UCRL--89699

DE83 017591

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20-TESLA LARGE-BORE SUPERCONDUCTING MAGNETS

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This paper was prepared for submittal to the
Eighth International Conference on Magnet Technology
Grenoble, France
September 5-9, 1983

September 30, 1983

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ANALYTICAL STUDIES OF ADVANCED HIGH-FIELD DESIGNS:

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Resumé - Plusieurs nouvelles techniques de pointe ont été combinées dans le cadre d'une étude de principe démontrant la possibilité de produire des champs magnétiques de très haute intensité sur un diamètre important à l'aide d'électro-aimants à bobinages supraconducteurs. Plusieurs prototypes ont été étudiés qui atteignent des intensités maximum d'environ 20 T au sein d'un cercle de diamètre utile de 2 mètres. Les expressions analytiques constituant les éléments essentiels des deux programmes "CONDUCTOR" et "ADVMAGNET" utilisés pour la conception de ces systèmes magnétiques avancés sont également discutés. Ces aimants ainsi que les techniques de mise au point constitueront une contribution de première importance à l'effort national dans le domaine de la fusion thermonucléaire par machines à miroirs (magnétiques) ainsi que dans le domaine nouveau des "scanners corps entiers" par résonance magnétique nucléaire. (RMN)

Abstract - Several emerging technologies have been combined in a conceptual design study demonstrating the feasibility of producing ultrahigh magnetic fields from large-bore superconducting solenoid magnets. Several designs have been produced that approach peak fields of 20-T in 2.0-m diameter inner bores. The analytical expressions comprising the main features of CONDUCTOR and ADVMAGNET, the two computer programs used in the design of these advanced magnets, are also discussed. These magnets and design techniques will make a paramount contribution to the national mirror-fusion endeavor and to the newly emerging field of nuclear magnetic resonance (NMR) whole-body scanners.

This is the third of three reports illustrating the steps and strategies used in designing ultrahigh-field superconducting solenoid magnets /1,2/ for producing fields of up to 20 T in 2-m diameter inner bores. Six technological developments since 1981 have contributed toward making these designs possible: 1) the use of pressurized superfluid He-II as the magnet coolant; 2) the development of Nb₃Sn-Ti, a new superconducting compound, with higher current densities than Nb₃Sn at higher magnetic fields ($B > 15$ T); 3) data indicating an ~200% enhancement of J_c when operating at 1.8 K instead of 4.2 K; 4) the use of a stainless-steel strip (Nitronic-40) wound in parallel with the conductor to react against the enormous electromagnetic hoop stresses; 5) the use of current density grading in the coil pack, with three different superconducting materials (Nb₃Sn-Ti, Nb₃Sn, and NbTi), to satisfy both the field variations and material current density limitations; 6) the development of CONDUCTOR and ADVMAGNET, two new solenoid-magnet design programs, for generating coil-pack designs that have optimal current density, are completely cryostatically stable, and incorporate an adequate

Work was performed under the auspices of the U.S. Department of Energy by the Lawrence Livermore National Laboratory under contract number W-7405-ENG-48.

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stainless-steel substructure for limiting the strain placed on the strain-sensitive superconductors. These programs also minimize the volume of the coil packs producing the required total on-axis field. This paper discusses the equations that incorporate the first five of these developments into the CONDUCTOR and ADVMAGNET programs.

I - CRYOSTATIC STABILITY OF THE CONDUCTOR

The first task is to determine a conductor size for the coil that will include an adequate amount of copper stabilizer for cryostatic stability when operating at a given wetted-perimeter heat flux. Figure 1 is a cross-sectional diagram of the conductor used in a coil winding pack. A superconductor insert, with an area of

Fig. 1. Cross section of a winding-pack element.

$W_c H_c$, is shown inside a copper stabilizer that gives the conductor an overall area of $W_s H_s$. The conductor's wetted perimeter heat flux is

$$Q_w = \frac{I_o^2 \rho}{A_{cu} P_w}, \quad (1)$$

in which I_o is the operating current, A_{cu} is the copper's cross-sectional area, ρ is the resistivity of copper, and P_w is the conductor's wetted perimeter. From an examination of Fig. 1 it can be seen that the area of the copper is

$$A_{cu} = W_s H_s - W_c H_c = W_s H_s - \frac{I_o S_f}{J_c}, \quad (2)$$

where S_f is the operating safety factor, i.e. the ratio of the superconductor's critical current to the operating current.

The conductor's wetted perimeter is

$$P_w = (H_s + 2W_s) f_w, \quad (3)$$

in which f_w is the fraction of the conductor's surface wetted by the coolant.

