$. Since the toroidal field on axis is 4.5 T, the vertical field on axis will be about .45 T. The energy containment time is 0.8 s and the start-up ramp may be about 2.5 s. The characteristic diffusion time for vertical field is

$$\tau_M = \frac{\mu_o \Delta y\, y_1^2}{2\rho_{ss}\,(2y_1)}$$

where y_1 is 2.0 m and Δy is 0.7 m. τ_M equals .084 s, so the loss equation for full penetration can be used with a 2.5 s ramp.

$$\frac{P}{L} = \frac{\dot{B}^2}{12\rho}(b^4 - a^4)$$

where b is 2.0 m and a is 1.86 m. This gives a loss rate during start-up of 16 kW/m. If the perimeter of a TF coil is 25 m, then the loss rate per coil is about 400 kW, 4 MW for the 10 coil system. Start-up and shutdown over a 100 s cycle give an effective duty factor of about 5 % for an integrated loss of 20 kW per coil or 200 kW for the system. This loss is comparable to the radiation loss.

For the distributed structure, forced-flow system, there is no structural case. AC losses in the cold steel structure will be negligible. Transverse coupling losses in the winding may be significant and were calculated to equal on the order of tens of kW for the ETF case. The existence of a case reduces eddy currents in the superconductor itself, but may increase total ac losses by a factor of 2 or 3.

In order to evaluate the effect of light shielding, we will consider the effect of radiation losses and ac losses as though they were handled by separate refrigerators. Since the refrigeration loads involved are so much larger than anything that has ever been handled by a liquid helium refrigeration system, it seems not unfair to ignore further economies of scale by implicitly assuming multiple units.

For a total system current (sum of actual currents in each magnet) and a radiation load of 125 kW, Stobridge's algorithm predicts an input electric power of 45 MW, a helium refrigerator cost of \$15.4 M, a nitrogen refrigerator cost of \$1.6 M, a helium storage cost for one day of \$12.4 M, a nitrogen storage cost of \$.2 M and piping cost of \$.2 M. The total refrigerator cost is \$29.8 M, which is significant but not prohibitive. Further, the size of the refrigerator needed for radiation losses is comparable to that needed for ac losses.

A recent vue graph by T. Ryan of LCP used the formula $1.2 \, x \, 10^4 \, (P)^{0.7}$ for the cost of a helium refrigerator, where P is the compressor electrical power in kW. This predicts a cost of \$31 M, based on 1978 LCP costs, instead of the \$15.4 M predicted by Stobridge. If all of Stobridge's costs should be doubled, the additional cost of a refrigeration system due to light shielding would be \$60 M dollars. This is beginning to be limiting.

D. Steady-state heat removal - Pool Boiling

In the case of a pool-boiling system, upward bubble transport must remove losses from neutron and gamma irradiation and ac losses. The worst spot appears to be the high-field layer of conductor, which must endure the most intense nuclear irradation, as well as the ac losses from the high-field case wall. We examine the 1979 ETF reference topology and find it to be inadequate for heat removal. A different topology is recommended.

D1. Method

The heat removal capability of a pool-boiling structure, cooled by the rise of helium vapor bubbles is calculated using the method of of Sydoriak and Roberts as a universal approach to boiling heat-removal design for structures with short, narrow channels [SY57]. The approach was later calibrated by Sydoriak and Roberts [SY66] for liquid helium. In this code, we explicitly assume uniform heating along the length of the channel.

The method of Sydoriak and Roberts is to integrate the steady-state equation

$$\rho\, v dv + dp + \rho\, g dz = 0 \tag{1}$$

from the bottom to the top of the channel, assuming uniform heating. ρ is the coolant density, v its velocity, p the pressure, z the height of the channel and g the acceleration of gravity. The pressure drop from the inlet to the outlet is expressed in terms of the equivalent head of liquid

$$Z_e = (p_1 - p_2)/\rho_L g \tag{2}$$

where the subscript 1 indicates the inlet and 2 the outlet. f is defined as the mass fraction of vapor and $\beta = \rho_L/\rho_V$. Integrating, we get

$$Q' = AL\rho_L \left[\frac{f_2(\beta_2 - 1)^{-1} Z_e - Z \ln[1 + f_2(\beta_2 - 1)]}{f_2(\beta_{av} - 1)} \right]^{\frac{1}{2}} \tag{3}$$

where β_{av} is a mean value of β.

Since $Z = (p_{1,0} - p_{2,0})/\rho_L g$, the equation for the calculate heat input for a natural convection immersion evaporator under the above assumptions is

$$Q' = AL\rho_L \left[\frac{f_2^2 g}{f_2(\beta_2 - 1) + \frac{1}{2}} (Z - Z_m) \right]^{\frac{1}{2}} \tag{4}$$

where Z_m, the head of liquid equivalent to the total head of vapor-liquid mixture, is defined as $Z_m = (1/\rho_L) \int_0^Z \rho\, dz$.

