Dr #1315

ORO-5152-1

**DESIGN STUDY OF TOROIDAL MAGNETS FOR
TOKAMAK EXPERIMENTAL POWER REACTORS**

Date Published—December 1976

Magnetic Corporation of America
Waltham, Massachusetts

TECHNICAL INFORMATION CENTER
ENERGY RESEARCH AND DEVELOPMENT ADMINISTRATION

DISCLAIMER

This report was prepared as an account of work sponsored by an agency of the United States Government. Neither the United States Government nor any agency Thereof, nor any of their employees, makes any warranty, express or implied, or assumes any legal liability or responsibility for the accuracy, completeness, or usefulness of any information, apparatus, product, or process disclosed, or represents that its use would not infringe privately owned rights. Reference herein to any specific commercial product, process, or service by trade name, trademark, manufacturer, or otherwise does not necessarily constitute or imply its endorsement, recommendation, or favoring by the United States Government or any agency thereof. The views and opinions of authors expressed herein do not necessarily state or reflect those of the United States Government or any agency thereof.

DISCLAIMER

Portions of this document may be illegible in electronic image products. Images are produced from the best available original document.

NOTICE

This report was prepared as an account of work sponsored by the United States Government. Neither the United States nor the United States Energy Research and Development Administration, nor any of their employees, nor any of their contractors, subcontractors, or their employees, makes any warranty, express or implied, or assumes any legal liability or responsibility for the accuracy, completeness or usefulness of any information, apparatus, product or process disclosed, or represents that its use would not infringe privately owned rights.

This report has been reproduced directly from the best available copy.

Available from the National Technical Information Service, U. S. Department of Commerce, Springfield, Virginia 22161

Price: Paper Copy \$11.00 (domestic)
 \$13.50 (foreign)
 Microfiche \$3.00 (domestic)
 \$4.50 (foreign)

DESIGN STUDY OF TOROIDAL MAGNETS
FOR TOKAMAK EXPERIMENTAL POWER REACTORS

Editors:

Z. J. J. Stekly

E. J. Lucas

December, 1976

Prepared for

Division of Magnetic Fusion Energy
Energy Research and Development Administration

Under Contract No. E - (40-1)-5152

Magnetic Corporation of America
179 Bear Hill Road
Waltham, Massachusetts 02154

Acknowledgements

The study was carried out by various individuals at Magnetic Corporation of America (MCA) who contributed in their areas of expertise. These individuals are:

Z. J. J. Stekly - Project Leader
E. J. Lucas
W. F. B. Punchard
N. Tischler
J. W. Ayers
A. Beckwith
T. A. de Winter
W. Heilbrunn

In addition, the efforts of W. Lipman, R. Renshaw and D. Sutton in the report production, and J. Flanagan, H. Sutherland, J. Stocker, L. Clinton and N. Lombard in helping with the report preparation are acknowledged.

The Combustion Engineering (CE) contribution to the study came from the Power Systems and Air Preheater Divisions. MCA designs were reviewed by CE for ease and cost of manufacturing and the fabrication was prepared and submitted to MCA by the Power Systems Division.

Foreword

The information contained in this report was developed by Magnetic Corporation of America (MCA), with cooperation and major inputs from the Power Systems and Air Preheater Divisions of Combustion Engineering (CE).

CE's part in the effort consisted of review of the designs for fabricability and generation of the manufacturing plan for all the structural system components which was submitted to MCA. The final report was written by MCA.

CE material was incorporated into the final report by MCA personnel and thus responsibility for misinterpretations or changes is MCA's.

TABLE OF CONTENTS

Acknowledgements	ii
Foreword	iii
List of Figures	viii
List of Tables	xv
Abstract	xviii
1. INTRODUCTION	1-1
2. SUMMARY DESCRIPTION OF CONTENTS	2-1
2.1 General	2-1
2.2 Comparison of the Characteristics of the Designs	2-5
2.3 Summary of Individual Designs	2-15
2.4 Coil Assembly	2-31
2.5 Comments on the Designs	2-44
3. CONCEPT FOR 8-TESLA PEAK FIELD	3-1
3.1 Winding	3-1
3.1.1 Introduction	3-1
3.1.2 Cooling	3-2
3.1.3 Conductor Stability	3-4
3.1.4 Stabilizer Material	3-16
3.1.5 Conductor	3-17
3.1.6 Electrical Insulation	3-42
3.2 Structure	3-45
3.2.1 Introduction	3-45
3.2.2 Toroidal Field Coil	3-58
3.2.3 Room Temperature Vessel (Warm Central Cylinder Design Only)	3-67
3.2.4 Central (Bucking) Cylinder	3-73
3.2.5 Intercoil Structure	3-82
3.2.6 Gravity Supports	3-83
3.3 Coil Protection	3-88
3.3.1 Quench	3-88
3.3.2 Electrical Shorts	3-89
3.3.3 Protection Circuit	3-91

TABLE OF CONTENTS (continued)

3.3.4	Cryogenic System Problems	3-101
3.3.5	Interactions with the Coil Design	3-101
3.4	Dewar Topology	3-103
3.4.1	Introduction	3-103
3.4.2	Warm Central Cylinder Design	3-104
3.4.3	Cold Central Cylinder Design	3-104
3.5	Heat Loads to Cryogenic System	3-107
3.5.1	Introduction	3-107
3.5.2	Magnetic Shields and Heat Input to Shields	3-108
3.5.3	AC Losses in the Conductor	3-126
3.5.4	Eddy Currents in the Structure	3-131
3.5.5	Other Heat Loads and Summary	3-139
3.6	Fabrication Plan	3-145
3.6.1	Introduction	3-145
3.6.2	Conductor Fabrication	3-150
3.6.3	Coil Winding	3-153
3.6.4	Coil Assembly	3-155
3.6.5	Magnet Assembly Module	3-161
3.6.6	Central Cylinder	3-161
3.6.7	Dewar Fabrication	3-161
3.7	Maintenance Features	3-170
3.7.1	Introduction	3-170
3.7.2	Warm Bucking Cylinder Design	3-170
3.7.3	Cold Bucking Cylinder Design	3-173
	REFERENCES FOR SECTION 3	3-175
4.	CONCEPT FOR MAXIMUM PRACTICAL FIELD	4-1
4.1	Factors Determining Maximum Practical Field	4-1
4.1.1	Comparison of Major Design Parameters with 8 T Design	4-4
4.2	Windings	4-9
4.2.1	Cooling and Conductor Stability	4-9
4.2.2	Conductor	4-11
4.2.3	Winding Configuration	4-17
4.3	Structure and Dewar	4-20

TABLE OF CONTENTS (continued)

4.4	Coil Protection	4-32
4.5	Other Heat Loads and Summary	4-36
4.6	Fabrication Plan	4-39
4.7	Maintenance Features	4-39
	REFERENCES FOR SECTION 4	4-40
5.	COST AND SCHEDULE ESTIMATE	5-1
5.1	Cost Estimate	5-1
5.2	Program Schedule	5-12
6.	UNCERTAINTIES IN THE DESIGN	6-1
6.1	Magnitude and Rate of Change of Pulsed Field	6-1
6.2	Conductor Uncertainties	6-3
6.3	Cryogenic Uncertainties	6-4
6.4	Structure	6-5
6.5	Others	6-6
7.	RECOMMENDED TEST COIL SIZE AND TEST CONDITIONS	7-1
7.1	Introduction	7-1
7.2	Characteristics of the Recommended Size	7-4
7.3	Test Conditions	7-4
8.	RECOMMENDATIONS FOR ADDITIONAL RESEARCH AND DEVELOPMENT	8-1
APPENDICES		
I.	Analysis of Tension and Bending in Multicoil Systems	AI-1
AI.1	Introduction	AI-1
AI.2	Single Coil Energized	AI-2
AI.3	All Toroidal Field Coils Energized in Series	AI-3
AI.4	Load on a Single Coil with Current i_n ; All Other Currents Equal	AI-3
AI.5	Tension and Bending	AI-4

TABLE OF CONTENTS (continued)

II.	Fabrication Plan	AII-1
AII.1	Introduction	AII-1
AII.2	Conductor Fabrication	AII-4
AII.3	Windings	AII-13
AII.4	Cold Coil Structure	AII-31
AII.5	Other Component Fabrication	AII-45
AII.6	Assembly of Warm Central Cylinder Design	AII-53
AII.7	Cold Central (Bucking) Cylinder Design	AII-70