If Equations 2 and 3 are inserted into Equation 1, the resulting expression is quadratic in the total conductor area, A_c . Solving for $A_c = W_s H_s$ yields:

$$A_c = -\frac{1}{4} \left(H_s^2 - \frac{2S_f I_o}{J_c} \right) + \frac{1}{4} \sqrt{\left(H_s^2 - \frac{2S_f I_o}{J_c} \right)^2 + 8H_s \left(\frac{S_f I_o H_s}{J_c} + \frac{I_o^2 \rho}{Q_w f_w} \right)}. \quad (4)$$

Equation 4 yields the conductor width needed for cryostatic stability, W_s , when values are inserted for the maximum permissible wetted heat flux, Q_w ($\sim 0.2 \text{ W/cm}^2$ for LHe and 1.0 W/cm^2 for superfluid He-II), the desired conductor height, H_s , and an operating current, I_0 .

II - STAINLESS STEEL CONDUCTOR REINFORCEMENT

A few comments should be made about the parallel winding of stainless steel with the conductor. The force on the conductor equals the cross product of the current density and the surrounding field value. The designer would like to limit stress on the conductor to values that will not degrade its J_c properties: usually corresponding to about 0.35% strain. On the other hand, to limit the overall volume of the coil, it is advantageous to drive the coil pack current density as high as possible. This requires selecting a material with a high elastic modulus even at high strains, i.e. the material must have a yield strain higher than 0.35%. Work-hardened copper falls short of this criterion, since its Young's modulus of 17.6×10^6 psi rapidly falls to 3×10^6 psi for strains in excess of 0.2%. A stainless steel such as 304 LN is much better; however, its yield stress is approximately 100 kpsi. Thus, if the coil operates at two-thirds of the yield stress, we can only load the 304 LN to 66 kpsi, which corresponds to a strain of 0.24%. For these reasons we have selected 21-6-9 (Nitronic-40) stainless steel, with a yield stress of 196 kpsi at 4.2 K, for the conductor reinforcing material. The amount (or thickness) needed for the stainless steel strip can be determined by using a modification of the formula for determining the load sharing between two parallel elements jointly carrying an applied load /3/. For the conductor (c) and stainless steel (ss) elements,

$$L_c = L_T \left(\frac{A_c E_c}{A_c E_c + A_{ss} E_{ss}} \right) \text{ and } L_{ss} = L_T \left(\frac{A_{ss} E_{ss}}{A_c E_c + A_{ss} E_{ss}} \right), \quad (5)$$

in which L_T is the total applied load, L_c and L_{ss} are the loads carried by the conductor and stainless members, respectively, A_i and E_i ($i = c$ and ss) are the appropriate Young's moduli and element cross-sectional areas for the conductor and stainless steel, respectively. Equation 5 can be restated by defining the total load as:

$$L_T = J_c B R A_c, \quad (6)$$

in terms of the force due to the current density, J_c , and the field, B_m , at the radius of the particular coil turn, R . The resulting expression becomes

$$\frac{A_{ss}}{A_c} = \left(\frac{E_c}{E_{ss}} \right) \left(\frac{J_c B R}{\epsilon_c E_c} - 1 \right), \quad (7)$$

which relates the required area of stainless reinforcing material to that of the conductor. The strain in the conductor, ϵ_c is chosen so that

$$\epsilon_c \leq \frac{2/3 \sigma_y}{E_{ss}} \leq \epsilon_{\max}, \quad (8)$$

where $\epsilon_{\max} = 0.35\%$ strain, and σ_y is the yield stress of the strengthening band wound in parallel with the conductor.

The current density in the coil pack, J_{pack} , can be computed from the results of the previous two sections. The total cross-sectional area, A_T , of the winding element illustrated in Fig. 1 is:

$$A_T = A_c + A_{ss} + W_t H_s + W_l W_t + W_l W_s + W_l A_{ss} / H_s, \quad (9)$$

in which W_t and W_l are the thicknesses of the turn-to-turn and layer-to-layer insulation, and J_{pack} is simply I_0/A_T .

III - MINIMUM VOLUME COIL CONCEPTS

The minimum volume approach to magnet design arises from the need to accomplish two goals. The first is to design and build these coils as inexpensively as possible. Since coil costs are known to scale according to volume, efforts must obviously be made to obtain the smallest possible coil-pack volume for generating the required on-axis field. The second task is to obtain even higher fields, to keep pace with the changing developments in plasma physics and plasma stability.

Designing minimum volume coils is straightforward, since the design program uses the techniques from the calculus of variations. The format of the design is a Lagrange multiplier problem, with a system of equations relating the coil volume to the pack length and its radial thickness, and to various boundary conditions. These boundary conditions include the axial field profile and maximum-field limits for the superconducting windings, which act as constraints on the maximum and minimum calculations. The system of equations is then solved for those values of radial thickness and coil length that satisfy the field boundary conditions using the smallest possible total coil volume.