D2. Results

The reference conductor of the 1979 ETF design and the GE 12 tesla coil is shown in Figure 1. It has a helium channel area of 2.2 cm^2. If the case sidewalls are 10 cm thick, the number of these conductors that can fit on the high field side are 240 for the ten coil system. Therefore, the total helium cross-sectional area in the high field first row is .052 m^2. The equivalent stack height is taken to equal 15 m. If the helium quality at the top of a coil stack is constrained to be less than .02, corresponding to a bubble volume fraction of .27, and the bottom pressure is 1.2 atmospheres, 23 kW can be removed steady-state from this stack. This model assumes frictionless flow, which is close to being true, and no significant holdup of bubbles percolating through conductor bundles and steel archway holes. Going around corners, vertical velocity is assumed to be reduced by the sine of the angle from horizontal travel. If the bottom pressure is changed to 1.5 atmospheres, the heat removal capability is 73 kW. Since the above assumptions must all be on the optimistic side, this predicted value of steady-state heat removal is inadequate.

If diagonal slits are machined in the interpancake insulating sheets and the front channel is increased in area, the heat removal capability of the topology can be significantly improved, although the effectiveness of bubble transfer from the winding channels to the slits is uncertain.The proposed topology is shown in Figure 2. It has also been proposed that a shunt helium channel be placed outboard of the case. This would improve transport around the top of the coil, but would compromise case structural integrity and groundwall electrical integrity much more severely than simply enlarging the first helium channel. However, a shunt helium channel on the outboard side of the case should still be included, without diagonal passages to it, in order to remove somewhat more than half of the radiation and ac losses in the case wall. The heat load on the first channel is then modeled as 40 % of case losses or .4 x (225 kW, radiation + 200 kW, ac losses) or 170 kW. If the depth of the first helium channel is increased by 2 cm, the area of each channel is increased to 12.6 cm. The heat removal capability of the first channel is then calculated to be 136 kW with an inlet pressure of 1.2 atmospheres, and 422 kW with an inlet pressure of 1.5 atmospheres.

The ability of slots in the interpancake sheets to transport bubbles to the high-field first channel is modeled by assuming 45^o angle risers, .5 cm deep by 1.0 cm wide, every 10 cm. Each 10 m high leg would have 100 slots, for an effective cross-sectional area of 50 cm^2. Any number of strategies could be employed, ranging from transporting bubbles from conductor losses deep within the coil to a large channel on the high field and/or low field side to transporting bubbles from the hot spots near the coil cases to the cooler center of the magnet, depending on the pattern in which slots are machined. However, in all cases, the vertical distance traveled by a bubble will be about one-half the coil thickness divided by the sine of 45^o or .7 m and the velocity will be that of a vertical column divided by the sine of 45^o. The heat removal capability at an additional quality of .01 is calculated to equal 500 kW at 1.2 atmospheres input pressure.

E. Steady-state heat removal forced-flow

Forced-flow cooled conductors must physically transport warmed helium from the inlet to the outlet of a magnet. Because of their assumed bolted plate construction, there is no cold temperature case (there is a warm temperature case, 7 cm thick, which attenuates neutron and gamma irradiation, as before) and, therefore, there are no eddy currents to remove in the first channel, other than those generated in the conductor. Since the first channel must wind around the coil perimeter a few times, widening the first channel helium area is not an option. Radiation losses in the plate tooth used as the first layer winding bobbin do have to be removed by the first channel. For example, we take the Westinghouse LCP conductor, wound four in hand and with four parallel hydraulic channels in each pancake.

E1. Method

A computer code, entitled FORFLO HERMOV, was written to select a a flow velocity for a channel of fixed dimensions and a given heat input, cooled by forced-flow supercritical helium. The desired output temperature and input temperature of the helium and the the desired output pressure of the coolant are input to the program. The program then picks a deliberately low first guess of the flow velocity needed to remove the specified external heat input. It then iterates the flow velocity in small increments, until the specified heat is removed.

The basic equation being solved by the program is:

$$Q' = M'\Delta\left[(u+\frac{p}{\rho})+\frac{v^2}{2g}+gz\right]_{Out-In} \quad (W) \tag{1}$$

where Q' is the heat removal rate, u is the specific internal energy, ρ is the mass density, p the pressure, z the height and g the acceleration of gravity. All units are MKS.

Given the desired output temperature and pressure, the program looks up the mass density in the NBS 631 helium tables [Mc61]. It then inputs the temperature and density to a function definition, called Functn enthal, supposedly based on a set of correlations by McCarty [Mc72]. I have examined both of the documents referenced by the code documentation and don't see where the correlations really came from. These correlations do not give the same results as NBS 631, especially when the temperature is less than 5.5 K. However, the change in enthalpy from one state to another appears to be correct to within about 10 %, for the several cases at which I have looked. This is smaller than the expected uncertainty in calculating the friction factor, so I think it is still acceptable to use these correlations for the purpose of design.