LIST OF FIGURES

<u>Figure</u>	<u>Title</u>	<u>Page</u>
2.1-1	Clear Access Requirement	2-3
2.3-1	8 T Warm Central Cylinder Design	2-16
2.3-2	8 T Warm Central Cylinder Design: Coil Cross Section	2-17
2.3-3	Cross Section of Typical Coil Module	2-22
2.3-4	Schematic Illustrating Method of Interlayer and Interturn Insulation	2-23
2.3-5	Various Conductor Grades for 8 T Design	2-24
2.3-6	8 T Cold Central Cylinder Design	2-27
2.3-7	8 T Cold Central Cylinder Design: Coil Cross Section	2-28
2.3-8	12 T Warm Central Cylinder Design Major Dimension	2-32
2.3-9	12 T Warm Central Cylinder Design - TF Coil Cross Section Along Straight Leg	2-33
2.3-10	Conceptual Conductors for 12 Wb/m ² Torus Design	2-36
2.3-11	12 T Cold Central Cylinder Design Major Dimension	2-38
2.3-12	12 T Cold Central Cylinder Design - TF Coil Cross Section Along Straight Leg	2-39
2.3-13	TF Magnetic Assembly Elements	2-42
3.1-1	Critical Current Density of MCA Production Grade NbTi Composite Conductor Showing 8 T Design Points	3-3
3.1-2	Critical Strand Diameter Versus Magnetic Flux Density for NbTi Superconductor at Two Operating Temperatures	3-8
3.1-3	Simple Model to Illustrate Frictional Heating of a Composite Conductor as a Result of Relative Motion	3-10
3.1-4	Curves of Constant Temperature Rise as a Function of Normal Stress and Distance Moved for Relative Conductor Motion	3-12
3.1-5	Boiling Heat Transfer Characteristic for Liquid Helium I for Well Ventilated Vertical Flat Plates	3-14
3.1-6	Peak Nucleate Boiling Heat Flux for Vertical Channels in Liquid Helium (Continuous Heating Condition)	3-15

LIST OF FIGURES (Continued)

<u>Figure</u>	<u>Title</u>	<u>Page</u>
3.1-7	Schematic Showing the Electric Circuits to be Used When Charging and Discharging the Torus	3-20
3.1-8	Power Dissipation Due to AC Losses for Whole of Torus as a Function of Conductor Size	3-25
3.1-9	Schematic Illustrating Evolution of Design of Copper Stabilizer	3-32
3.1-10	Distribution of Windings and Turns as a Function of Magnetic Field	3-36
3.1-11	Various Conductor Grades for 8 T Design	3-38
3.1-12	Surface Heat Flux in Various Regions of the Coil Due to AC Losses During the One-Second Charge Period of the Vertical Field Coils	3-41
3.1-13	Schematic Illustrating Method of Interlayer and Interturn Insulation	3-44
3.2-1A	8 T Warm Central Cylinder Design	3-47
3.2-1B	8 T Warm Central Cylinder Design: Coil Cross Sections	3-48
3.2-2A	8 T Cold Central Cylinder Design	3-49
3.2-2B	8 T Cold Central Cylinder Design: Coil Cross Sections	3-50
3.2-3	Stick Model of D-Shaped TF Coil	3-63
3.2-4	8 T Magnetic Loads: Normal Condition	3-64
3.2-5	8 T Magnetic Loads: Fault Condition	3-65
3.2-6	8 T Magnetic Loads: Test Condition	3-66
3.2-7	TF Coil Displacement Modes: Warm Central Cylinder Design	3-70
3.2-8	TF Coil Displacement Profile, Normal Loading	3-71
3.2-9	TF Coil Deflection Modes: Cold Central Cylinder Design	3-74
3.2-10	Bucking Cylinder Loading Under Normal and Fault Conditions	3-76
3.2-11	Bucking Cylinder Finite Element Displacement Plot: Fault Condition	3-79
3.2-12	Bucking Cylinder Sizing	3-81

LIST OF FIGURES (Continued)

<u>Figure</u>	<u>Title</u>	<u>Page</u>
3.2-13	Illustration of Low Heat Leak Supports Consisting of Three and Five Members	3-85
3.3-1	Temperature at the End of a Quench as a Function of Initial Current Density and Quench Time	3-90
3.3-2	Normalized Current in Shorted Single Turn During Discharge of System with a 2-Minute Time Constant	3-92
3.3-3	Normalized Current in Shorted 100-Turn Bundle During Discharge of System with a 2-Minute Time Constant	3-93
3.3-4	Current in Shorted Coil (895 Turns) During Discharge of System with 2-Minute Time Constant	3-94
3.3-5	Power Dissipated in Short of Single Turn During System Discharge with 2-Minute Time Constant	3-95
3.3-6	Power Dissipated in Short of 100-Turn Bundle During System Discharge with 2-Minute Time Constant	3-96
3.3-7	Power Dissipated in Short of Single Coil (895 Turns) During System Discharge with 2-Minute Time Constant	3-97
3.3-8	Circuit Showing Short-Circuited Coil Isolated from Rest of System	3-98
3.3-9	Protective Circuit in Rapid Discharge of System with All Coils in Series	3-99
3.5-1	Applied Pulse Shape and Model Used to Calculate Winding Shielding Effects	3-110
3.5-2	Average Power Dissipation in Shield	3-112
3.5-3	Refrigeration Power as a Function of Shield Time Constant for Shield Operating Temperatures of 20 K and 80 K	3-113
3.5-4	Relative Conductor Magnetization Losses as a Function of Shield Time Constant	3-115
3.5-5	Relative Eddy Current Losses as a Function of Shield Time Constant	3-116
3.5-6	Total Refrigeration Power as a Function of Shield Time Constant for a Shield Temperature of 20 K	3-118

LIST OF FIGURES (Continued)

<u>Figure</u>	<u>Title</u>	<u>Page</u>
3.5-7	Total Refrigeration Power as a Function of Shield Time Constant for a Shield Temperature of 80 K	3-119
3.5-8	Shield Time Constant as a Function of Shield Resistivity and Thickness	3-120
3.5-9	Refrigeration Power for a Double Shielded System	3-124
3.5-10	Relative Eddy Current Loss for a Double Shielded System	3-125
3.5-11	Schematic Showing Location of the Three Different Conductor Grades and the Coil Sections Used in Computing the Conductor AC Loss	3-127
3.5-12	Cross Section of 8 T Warm Central Cylinder Design Showing Required Insulation to Minimize Eddy Currents	3-132
3.5-13	Cross Section of 8 T Cold Central Cylinder Design Showing Required Insulation to Minimize Eddy Currents in the Nose Area	3-133
3.5-14	Eddy Current Losses in a Solid Central Cylinder Due to Pulsed Fields of Poloidal Coils	3-137
3.5-15	Bucking Cylinder Assembly	3-138
3.6-1	Major Components Arrangement	3-146
3.6-2	Fabrication Plan	3-147
3.6-3	TF Magnet Assembly Elements	3-148
3.6-4	Schematic Illustrating Method of Interlayer and Interturn Insulation	3-151
3.6-5	Schematic Diagram of Coil Module Winding Process	3-154
3.6-6	Exploded View of Conductor Joint	3-156
3.6-7	Coil Module Fabrication Sequence	3-157
3.6-8	Coil Module Winding Machine	3-158
3.6-9	Cross Section of Coil Module	3-159
3.6-10	Cold Coil Structure: Plan View	3-160
3.6-11	Cross Section of Coil Assembly	3-163
3.6-12	Inspection and Testing Plan Flow Diagram, Coil Assembly	3-164

LIST OF FIGURES (Continued)

<u>Figure</u>	<u>Title</u>	<u>Page</u>
3.6-13	Side View of Coil/Intercoil Shear Panel Alignment and Assembly at Factory	3-165
3.6-14	Top View of Coil/Intercoil Spacer Alignment and Assembly at Factory	3-166
3.6-15	Intercoil Shear Panel, Cold Central Cylinder Design	3-167
3.6-16	Bucking Cylinder Assembly	3-168
3.6-17	Dewar Sections for Cold Central Cylinder Design	3-169
4.1-1	Estimate of Installed Refrigerator Power as a Function of Heat Load at 4.2 K and 2.5 K	4-5
4.1-2	Estimated Refrigerator Cost as a Function of Total Heat Load at 4.2 and 2.5 K	4-6
4.2-1	Heater Power at Takeoff and Recovery for Several Bath Temperatures	4-10
4.2-2	Conceptual Conductors for 12 Wb/m ² Torus Design	4-14
4.2-3	Critical Current Density of MCA Production Grade NbTi Composite Conductor Showing 12 T Design Points	4-16
4.2-4	Schematic Illustrating Method of Interlayer and Interturn Insulation	4-18
4.2-5	Cross Section of Typical Coil Module	4-19
4.3-1	12 T Warm Central Cylinder Design: Major Dimensions	4-24
4.3-2	12 T Warm Central Cylinder Design: TF Coil Cross Section Along Straight Leg	4-25
4.3-3	12 T Cold Central Cylinder Design: Major Dimensions	4-28
4.3-4	12 T Cold Central Cylinder Design: TF Coil Cross Section Along Straight Leg	4-29
4.4-1	Schematic Showing Additional Series Discharge Resistor Between Two Coil Halves to Reduce Discharge Voltage	4-34
5.2-1	Experimental Power Reactor Toroidal Magnet System	5-13
5.2-2	Manufacturing Schedule for Warm Central Cylinder Design	5-14
5.2-3	Manufacturing Schedule for Cold Central Cylinder Design	5-15

LIST OF FIGURES (Continued)