An example of these calculations is illustrated in Fig. 2, which depicts a cross-sectional view of three nested concentric subcoils, each one using a different superconductor (i.e. NbTi, Nb₃Sn, and Nb₃Sn:Ti). The designer's

Fig. 2. A set of nested subcoils, separated by the distances δ_1 and δ_2 .

challenge is to produce the desired central axial field from this coil set, while limiting the peak field values on the inner turns of each superconductor to a certain maximum permissible value (e.g., to 8, 15, and 20 T, respectively). The central axial field, H_c , can be expressed as:

$$H_c = j_1 \lambda_1 a_1 F(\alpha_1, \beta_1) + j_2 \lambda_2 a_2 F(\alpha_2, \beta_2) + j_3 \lambda_3 a_3 F(\alpha_3, \beta_3) . \quad (10)$$

The products, $j_i \lambda_i$, are the global current densities for the coil packs; and

$$F(\alpha_i, \beta_i) = \frac{2\pi}{5} \beta \log \left(\frac{\alpha_i + \sqrt{\alpha_i^2 + \beta_i^2}}{1 + \sqrt{1 + \beta_i^2}} \right), \quad i=1,2,3 \quad (11)$$

where the α 's and β 's are dimensionless ratios related to the inner and outer radii and coil length (a , r_o , and l , respectively) of each subcoil.

$$\alpha_i = r_{oi}/a_i \quad \text{and} \quad \beta_i = l_i/2a_i \quad (12)$$

The peak field values at each subcoil's innermost turns can be found by using:

$$H_j(r=a_i, z=0) = \sum_{i=1}^J j_i \lambda_i a_i F(\alpha_i, \beta_i) \left(1 - \frac{1}{2} E_{2i} + \frac{3}{8} E_{4i} - \frac{5}{16} E_{6i} + \dots \right) \quad \begin{matrix} i=1,2,3 \\ j=1,2,3 \end{matrix} \quad (13)$$

in which each mE_{ni} term in the expansion series represents a unique field-homogeneity correction factor for each subcoil ($i = 1, 2$, and 3), with m being Legendre polynomial coefficients and the E_{ni} being calculated from the derivatives of Eq. 11 with respect to the axial distance z .

From Fig. 2, we can see that the total volume, V_T , to be minimized is the sum of the subcoils' volumes:

$$V_T = \sum_{i=1}^3 2\pi a_i^3 (\alpha_i^2 - 1) \beta_i, \quad (14)$$

in terms of the dimensionless ratios α and β .

Equation 14 is minimized with respect to four constraint equations; Eq. 10 and Eq. 13 with $j = 1, 2$, and 3 . A Lagrange multiplier-solving algorithm solves these five nonlinear equations (Eqs. 10, 13, and 14) for the six unknown α_i and β_i . The complete size and geometry for each subcoil pack is found by inverting Eq. 12, to determine the lengths and outer radii for each subcoil.

IV - RESULTS AND CONCLUSIONS

Several high-field coil designs have been created by this technique. Figure 3 plots several coil system volumes and their costs as a function of the inner bore diameter for 20-T peak field solenoids. Note the approach toward an asymptotic limit at an inner bore diameter of 300 cm. Attempts to build coils larger than this will not only become quite costly, but may in fact be impossible with the technology discussed in this report. Two major factors contribute to this. First, the low critical current density (even of Nb₃Sn:Ti) at 20 T forces the inner subcoil, which generates the final 15 to 20 T field, to occupy approximately 25 to 33% of the system's total volume. Second, the electromagnetic forces from the outer subcoil are so large that nearly equal amounts of conductor and stainless

Fig. 3. Solenoid magnet volume and cost vs inner bore diameter.

steel are needed to react against the hoop stresses. The additional stainless steel in the winding pack lowers the overall current density, resulting in a need for an even larger coil. Eventually, this runaway condition produces the asymptote indicated in Fig. 3.

The presence of another asymptotic phenomenon is indicated by Fig. 4, which plots the minimum-volume coil size needed for producing a given central magnetic field with an inner bore diameter of 260 cm. These coils use various materials for the

Fig. 4. Minimum magnet volume vs maximum field for three structural materials.

parallel winding technique; one-half hard copper, or 304 LN or 21-6-9 stainless steels. In all uses, the maximum permissible electromagnetic strains on the conductor are set in the inequality defined in Eq. 8. Figure 4 indicates that the low Young's modulus and yield stress of copper limits its use to coils (at this bore diameter) generating 14 T. Even 304-LN stainless becomes of limited usefulness at 17 T, while the greater yield stress of 21-6-9 allows its use in coils producing a 20 T field at this bore diameter. References /1,2/ tabulate the various design steps and list specific coil-pack design examples.

V - ACKNOWLEDGMENTS

It is always a pleasure to acknowledge the work of those people who help make these reports possible. We would like to thank Mrs. V. Zuppan for her phenomenally excellent manuscript typing, Mr. S. Greenberg for his editing, and Mr. J. Warmouth for his production of the report figures.

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