The specific heat at the output is used to guess the flow rate necessary to remove the external heat input.

The first guess at the mass flow rate is

$$M' = K_1 \frac{Q'}{C_p(T_{out} - T_{in})} \qquad (2)$$

where K_1 is a number less than one, to ensure that the first guess will always be optimistic, but not so small that too many iterations will be needed to find a solution.

On each iteration of the guessed mass flow rate, the following correlation is used to obtain the friction factor; and ,therefore, the pressure drop per unit length, through the conductor.

$$f = \frac{16.0 + .6245\, Re^{.842}}{Re} \qquad (3)$$

The pressure drop per unit length is then

$$\frac{\Delta p}{L} = \frac{2G^2 f}{\rho_{out} g_c D_h} \qquad (4)$$

where G = M'/A_{He} is the mass flow rate per unit area, ρ_{out} is the outlet mass density, g_c is the acceleration of gravity (9.8 m/sec^2) and D_h is the hydraulic diameter of the channel. The pressure drop per unit length is then multiplied by the length in order to get the input pressure. This is somewhat approximate and should be revised to divide the channel into more sections, since ρ varies considerably over the length of a typical channel. Once the input pressure is known, the input enthalpy and velocity are known and the heat removal rate can be calculated. If this rate is less than the external heat input rate, the flow velocity is increased. If the heat removal rate is decreased, rather than increased, by increasing the mass flow rate, an error message is output and the user is informed of the maximum amount of heat which can be removed from the channel.

E2. Results

If the Westinghouse LCP conductor were used, with 4 hydraulic channels, the helium cross-sectional area of each conductor would be 1.44 cm^2. The reference design requires 130 MAT in the TF system. If the conductors are run at 15 kA, 8,670 turns will be required in the system and 867 turns in a coil. There is now no need for thick coil cases, but toroidal build is taken up by the thickness of the plates supporting the conductors. If the plate teeth are two sided, as in the HFCTR conceptual design and 1 cm thick, they will take up 25% of the toroidal build; if they are one-sided as in LCP, they will take up 33% of the toroidal build. We will assume that 33% of the available toroidal space is taken up by plates, insulation and side structural pieces. This allows

586 conductors in the high field row. As many as 20 turns should then be required in the full radius pancakes. Therefore, the longer conductors will have 20 turns/ 4 parallel paths = 5 turns for a total length of about 125 m.

The first row of conductors has to remove the heat generated in say 2 cm of winding bobbin tooth, 2 cm of conductor and 4 cm of plate. If the e-folding distance of the composite structure is 7 cm, then the heat which must be removed is about 71 kW. If the desired inlet and outlet temperatures are 4.5 K and 5.5 K, respectively, and the desired outlet pressure is 2.3 atmospheres, the required inlet pressure is 3 atmospheres, the Reynolds number is 10,000 and the pump power per channel is 5.7 W. For a four-in-hand winding, there will be 2,170 channels in the ten coil system. Therefore, the required pumping power would be 12.4 kW. This is a modest additional cost. If a larger (e.g. 50 kA) conductor were developed for the program, such as the 50 kA pool-boiled cable in the Los Alamos/Westinghouse 20 MJ pulsed coil, the pump power should be that much lower.

Conclusions

• A qualified endorsement should be given to the idea of operating the FED at a low integrated duty factor, with only 50 cm of inboard toroidal field magnet shielding. The benefits of a 30 cm reduction in shield thickness on overall machine size are well-known and of first-order importance. With extremely careful design in the areas of insulation life, increased matrix resistivity, refrigerator cost and steady-state heat removal, it appears possible that the penalties for light shielding might have only a second order effect on overall system economics and that steady-state heat removal is not infeasible.

• The most limiting factor for a lightly shielded design is probably the additional refrigeration cost, followed by steady-state heat removal for pool-boiling magnets.

• As in every other respect - structural, electrical and thermal - forced-flow, distributed structure magnets appear to be superior to pool-boiling magnets for the case of surviving light shielding. They do not have high cold case losses, nor do they concentrate those losses on the most vulnerable high-field row of conductors. They do not build up centering or out-of-plane stresses, and are thus more likely to survive some degradation of compressive yield in organic insulation. According to our models, the pump power required to remove the additional heat in the magnet is less than the additional ac losses in the lumped structure magnets, due to the presence of a cold structural case. The modeling of heat transfer from inlet to outlet in the forced flow case is better established than the assumed model for bubble transfer, where the effect on transport of complex percolation paths had to be ignored.

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