<u>Figure</u>	<u>Title</u>	<u>Page</u>
AII-1	Schematic Illustrating Method of Interlayer to Interturn Insulation	AII-5
AII-2	Schematic Diagram of Coil Module Winding Process	AII-14
AII-3	Illustration of Typical Joint Geometry	AII-16
AII-4	Exploded View of Conductor Joint	AII-18
AII-5	Cross Section of Typical Coil Module	AII-22
AII-6	Coil Module Fabrication Sequence	AII-24
AII-7	Coil Module Winding Machine (Conceptual Drawing)	AII-26
AII-8	Inspection and Testing Plan Flow Diagram - Coil Assembly	AII-29
AII-9	Major Component Arrangement	AII-32
AII-10	Cold Coil Structure	AII-34
AII-11	Center Plate - Cold Coil Structure	AII-36
AII-12	Center Plate Fit-up and Weld	AII-37
AII-13	Hoop Plates - Cold Coil Structure	AII-38
AII-14	Closed Die Plate Forming	AII-40
AII-15	Hoop Plate - Fit-up and Weld	AII-41
AII-16	Side Plate - Cold Coil Structure	AII-42
AII-17	Cold Coil Structure - Fit-up and Weld	AII-43
AII-18	Room Temperature Vessel	AII-46
AII-19	Intercoil Shear Panel - Warm Central Cylinder Design	AII-49
AII-20	Split Ring Segment - Bucking Cylinder Design	AII-51
AII-21	Coil Fabrication Plan Outline	AII-56
AII-22	TF Magnet Assembly Elements	AII-57
AII-23	Cross Section of Coil Assembly	AII-58
AII-24	Top View of Coil/Intercoil Spacer Alignment and Assembly at Factory	AII-61

LIST OF FIGURES (Concluded)

<u>Figure</u>	<u>Title</u>	<u>Page</u>
AII-25	Side View of Coil/Intercoil Shear Panel Alignment and Assembly at Factory	AII-62
AII-26	Bucking Cylinder Assembly	AII-64
AII-27	Major Components - Toroidal Field Magnet Assembly	AII-65
AII-28	Intercoil Shear Panel-Cold Central Cylinder Design	AII-71
AII-29	Dewar Cross Section	AII-72
AII-30	Upper Section - Outer Vessel	AII-74
AII-31	Lower Section - Outer Vessel	AII-75
AII-32	Bucking Cylinder - Inner Wall	AII-76
AII-33	Access Port Section	AII-77
AII-34	Inner Vessel Octant	AII-78

LIST OF TABLES

2.1-1	Main Requirements	2-2
2.2-1	General Configurations	2-6
2.2-2	Major Design Parameters of Magnet Systems	2-10
2.2-3	Design Parameters	2-12
2.2-4	Summary of System Weights	2-14
2.3-1	Summary of 8 T Torus Characteristics: Warm Central Cylinder Design	2-18
2.3-2	8 T Magnet Characteristics	2-25
2.3-3	Summary of 8 T Torus Characteristics: Cold Central Cylinder Design	2-29
2.3-4	Summary of 12 T Torus Characteristics: Warm Central Cylinder Design	2-34
2.3-5	12 T Magnet Characteristics	2-37
2.3-6	Summary of 12 T Torus Characteristics: Cold Central Cylinder Design	2-40
3.1-1	Helium Heat Sink Capability	3-5
3.1-2	Comparison of Resistivity of Copper and High Purity Aluminum at Various Field and Neutron Fluences	3-18
3.1-3	Charge and Discharge Voltages - 8 T Magnet	3-21
3.1-4	Instantaneous Power Generation Due to Various Loss Mechanisms in Round Wires	3-24
3.1-5	Values of Parameter Used in Applying Formulae of Table 3.1-4 to Produce Figure 3.1-8	3-27
3.1-6	Average Power Dissipation Over a Cycle Due to Various Loss Mechanisms in Rectangular Conductor	3-29
3.1-7	8 T Magnet Conductor Specifications	3-39
3.2-1	Summary of Mechanical Characteristics of Major Components: Warm Central Cylinder Design: Original and Alternate	3-51
3.2-2	Summary of Mechanical Characteristics of Major Components: Cold Central Cylinder Design	3-53
3.2-3	Bucking Cylinder Material Comparison: Normal Condition, 8 T Design	3-77
3.2-4	Bucking Cylinder Material Comparison: Fault Condition, 8 T Design	3-80

List of Tables (Continued)

3.5-1	Refrigerator Power and Shield Thickness at Several Temperatures and AC Losses	3-122
3.5-2	AC Loss in Whole Torus in KW by Specific AC Loss Mechanism	3-129
3.5-3	Material Properties Used in Calculation of AC Losses for 8 T Coil, and Conductor Dimensions Used	3-130
3.5-4	Eddy Currents in Cold Structure	3-135
3.5-5	Summary of Heat Input from All Sources: 8 T Warm Central Cylinder Design	3-140
3.5-6	Summary of Heat Input from All Sources: 8 T Cold Central Cylinder Design	3-141
3.6-1	Work Flow Sequence for Superconducting Wire and Cable	3-152
4.1-1	Comparison of Characteristics of Cold Central Cylinder Design at 8 and 12 T	4-7
4.2-1	Charge and Discharge Voltages - 12 T Magnet	4-12
4.2-2	12 T Magnet Characteristics	4-15
4.3-1	12 T Material and Design Stress Summary	4-22
4.3-2	Summary of Mechanical Characteristics of Major Components: 12 T Warm Central Cylinder Design	4-26
4.3-3	Summary of Mechanical Characteristics of Major Components: 12 T Cold Central Cylinder Design	4-30
4.4-1	Operation of 12 T Protection System	4-35
4.5-1	Summary of Heat Input from All Sources: 12 T Warm Central Cylinder Design	4-37
4.5-2	Summary of Heat Input from All Sources: 12 T Cold Central Cylinder Design	4-38
5.1-1	Estimated Costs for the 8 T Warm Central Cylinder Design: Design I	5-2
5.1-2	Estimated Costs for the 8 T Warm Central Cylinder Design: Design Ia	5-3
5.1-3	Estimated Costs for the 8 T Cold Central Cylinder Design: Design II	5-4
5.1-4	Estimated Costs for the 12 T Warm Central Cylinder Design: Design III	5-5
5.1-5	Estimated Costs for the 12 T Cold Central Cylinder Design: Design IV	5-6

List of Tables (Continued)

5.1-6	Materials and Estimated Labor Hours for Fabrication of 8 T Warm Central Cylinder Design: Design I	5-7
5.1-7	Materials and Estimated Labor Hours for Fabrication of 8 T Warm Central Cylinder Design: Design Ia	5-8
5.1-8	Materials and Estimated Labor Hours for Fabrication of 8 T Cold Central Cylinder Design: Design II	5-9
5.1-9	Materials and Estimated Labor Hours for Fabrication of 12 T Warm Central Cylinder Design: Design III	5-10
5.1-10	Materials and Estimated Labor Hours for Fabrication of 12 T Cold Central Cylinder Design: Design IV	5-11
7.1-1	System Comparison: Scale Models to EPR	7-5
7.1-2	Comparison of Main Variables of Scale Model to EPR to EPR	7-6

APPENDICES

AII-1	Work Flow Sequence for Superconducting Wire and Cable	AII-7
-------	--	-------

ABSTRACT

This report contains the results of a six-month study of superconducting toroidal field coils for a Tokamak Experimental Power Reactor to be built in the late 1980's.

The designs are for 8 T and 12 T maximum magnetic field at the superconducting winding. At each field level two main concepts were generated; one in which each of the 16 coils comprising the system has an individual vacuum vessel and the other in which all the coils are contained in a single vacuum vessel.

The coils have a D shape and have openings of 11.25 m x 7.5 m for the 8 T coils and 10.2 m x 6.8 m for the 12 T coils.

All the designs utilize rectangular cabled conductor made from copper stabilized Niobium Titanium composite which operates at 4.2 K for the 8 T design and at 2.5 K for the 12 T design.

Manufacturing procedures, processes and schedule estimates are also discussed.

1. INTRODUCTION

This report contains the results of a six-month design study of superconducting toroidal field coils for a Tokamak Experimental Power Reactor.

Starting from specifications of the system size and general system configuration, a total of four designs and one alternate design were developed. Three designs were generated at 8 T and two for 12 T magnet systems.

The study began in May, 1976. Preliminary concepts were presented at a meeting in Oak Ridge at the end of August, 1976. At that time, one 8 T and one 12 T design concept were presented and only a partial account of the manufacturing considerations was included in the designs. Since August, the additional designs have been generated and iterated upon with regard to changes dictated by manufacturing considerations.

All the designs at 8 T and 12 T were generated using copper and NbTi composite superconductor of a type currently being produced in tonnage quantities. As such the basic building-block for the conductors is readily available today.

The study was carried out by Magnetic Corporation of America with inputs from the Power Systems and Air Preheater Divisions of Combustion Engineering. Inputs from Combustion Engineering were primarily associated with the structure and manufacture of the magnet system.

Two basic guidelines were adhered to in the generation of each of these designs. First, in so far as it was practical, existing technology was to be utilized. Second, the manufacturing procedures were to be such that, as much as possible, finished magnet assemblies would be delivered to the Reactor Site.

The following major conclusions have been reached as a result of the

study:

1 The design concepts that are being considered for fabrication do not present any insurmountable manufacturing problems. All processes, equipment and metallurgical requirements are well within present "state-of the art" capabilities. The size, configuration and tolerance demands of the components dictate the development and implementation of some special methods, procedures and tooling to accomplish the unusual requirements for manufacture of the components. Combustion Engineering has, in the past, accomplished fabrication of large components having extraordinary size and tolerance parameters with methods, procedures and equipment that in most instances are similar to the requirements for fabrication of the EPR Coil structures.

2 Niobium Titanium superconductor technology can be stretched to provide an operational field of 12 T in an EPR system.

3 The requirements for the conductor, refrigeration system and structure are very sensitive to the amplitude and rate of change of the pulsed field generated by the poloidal field coils.

4 An EPR toroidal field magnet system can be fabricated such that completed magnet pairs, tested, and assembled with the intercoil structure can be shipped to the reactor site.

To the extent that it was possible the different failure modes of the magnet system were anticipated and taken into account in the designs. This led to a system that allows faulty coils to be isolated and discharged in a safe manner. It further led to an increase in the weights of the structural components.

Although the basic requirement of the study was to generate one design at 8 T (70% of the effort) and one design at a higher field (30% of the effort) a total of five designs were generated. Aside from the variation in magnetic field (the higher field was selected as 12 T), it

was felt that important system considerations entered into the design of the coils.

The considerations were those of ease of coil isolation and removal as well as the question of single system vacuum or individual vacuums for each coil. The choice of an individual vacuum container for each coil leads to bulkier coils and less effective use of conductor in the central region where all of the coils come together. On the other hand, use of a single vacuum allows a more compact winding.

The choice between these alternates depends on the penalty one has to pay for choosing the system with individual dewars. While a selection between these alternates is not presented in this report, the two alternate designs are. This report contains the four primary designs - 8 T with individual dewars, 8 T with single dewar, 12 T with individual dewars, 12 T with a single dewar. The fifth design is simply a variation of the structural configuration in the 8 T design with individual dewars.

The initial magnitude of 0.5 T in a one-second ramp as the value of the pulsed magnetic field generated by the poloidal field coils was used in the study for sizing the conductor, the refrigeration and the torque structure. The option of dropping the maximum value to 0.1 T was utilized only to estimate the heat load of the system as designed. The lower torques were not taken into account. As such the designs presented are capable of mechanically supporting the forces and torques corresponding to 0.5 T maximum poloidal field, however, the desired goal of less than 10 MW refrigeration power could only be achieved by reducing the magnitude of the pulsed field.

Like all studies of finite scope and finite time, the designs are limited by the available time and effort. Further time and effort would have resulted in improving the designs by more careful consider-

ation of the tradeoffs involved, optimization through analysis and further feedback of manufacturing methods and costs. Because of this, the designs represent concepts that have been carried forward to a point determined primarily by the time allowed for the performance of the work.

The designs are summarized in Section 2 which contains drawings and tables that present the main performance and design variables.

Section 3, which discusses the two 8 T design concepts, contains the bulk of the design considerations utilized in the design of superconducting coils of this size. This section includes a detailed description of the analyses and reasons for the selection of particular design details or variables. It discusses conductor cooling, conductor selection, winding configuration, structural considerations, dewar geometry, fabrication, and maintenance features.

Section 4 discusses the two 12 T design concepts. Since many of the design considerations are identical to the 8 T system, reference is made to the pertinent discussions contained in Section 3.

Section 5 contains the estimated costs and schedules for building the magnets. These schedules show a fabrication completion time of five years for the entire magnet system. Since the designs presented herein were restricted to the use of present-day technology and can be made using existing fabrication techniques and equipment, the realization of these schedules depends primarily on the emphasis that is put on the EPR program.

Section 6 is a summary of the uncertainties in the design.

Section 7 presents the reasons for recommending a 1/3 size coil as a preliminary model prior to building the EPR size coils.

Section 8 is a brief description of the recommended development to support the design concepts presented in this report.

Appendix I is an analysis of the loading conditions that exist in a multicoil system.

Appendix II reviews in detail the component factors of the fabrication plan summarized in Section 3.

2. SUMMARY DESCRIPTION OF CONCEPTS

2.1 General

This section presents a summary of the design requirements, design considerations, and characteristics of the 8 T reference and the 12 T high field designs generated in this study. Because of the importance of being able to remove and replace a single coil in the torus two designs were carried through in parallel at each of 8 T and 12 T levels. In one the coils can be removed by simple disassembly (without warmup)--the other requires warmup and breaking of the main vacuum and disassembly of the vacuum vessel. The fifth design is a variation of one of the 8 T designs.

Emphasis throughout the study centered on the 8 T design; and, consequently, more details on design, design philosophy, and fabrication are included in this report relative to the 8 T designs than for the 12 T designs. However, since the 12 T design concepts are similar to the 8 T, approaches and considerations were developed for the 8 T designs and then applied to the 12 T designs. Because of the similarities in the conductors and structural configurations and the overall concepts the 8 T discussions are also applicable in large part to the 12 T designs.

The primary requirement of the study was to design a coil system for use in an Experimental Power Reactor (EPR) having a maximum field at the winding of 8 T. In addition to this, a higher field design was required. The choice of field level for this design being the maximum practical field level that could be attained using state-of-the-art of superconducting coil and conductor technology.

The main requirements for the 8 and 12 T designs are summarized in Table 2.1-1 and Figure 2.1-1. The table shows the geometrical constraints

Table 2.1-1

MAIN REQUIREMENTS

<u>Case</u>	<u>1.</u>	<u>2.</u>
Peak Toroidal Field.....	8 T	As high as practicable in range 11-16 T
Magnet Size		
Horizontal Opening.....	7.5 m	6.8 m
Vertical Opening.....	11.25 m	10.2 m
Free Central Diameter.....	4.3 m	4.7 m
Number of Coils.....	16	16
Pulsed Field.....	Up to 0.5 T in 1 sec ramp	
	Optional:	Reduction by Factor of 5 if too difficult
Radiation Environment.....	10^7 rads in lifetime*	
	10^{17} n/cm ² -yr (E > 0.1 Mev)+	
Nuclear Heating.....	Up to 10^{-6} w/g	
Total Power Consumption..... (including Refrigeration)	Less than 10 MW	

* Affects electrical insulation choice

+ Affects resistivity of copper and aluminum

Figure 2.1-1 Clear Access Requirement

for both field levels as well as the maximum pulsed field to which the coils are subjected to as a result of the magnetic field generated by the poloidal field coils.

The large amplitude of the pulsed field (an average value of 0.4 T vertical field for force calculating purposes) generates a large overturning moment on each of the coils. This results in large lateral forces which must be supported. The support of these loads causes large conduction heat loads if the forces are transmitted to room temperature and adds to the eddy current heating if the resulting support structure is cold.

The 0.5 T maximum pulsed field ramp in 1 sec was the primary consideration in the selection of the conductors for each design as well as the major cause of the high heat loads due to AC losses in the conductor and structural eddy currents.

Partway through the study the pulsed field requirements were changed by adding an option that would reduce the pulsed field by a factor of five. This option could be chosen as a design alternative if the pulsed field problems became too severe. The net result of choosing this option would be to deemphasize the high AC losses and high torques resulting from use of the higher pulsed field value. Rather than change the basic design to accommodate only the lower pulsed field the designs were generated for the 0.5 T requirement; however, the performance for operation of the system was generated for the condition when the applied pulse was lowered to 0.1 T. Had the designs been generated for the lower value of pulsed field from the beginning, significant conductor as well as torque structure changes would result.

Because the charging of the coil system is an infrequent occurrence the primary power consumption by the coil system is that required to operate

the refrigerator. As such the goal of a 10 MW power requirement for the coil system represents a maximum allowable heat load. While it was possible to design the coils to withstand the 0.5 T/sec maximum pulsed field the resulting refrigeration power consumptions exceeded the goal of 10 MW of refrigerator power. Because essentially all of the heat load is due to eddy current heating and coupling losses in the conductor, the heat load is very sensitive to the rate of change of magnetic field; and, consequently, a reduction by 5 in the field rate of change reduces these particular heat loads by 25.

All of the designs at both field levels make use of Nb-Ti conductor and thus are based on current coil and conductor technology. The 8 T designs operate at 4.2 K while the 12 T designs operate at 2.5 K. All the designs use a D shape in which the large centering forces are supported along the straight leg of the D and the curved portion of the D approaches a pure tension shape to approach the conditions existing in solenoids.

Because the inner region (along the major radius of the toroid) as well as the vertical and horizontal bore of the coils was specified, there are variations in the major radius of the designs due to the fact that the depth of the coils varies with the choice of magnetic field as well as the particular configuration chosen.

2.2 Comparison of the Characteristics of the Designs

The main design and performance characteristics for the five designs chosen are identified in Table 2.2-1 and summarized in a comparative form in Tables 2.2-2 through 2.2-4 and individually for each of the designs in Tables 2.3-1 through 2.3-6 and Figures 2.3-1 through 2.3-12.

Table 2.2-1

GENERAL CONFIGURATION

	<u>I</u>	<u>Ia</u>	<u>II</u>	<u>III</u>	<u>IV</u>
1. Maximum Magnetic Field T	8	8	8	12	12
2. Operating Temperature K	4.2	4.2	4.2	2.5	2.5
3. Dewar Type	Individual Dewars for Each Coil	Individual Dewars for Each Coil	Single Dewar	Dewars for Each Coil	Single Dewar for Each Coil
4. Central Support Cylinder	Room Temp	Room Temp	He Temp	Room Temp	He Temp
5. Intercoil Structure	Room Temp	Room Temp	He Temp	Room Temp	He Temp
6. Main Features	Easy Insertion & Removal from System	Easy Insertion & Removal from System	Compact Coil Assembly	Easy Insertion & Removal from System	Compact Coil Assembly
7. Lateral Access	Rectangular Access Space	Elliptical Access Space	Elliptical Access Space	Elliptical Access Space	Elliptical Access Space
8. Protection	Rapid Discharge of System in Series if No Electrical Short Discharge of Individual Faulted Coil				
Structure Requirement	Support Cylinder, Intercoil Structure, and Coil Assembly Designed for One Coil at Zero Current				

Designs I, Ia, and II are for 8 T utilizing a fully copper stabilized Nb-Ti composite conductor at 4.2 K. While Designs III and IV are for 12 T using similar conductors operating at 2.5 K, all designs use an open cooling passage with nucleate boiling liquid helium as the coolant. This cooling mode was chosen primarily because of considerations of temperature rise for a given amount of heat absorption as well as the ability to replace helium liquid from adjacent cooling passages.

Design I is an 8 T system in which each coil has its own individual dewar. The coil centering forces are supported by a central support cylinder at room temperature. By making use of a room temperature intercoil structure which supports the overturning moments due to the field of the poloidal coils, this design minimizes the task of initially assembly the system as well as the removal and replacement of a coil should this ever be required.

The requirement for full lateral access between the 16 coils is met in this design by a rectangular opening which spans 90° and goes from coil to coil. This requires heavy structural sections to adequately transmit the torque from the top of the coil to the bottom of the coil with a resultant penalty in system complexity and weight.

The coils are designed for a rapid system discharge with a time constant of two minutes in the case of vacuum or cryogenic system failure. Should a coil develop a short it can be isolated and separately discharged. This procedure results in lateral loads on adjacent coils as well as bending in a pure tension or minimum bending coil design. In addition, the central cylinder is also subjected to unsymmetrical compression. This results in heavier structures to support the windings, to carry the large lateral loads from coil-to-coil, and to support the unbalanced centering forces on the central support cylinder. All of these have been considered in developing the designs presented within this report.

With individual dewars the coils are independent of each other cryogenically so that one large vacuum leak will affect only one coil of the system--requiring only the affected coil to be shut down and not necessarily the entire system. This allows operation in a symmetrical mode by shutting down every other coil. Use of a single vacuum shuts down the complete system if a vacuum leak occurs. Further, should a shorted coil occur simultaneously with a vacuum failure, isolation and controlled shutdown of the faulted coil cannot be done because of the necessity to shut the whole system down. The use of individual dewars as well as individual coil isolation in the event of fault together with the attendant structural and space penalties fulfill all of the requirements that coil and system protection impose on the design.

Design I-a has all of the features of Design I except that the lateral access has been changed from rectangular to a reinforced elliptical hole. This change considerably simplifies the design of the coil assembly and intercoil structure in the access region area. This elliptical access design has been used in all the other designs.

While the individual dewars and warm central cylinder facilitate the insertion and removal of coil assemblies as well as prevent vacuum failure from spreading to the whole system, there is a penalty in space and in conduction heat load for a design of this type.

A cold rather than warm central cylinder would eliminate having to transmit the large radial centering forces to room temperature. Further, elimination of individual dewars results in a more compact cross section of the winding and structure. Design II was therefore generated using a cold central cylinder and cold intercoil structure as well as a single large room temperature vacuum vessel. Like Designs I and I-a the intercoil structure, central cylinder, and low temperature coil structure are designed to withstand

the operation of individual coils at current values different from the toroidal current. This affects all of the structural components.

The value of the magnetic field for the high field design option was chosen at 12 T based on considerations of the maximum practical field for Nb-Ti conductor (at reduced temperature) and considerations of a purely structural nature, which show that the main loads, including those on the conductor, increase as the square of the magnetic field.

The two 12 T designs have the same basic variations as the 8 T designs; namely, warm central cylinder with individual dewars for Design III, and cold central cylinder with a single room temperature vacuum vessel for Design IV.

The main system parameters are shown in Table 2.2-2 for all the systems. The table shows the required vertical and horizontal bores as well as the diameter of the free central region. The major radii of each system is determined by the dimensions of the components in each of the designs. Because of the more compact nature of designs with a cold central cylinder the major radii and weights of the cold central cylinder designs are smaller than the comparable values for a warm central cylinder design.

For operation at 0.5 T/sec pulsed field the 8 T designs require 28.3 MW of refrigeration power for a warm central cylinder design, while a cold central cylinder design requires 42.3 MW of refrigeration power. This is primarily due to the fact that the cold central cylinder design has additional cold intercoil structure with eddy current heating. Reduction of the pulse magnitude to 0.1 T/sec results in refrigeration power of 9.7 MW for the warm central cylinder design and 4.5 MW for the cold central cylinder design. The main reason for this reversal is that the warm central cylinder has higher conduction heat loads which become more important when the AC loss and eddy current heat loads have been reduced for the lower value of pulsed field.

Table 2.2-2

MAJOR DESIGN PARAMETERS OF MAGNET SYSTEMS

Type	<u>I</u> Warm Central Cylinder	<u>Ia</u> Warm Central Cylinder	<u>II</u> Cold Central Cylinder	<u>III</u> Warm Central Cylinder	<u>IV</u> Cold Central Cylinder
Major Rad. (m)*	7.75	7.75	7.57	8.31	7.80
Vertical Bore (m)	11.25	11.25	11.25	10.2	10.2
Horizontal Bore (m)	7.5	7.5	7.5	6.8	6.8
Free Central Diameter (m)	4.3	4.3	4.3	4.7	4.7
Number of Coils	16	16	16	16	16
Magnetic Energy (GJ)	25.9	25.9	25.9	73.8	73.8
Magnetic Field at Winding (T)	8	8	8	12	12
Total Weight (M Tons)	6,312	5,760	5,900	10,330	8,930
Energy/Wt GJ/T	.0041	.0045	.0044	.0071	.0083
Operating Temperature K	4.2	4.2	4.2	2.5	2.5
Refrig. Power (0.5 T/sec) MW	28.3	28.3	42.3	104	117
Refrig. Power (0.1 T/sec)- MW	9.7	9.7	4.5	29	8.5

* Average of major radii to the inner and outer bore.

+ Refrigeration power of system designed for 0.5 T/sec when operated at 0.1 T/sec.
Redesign would result in lower heat loads and somewhat lighter structures.

The use of one or more normal metal shields at 20 K, 80 K, and room temperature was thoroughly investigated. The results of the study showed that with electrical resistivities that results from the radiation levels of 10^{17} neutrons/cm²/year specified for this study the shields occupy too much space or there is no net decrease in the total refrigerator power requirement (sum of the refrigeration power required for the heat load in the shield due to eddy currents plus the refrigeration power required for the windings). Consequently, all the designs presented herein are unshielded.

Because the 12 T designs have more structure and conductor and operate at lower temperature, their refrigeration powers are higher than the corresponding 8 T design at the same magnitude of pulsed magnetic field. Again, at the lower magnitude of pulsed magnetic field (0.1 T/sec) the conduction heat load for the warm central cylinder (with individual room temperature dewars) predominates.

The desired refrigeration power level of 10 MW or less is achieved at the lower magnitude of pulsed field - 0.1 T/sec by all the designs except the 12 T design with a warm central cylinder and individual dewars.

As pointed out earlier, the designs were all based on a pulsed field of 0.5 T in 1 second ramp. As such, eddy currents and AC losses as well as the maximum cyclic lateral force depend on this value. The systems were designed to take this maximum force. In obtaining the refrigeration power for 0.1 T/sec the eddy current and AC losses were reduced accordingly. However, the system was not redesigned for lower lateral forces which would lower conduction heat loads as well as potentially lighten the shear structure required to react to the lateral loads.

The main characteristics of the coils are compared in Table 2.2-3.

Table 2.2-3

DESIGN PARAMETERS

<u>Design</u>	<u>I</u>	<u>Ia</u>	<u>II</u>	<u>III</u>	<u>IV</u>
Maximum Field (T)	8	8	8	12	12
Operating Current (k A)	10	10	10	10	10
Number of Conductor Grades	3	3	3	6	6
Conductor AC Losses (KW)					
(.5 T Pulsed Field)	60.2	60.2	54.8	113.1	121.3
(.1 T Pulsed Field)	3.6	3.6	3.3	6.0	5.4
Estimated Eddy Current Losses (KW)					
(.5 T Pulsed Field)	26.1	26.1	118.8	99.0	214.1
(.1 T Pulsed Field)	1.0	1.0	4.8	4.0	8.6
Cold Structure					
Material/Design Stress	310S/276MN/m ²	310S/276MN/m ²	310S/276MN/m ²	310S/379MN/m ² 718/655MN/m ²	310S/379MN/m ² 718/655MN/m ²
Dewar Material/Design Stress	Nitronic 33/193MN/m ²	Nitronic 33/193MN/m ²	Nitronic 33/193MN/m ²	Nitronic 33/193MN/m ²	Nitronic 33/193MN/m ²

All the designs use rectangular cables of Nb-Ti and copper composite stabilized with stranded copper. The conductors are cooled by means of boiling liquid helium at 4.2 K for the 8 T magnets and 2.5 K for the 12 T magnets.

The 8 T designs have three different conductor types while the 12 T designs have six different types which are designed to operate at intermediate values of magnet field. This grading of the conductor saves superconducting material, as well as space.

The conductor current densities range from 2.54 to 6.05 k A/cm² in all of the designs which allow 120 sec (exponential time constant) for shutdown before the adiabatic heating at a hot spot could damage the winding by excessive temperatures. This 120 second discharge time was used in designing the protection system for all coils, as well as the voltages to which the coils were exposed to during a rapid shutdown.

The 10 k A was chosen as the operating current for all designs primarily based on conductor AC loss considerations (larger conductors result in larger losses). The AC losses in the conductor and the cold structure eddy current losses (these are the major heat loads at 0.5 T/sec) are also shown in Table 2.2-3.

The weights of the major system components in the various designs are shown in Table 2.2-4. As has already been pointed out the designs using individual dewars for each coil are heavier.

To minimize manufacturing risks and operational downtime and costs should a fault occur in one of the coils, all of the concepts presented are designed to be fabricated in a building block fashion. Each of the coils is divided into eight separate winding modules containing the superconducting windings.

Table 2.2-4

SUMMARY OF SYSTEM WEIGHTS

<u>Type</u>		<u>I</u>	<u>Ia</u>	<u>II</u>	<u>III</u>	<u>IV</u>
		Warm Central Cylinder Individual Dewars		Cold Central Cylinder Single Dewar	Warm Central Cylinder Individual Dewars	Cold Central Cylinder Single Dewar
Maximum Field	T	8	8	8	12	12
Operating Temperature	K	4.2	4.2	4.2	2.5	2.5
Weight of Conductor/Coil (1000 kg)		61.4	61.4	56.0	137	124
Weight of One Coil Assembly (1000 kg)		244	244	256	440	404
Weight of Central Cylinder (1000 kg)		227	227	227	364	441
Dewar Weight	(1000 kg)	1856	939	885	1611	870
Total Weight of System	(1000 kg)	6310	5760	5900	10330	8930
Magnetic Energy Stores	GJ	25.9	25.9	25.9	73.8	73.8
Energy/Weight	GJ/1000 kg	.0041	.0045	.0044	.0071	.0083

During manufacturing each of these modules is separately fabricated and then assembled into the structure. This permits the replacement of individual winding modules rather than entire coil assemblies should a defect occur in the superconducting windings either during manufacturing or subsequent operations.

2.3 Summary of Individual Designs

This section presents a summary of the individual designs generated. Figure 2.3-1 shows the overall layout of the 8 T concept using individual dewars for each of the sixteen coils. Table 2.3-1 presents the main characteristics of the system. This design makes use of a warm central cylinder which the 16 D-shaped coils butt up against during normal operation. The D-shape was chosen primarily because the high load bearing area was straight and structural tolerances could be better maintained. Intercoil structure at room temperature connects coil to coil and is used to transmit the overturning torque resulting from the vertical field generated by poloidal coils and the out-of-plane loads arising from a fault condition. A rectangular access hole provides full access to the magnet field region over a 90° arc. Helium and electrical connections are made at the top of each coil.

Figure 2.3-2 shows the coil cross section details: (1) along the straight section next to the central cylinder in the region where the large centering forces are reacted; (2) at the top and bottom of the coil; and (3) in the region adjacent to the access area.

In the nose region all of the forces in the winding under normal operation are directed toward the small base (nose) of the trapezoidal section. The internal coil structure is designed to take up this load and transmit it to the nose region by means of side plates on each side and the center post.

Fig. 2.3-1 8 Tesla Warm Central Cylinder Design

Fig. 2.3-2 8 Tesla Worm Central Cylinder Design

Table 2.3-1

SUMMARY OF 8 T TORUS CHARACTERISTICS
Warm Central Cylinder Design

I. GENERAL

Peak Toroidal Field	8 T
Superconductor	NbTi
Stabilizer	Copper
Number of Coils	16
Coil Shape	D
Coil Clear Bore	
Vertical	11.2 m
Horizontal	7.5 m
Operating Temperature	4.2 K

II. ELECTRICAL

A. General

Operating Current	10 k A
Stored Energy	25.9 GJ
Inductance	518 H

B. Conductor

Total Conductor Length	544 km
Number of Turns/Coil	949
Avg Conductor Length/Turn	36 m
Avg Conductor Current Density	4575 A/cm ²
Avg Conductor Cross Section Area/Turn	$2.19 \times 10^{-4} \text{ m}^2$
Winding Cross Section Area/Turn	0.492 m ²
Avg Conductor Cross Section Area/Coil	0.208 m ²
Amp-Turns/Coil	10.05×10^6
(Bearing) Stress in Conductor	21 MN/m ²

Table 2.3-1 (Continued)

SUMMARY OF 8 T TORUS CHARACTERISTICS
Warm Central Cylinder Design

III. MECHANICAL

A. Cold Coil Structure

Avg Tensile Force in Coil	82.9 MN
Avg Tensile Stress	156 MN/m ²
Maximum Combined Stress	276 MN/m ²

B. Major Component Weights (metric tons)

Component

1. Conductor (per coil assembly)

NbTi	2.0
Copper	59.4

2. Cold Structure (per coil Assembly)

Stainless Steel	<u>182.5</u>
-----------------	--------------

Coil Assembly Weight	243.9
----------------------	-------

3. Room Temperature Vessel
(per coil assembly)

Stainless Steel	112.3	57.0
Al Radiation Shield	3.7	3.7

4. Intercoil Structure (per coil assy)

Stainless Steel	<u>20.2</u>	<u>41.1</u>
Total Weight per Coil	380.1	345.7
Weight for Torus	6082	5531

5. Central (Bucking) Cylinder

Stainless Steel	<u>227</u>	<u>227</u>
-----------------	------------	------------

TOTAL TORUS WEIGHT	6310	5760
--------------------	------	------

The individual coil winding modules form vacuum-tight helium containers permitting the cold structure to be bolted together with insulation between the joints to minimize eddy currents. The cold structure is held in place by means of epoxy fiberglass G-10 bearing blocks stationed at 80 K. For the case when an adjacent coil is deenergized, large lateral forces away from the deenergized coil are generated. The internal structure is designed to take these forces and transmit them through bearing blocks on the sloping sides to the room temperature vessel walls.

For ease of manufacture the cross section changes to accommodate the use of flat plates in the construction. The cross section of the winding modules remains the same; the sloping sides of trapezoidal cold structure in the nose area are replaced by sides comprising flat plates bolted together; the room temperature dewar becomes rectangular, with the extra space taken up by increasing the length of the bearing blocks.

Regions away from the straight section are where the large cyclic lateral forces due to the vertical field of the poloidal coils are applied. The internal structure is designed to transmit this (on one side only) to the room temperature structure. The lateral forces are in turn reacted by a shear structure which transmits the resulting torque from top to bottom of the torus. The shear structure is composed of the intercoil structure and the room temperature dewar. In the region adjacent to the access area the outer room temperature dewar wall increases to a size determined by the requirement to transmit shear from top to bottom of the torus to react the lateral forces resulting from the vertical field of the poloidal coils.

This large cross section is heavy to fabricate and install. However, it is required if all of the access region is to be left open. If instead of

maximizing the access area the shear structure is continued from top to bottom with a large elliptical hole for access, the large cross section becomes unnecessary. This is Design I-a. Because of the design advantages of this elliptical access all further designs were generated using elliptical access areas.

The room temperature dewar is a bolted construction with seal welds to form the vacuum enclosure.

Figure 2.3-3 shows the general cross section of a winding module--it is generally valid for all the designs. The module itself forms a vacuum-tight container and forms the low temperature dewar. Ground insulation is placed around the inside of the module to withstand the maximum voltages the conductor is expected to see. The main philosophy of using winding modules is to be able to separate the manufacturing steps. The modules are wound and sealed separately. They are then inserted into the structure which is then bolted together. Since the module itself is the helium vessel the low temperature structure can be bolted and segmented as required to reduce structural eddy current heat loads.

Figure 2.3-4 is a schematic of the conductor illustrating the interlayer and interturn insulation method that was utilized. As shown, this insulation technique will result in the formation of cooling passages within the conductor bundle.

Specific conductors for the 8 T designs and their characteristics are shown in Figure 2.3-5 and Table 2.3-2. These are stranded and kept to a minimum size primarily for AC loss considerations. (Fully insulated strands would have improved the AC losses but were not used because of current distribution problems, the inability to current share, and the need for full ground insulation around each fully-insulated strand in case of a physical break in an adjacent strand).

Figure 2.3-3

Cross Section of Typical Coil Module

Figure 2.3-4 Schematic Illustrating Method of Interlayer and Interturn Insulation

Figure 2.3-5

Various Conductor Grades for the 8 T Design

Table 2.3-2

8 T Magnet

Conductor Specifications

Operating Temperature 4.2 K

Characteristics of Component Strands

Material	Niobium titanium in copper matrix
Copper to Superconductor ratio	1.8:1
Diameter	.187 cm (73.6 mils)
Filament Diameter	24.7 μm

Characteristics of Stabilized Conductor

Type of Construction	Rectangular stranded cable conductor (Rutherford type) with central insulated divider stabilized with additional stranded copper
----------------------	---

Grade	1	2	3
Maximum Field, T	8.0	5.3	2.7
Operating Current, kA	10.0	10.0	10.0
Critical Current, kA	13.3	13.8	14.2
Number of Component Strands	17	10	6
Overall Copper to Superconductor Ratio	15.0	19.8	28.4
Conductor Current Density, kA/cm ²	3.74	4.64	5.79
Superconductor Current Density, kA/cm ²	60.0	102	170
Surface Heat Flux, W/cm ²	0.35	0.35	0.35
Overall Width, cm	.97	.97	.97
Overall Height, cm	3.68	3.00	2.41

The central support cylinder in this design as well as in the others is segmented to reduce eddy currents and is thick enough to withstand the unsymmetrical compressive loading which results when one coil is at zero current. To reduce eddy currents the following interfaces are insulated-- internal joints in the cold structure, room temperature container, intercoil-dewar interface, room temperature vessel, central support cylinder.

The 8 T cold central cylinder design and characteristics is shown in Figure 2.3-6 and Table 2.3-3 (Design II). This design has a single large dewar that is built up of segments bolted together at insulated joints, a cold central cylinder, and cold intercoil structures. Because only the weight of the system is reacted at room temperature the conduction heat load is considerably lower than the system with individual dewars and warm central cylinder. The cold intercoil structure as well as the wider cross section of the coil results in significantly more eddy current losses than in the warm central cylinder version.

The insertion or removal of a coil in this system is done by bringing the system up to room temperature and removing a segment of the dewar to provide access to the coils. The disadvantage of this procedure has to be weighed against the compactness and lighter weight of this type of design. The winding cross sections in the straight section, at the top and bottom of the coil, and at the area adjacent to the access region are shown in Figure 2.3-7. The cross sections are wider and have less depth than the comparable warm central cylinder designs. The reason for this is that the space in the straight section can be more effectively utilized as far as the conductor is concerned. The winding module construction and conductor details are the same as for the warm central cylinder design. The access area is elliptical for reasons already covered.

Fig. 2.3-6 8 Tesla Cold Central
Cylinder Design

SECTION D-D

SECTION E-E

SECTION F-F

Fig. 2.3-7 8 Tesla Cold Central Cylinder Design

Table 2.3-3

SUMMARY OF 8 T TORUS CHARACTERISTICS

Cold Central Cylinder Design

I. GENERAL

Peak Toroidal Field	8 T
Superconductor	NbTi
Stabilizer	Copper
Number of Coils	16
Coil Shape	D
Coil Clear Bore	
Vertical	11.2 m
Horizontal	7.5 m
Operating Temperature	4.2 K

II. ELECTRICAL

A. General

Operating Current	10 k A
Stored Energy	25.9 GJ
Inductance	518 H

B. Conductor

Total Conductor Length	495 Km
Number of Turns/Coil	886
Avg Conductor Length/Turn	35 m
Avg Conductor Current Density	4575 A/cm ²
Avg Conductor Cross Section Area/Turn	2.19 x 10 ⁻⁴ m ²
Winding Cross Section Area/Turn	0.461 m ²
Avg Conductor Cross Section Area/Coil	0.194 m ²
Amp-Turns/Coil	9.39 x 10 ⁶
(Bearing) Stress in Conductor	16 MN/m ²

Table 2.3-3 (Continued)

SUMMARY OF 8 T TORUS CHARACTERISTICS
Cold Central Cylinder Design

MECHANICAL

A. Cold Coil Structure

Avg Tensile Force in Coil	82.9 MN
Avg Tensile Stress	156 MN/m ²
Maximum Combined Stress	276 MN/m ²

B. Major Component Weights (metric tons)

Component

1. Conductor (per coil assembly)

NbTi	1.8.
Copper	54.2

2. Cold Structure (per coil Assembly)

Stainless Steel	<u>199.5</u>
-----------------	--------------

Coil Assembly Weight	255.5
----------------------	-------

3. Intercoil Structure (per coil assy)

Stainless Steel	<u>39.5</u>
-----------------	-------------

Total Weight per Coil	295.0
-----------------------	-------

Weight for Torus	<u>4720</u>
------------------	-------------

4. Central (Bucking) Cylinder

Stainless Steel	294
-----------------	-----

5. Dewar

Stainless Steel	784
-----------------	-----

Al Radiation Shield	<u>101</u>
---------------------	------------

TOTAL TORUS WEIGHT	5900
--------------------	------

Two 12 T designs were generated to provide the two alternate approaches--divided vacuum and ease of insertion and removal of lower weights and smaller cross sections. The 12 T systems require more conductor and more structure than the 8 T system. However, their mechanical layout and fabrication are similar to the corresponding 8 T designs.

The main mechanical differences between the 8 T and 12 T designs are:

1. Operation at lower temperature - 2.5 K
2. Use of six conductor grades
3. Each coil assembly is broken into two separate coils electrically to reduce the ground insulation requirement

The dimensions of the 12 T system with individual dewars and a warm central cylinder are shown in Figure 2.3-8 (Design III). The main characteristics are tabulated in Table 2.3-4. The cross section of the winding is shown in Figure 2.3-9. The six grades of conductor used for the 12 T designs are shown in Figure 2.3-10 and their characteristics are tabulated in Table 2.3-5. Because of the increased cross section of structure and the large amount of conductor the heat load is significantly higher than the 8 T design.

The 12 T system with cold central cylinder and a single dewar is shown in Figure 2.3-11. Its winding cross section in the straight region adjacent to the central support cylinder is shown in Figure 2.3-12. As is evident the cross section is considerably more compact than the warm central cylinder design. The system characteristics are summarized in Table 2.3-6.

2.4 Coil Assembly

Figure 2.3-13 illustrates the sequential assembly of the coils for the individual dewar design prior to their shipment to the site and installation

Figure 2.3-8 12 T Warm Central Cylinder Design
Major Dimensions

Figure 2.3-9 12 T Warm Central Cylinder Design: TF Coil Cross Section Along Straight Leg

Table 2.3-4

SUMMARY OF 12 T TORUS CHARACTERISTICS
Warm Central Cylinder Design

I. GENERAL

Peak Toroidal Field	12 T
Superconductor	NbTi
Stabilizer	Copper
Number of Coils	16
Coil Shape	D
Coil Clear Bore	
Vertical	10.2 m
Horizontal	6.8 m
Operating Temperature	2.5 K

II. ELECTRICAL

A. General

Operating Current	10 k A
Stored Energy	73.8 GJ
Inductance	1476 H

B. Conductor

Total Conductor Length	889 km
Number of Turns/Coil	1652
Avg Conductor Length/Turn	34 m
Avg Conductor Current Density	3985 A/cm ²
Avg Conductor Cross Section Area/Turn	2.66 x 10 ⁻⁴ m ²
Winding Cross Section Area/Turn	0.96 m ²
Avg Conductor Cross Section Area/Coil	0.44 m ²
Amp-Turns/Coil	17.5 x 10 ⁶
(Bearing) Stress in Conductor	38 MN/m ²

Table 2.3-4 (Continued)

SUMMARY OF 12 T TORUS CHARACTERISTICS

Warm Central Cylinder Design

III. MECHANICAL

A. Cold Coil Structure

Avg Tensile Force in Coil	189 MN
Avg Tensile Stress	330 MN/m ²
Maximum Combined Stress- 310 Stainless	379 MN/m ²
Inconel 718	655 MN/m ²

B. Major Component Weights (metric tons)

Component

1. Conductor (per coil assembly)

NbTi	5.3
Copper	131.7

2. Cold Structure (per coil Assembly)

Stainless Steel	<u>303</u>
-----------------	------------

Coil Assembly Weight	440
----------------------	-----

3. Room Temperature Vessel
(per coil assembly)

Stainless Steel	94.6
Al Radiation Shield	6.1

4. Intercoil Structure (per coil assy)

Stainless Steel	<u>82.2</u>
Total Weight per Coil	<u>622.9</u>
Weight for Torus	9966

5. Central (Bucking) Cylinder

Stainless Steel	364
-----------------	-----

TOTAL TORUS WEIGHT	10,330
--------------------	--------

Figure 2.3-10
Conceptual Conductors for 12 Wb/m^2 Torus Design

Table 2.3-5

12 T Magnet

Conductor Specifications

Operating Temperature 2.5 K

Characteristics of Component Strands

Material	Niobium titanium in copper matrix
Copper to Superconductor Ratio	1.8:1
Diameter	.187 cm (73.6 mils)
Filament Diameter	24.7 μ m

Characteristics of Stabilized Conductor

Type of Construction	Rectangular stranded cable conductor (Rutherford type) with central insulated divider stabilized with additional stranded copper					
----------------------	---	--	--	--	--	--

Grade	1	2	3	4	5	6
Maximum Field, T	12.0	11.0	10.0	8.0	5.3	2.7
Operating Current, kA	10.0	10.0	10.0	10.0	10.0	10.0
Critical Current, kA	13.7	13.4	14.1	20.8	13.5	14.8
Number of Component Strands	38	19	14	13	6	4
Overall Copper to Superconductor Ratio	10.5	16.4	20.4	20	33	42
Conductor Current Density kA/cm ²	2.54	3.26	3.56	3.90	5.17	6.05
Superconductor Current Density, kA/cm ²	26.8	53.7	72.9	78.5	171	254
Surface Heat Flux, W/cm ²	0.35	0.35	0.35	0.35	0.35	0.35
Overall Width, cm	0.965	0.965	0.965	0.965	0.965	0.965
Overall Height, cm	5.26	4.24	3.89	3.56	2.72	2.34

FA 3779 Figure 2.3-11

12 T Cold Central Cylinder Design
Major Dimensions

Figure 2.3-12

12 T Cold Central Cylinder Design: TF Coil Cross Section Along Straight Leg

Table 2.3-6

SUMMARY OF 12 T TORUS CHARACTERISTICS

Cold Central Cylinder Design

I. GENERAL

Peak Toroidal Field	12 T
Superconductor	NbTi
Stabilizer	Copper
Number of Coils	16
Coil Shape	D
Coil Clear Bore	
Vertical	10.2 m
Horizontal	6.8 m
Operating Temperature	2.5 K

II. ELECTRICAL

A. General

Operating Current	10 k A
Stored Energy	73.8 GJ
Inductance	1476 H

B. Conductor

Total Conductor Length	810 K m
Number of Turns/Coil	1546
Avg Conductor Length/Turn	33 m
Avg Conductor Current Density	3985 A/cm ²
Avg Conductor Cross Section Area/Turn	$2.66 \times 10^{-4} \text{ m}^2$
Winding Cross Section Area/Turn	.87 m ²
Avg Conductor Cross Section Area/Coil	.41 m ²
Amp-Turns/Coil	16.4×10^6
(Bearing) Stress in Conductor	30 MN/m ²

Table 2.3-6 (Continued)

SUMMARY OF 12 T TORUS CHARACTERISTICS
Cold Central Cylinder Design

III. MECHANICAL

A. Cold Coil Structure

Avg Tensile Force in Coil	189 MN
Avg Tensile Stress	330 MN/m ²
Maximum Combined Stress -310 Stainless	379 MN/m ²
Inconel 718	655 MN/m ²

B. Major Component Weights (metric tons)

Component

1. Conductor (per coil assembly)

NbTi	4.8
Copper	119.2

2. Cold Structure (per coil Assembly)

Stainless Steel	<u>280</u>
-----------------	------------

Coil Assembly Weight	404
----------------------	-----

3. Intercoil Structure (per coil assy)

Stainless Steel	<u>72.2</u>
-----------------	-------------

Total Weight per Coil	<u>476.2</u>
-----------------------	--------------

Weight for Torus	<u>7619</u>
------------------	-------------

4. Central (Bucking) Cylinder

Stainless Steel	441
-----------------	-----

5. Dewar

Stainless Steel	770
-----------------	-----

Al Radiation Shield	<u>100</u>
---------------------	------------

Total Torus Weight	8,930
--------------------	-------

WINDING MODULE

COLD COIL STRUCTURE

COIL ASSEMBLY

MAGNET ASSEMBLY MODULE

T. F. MAGNET ASSEMBLY

Figure 2.3-13
T. F. MAGNET ASSEMBLY ELEMENTS

into the toroidal magnet system. The basic element is the winding module which contains the superconducting windings and forms a closed helium-tight container. After being wound and sealed and passed through quality assurance tests it is assembled into the coil structure together with seven other modules. The coil structure is assembled into a coil assembly which is the outer walls of which are vacuum-tight and form the room temperature vacuum container. Once assembled into this coil assembly the windings are cooled down and tested for performance.

Following successful performance tests the coil assembly is put into a module consisting of two pretested coils and their intercoil structure which is then shipped to the site for installation. The modular construction of the coils together with the assembly procedure briefly outlined above has several advantages.

1. The risk of incurring expenses due to faulty coil winding fabrication is greatly minimized because a small section of faulty coil winding can be replaced instead of the entire coil winding of one coil.
2. During operation a faulted coil assembly could be repaired simply by removal of the defective module and insertion of a good module.
3. Coil modules are relatively small and light and therefore can be handled easily.
4. The critical coil winding and assembly operations can be conducted at the factory where the expertise and applicable machinery and skills are available economically. This means the fabrication costs are reduced and the reliability and quality is increase.

6. On-site personnel can be minimized and on-site assembly time can be reduced, implying lower assembly costs.

The coil assembly shown in Figure 2.3-13 is for a warm central (bucking) cylinder design. However, a similar procedure would be employed for a cold central cylinder design. The major difference would be that the vacuum vessel would not be assembled off site, as shown, but would be shipped to the reactor site in sections along with the magnet assembly modules and assembled there. Cost tests at the factory could still take place by setting up vacuum vessels for test purposes.

2.5 Comments on the Designs

If any conclusions can be drawn from these studies it is that magnets of the size required for the EPR can be designed and fabricated using essentially presently available technology. The main design problems encountered were those associated with designing the conductor, the structure as well as all other components to accommodate the 0.5 T/sec field generated by the poloidal field coils. This field influenced the choice of conductor current, the specific conductor design, and the heavy structures required to react the resulting torques--with the resulting high heat loads due to eddy currents, AC losses, and heat conduction due to the large cross sections of load-bearing supports.

Although at 8 T the cold central cylinder designs are more compact and lighter it is felt that the use of individual dewars may be more realistic from a system point of view. Redesign of the system for lower values of pulsed fields would under this version be more attractive, and further optimization may reduce the weight (and cost) penalty in going to individual dewars.

The 12 T designs are very reasonable as far as superconductor considerations are concerned, and margins of operation comparable to 8 T operation at 4.2 K are obtained by using NbTi at 2.5 K. The refrigeration requirement of the 12 T systems at 2.5 K is almost entirely due to the large pulsed field magnitude (0.5 T/sec). While operation at 2.5 K increases refrigeration power above that required for 4.2 K, the fact that the 12 T designs have more conductor, more structure, and the fact that conductor AC losses and heat loads due to structural eddy currents are proportional to the square of the rate of change of magnetic field, make the large refrigeration requirement mainly a result of the pulsed field level rather than the lower temperature or higher field at the winding. While further careful design optimization can undoubtedly reduce the AC losses and eddy currents, lower values of pulsed fields and rates of change of pulsed field will render both of the 12 T designs closer to present-day conductor and coil technology.

3. CONCEPT FOR 8 TESLA PEAK FIELD

3.1 Winding

The primary requirement of a conductor and winding configuration is that it operate reliably and effectively under the operating conditions to which it is subjected. The EPR requirement of large size and magnetic fields of 8 T and above coupled with the large pulsed fields of 0.5 T/sec make the choice of a suitable conductor and winding configuration very difficult.

Because of the large size, fully stable windings are desired--windings that revert back to being superconducting from any unexpected transition into the normally resistive state. However, this is associated with addition of large cross sections of copper which lower the current density and result in eddy current heating due to the large pulsed field. Breaking up of the conductor into individually fully insulated strands would result in minimizing the eddy current and ac loss problem within the superconductor at the expense of current sharing ability between the strands.

Further, the design of the conductor to operate successfully in an essentially straight region as well as in a curved region designed for minimum bending is a major mechanical consideration. In the straight region, the main loads are of a compressive nature perpendicular to the conductor, while in the curved minimum bending region the primary consideration is the tension along the conductor.

The following sections go over in considerable detail the design and design considerations of the conductor and windings for the 8 T design.

3.1.2 Cooling

The question of cooling mode for a superconducting magnet is strongly related to stability. Stability is usually arrived at by matching heat generation (due to current flowing in the substrate) to the rate of heat transfer from the surface to the coolant. In a magnet the available cooling at any point strongly depends on the heat conduction as well as heat transfer to the coolant. Consequently, whether or not a magnet is "stable" depends on the size and duration of the disturbance or heat source as well as the local heat transfer environment.

The largest superconducting magnets now in existence have been built on the basis of cryostatic stability which means that enough cooling is available to maintain the conductor below the transition temperature of the superconductor if all the current is flowing in the normal substrate. These have proved reliable relative to the internal disturbances inherent to their design and construction. The magnets with which this program is concerned are generally larger in size and stored energy than existing magnets; hence the total available energy for local perturbations or releases of energy (i.e., destabilizing influences) will also be larger. Furthermore, the field levels to be experienced in the windings are relatively high (~ 8 T). Reference to Figure 3.1-1 shows that the allowable variation in temperature for NbTi conductor is .5 K before transition to the normal state occurs. Consequently, the cooling mode selected should allow for the best possible temperature stability.

If the circulation of supercritical helium through hollow conductor is considered, it is likely that relatively large amounts of energy will be transported away from a local disturbance with limited temperature rise because the specific heat capacity of "fresh" helium is continually passing

Figure 3.1-1

Critical current density of MCA production grade Nb-Ti
composite conductor showing 8 T design points

over the disturbance. However, if the disturbance occurs over a long length of conductor (which becomes more likely as magnet size increases), then the temperature rise is limited primarily by the volume and specific heat of the helium within the disturbed region at the time the disturbance occurs since the helium cannot be replaced immediately.

Table 3.1-1 compares the local energy absorption capability of helium for boiling helium at 1 atmosphere and supercritical helium at 5 atmospheres (initially at 4.2 K). It is evident from the table that vaporizing liquid is a better heat sink and provides a more stable temperature than supercritical helium.

If helium is present in the cooling passages, the same amount of heat input produces a smaller temperature rise in the conductor if liquid is vaporized rather than if supercritical fluid is used. Helium in channels between conductors can communicate in three dimensions with liquid in the rest of the reservoir of liquid. For these reasons, boiling liquid helium in open passages was selected as the mode of cooling.

3.1.3 Conductor Stability

The current density that a superconductor fabricated into a conductor can carry when wound into a coil depends on its operating environment. The reason for this is that small mechanical, magnetic and thermal disturbances exist in magnet systems. These disturbances can generate instabilities in the current distribution which limit the maximum current density that can be achieved. The instabilities occur because the specific heat of all materials is very low at temperatures in the liquid helium range, and consequently, any heat generation, whether electromagnetic or mechanical in origin, can cause a local temperature rise capable of driving the superconductor into the normal state. The stability of the conductor is determined

Table 3.1-1

Helium Heat Sink Capability

	Energy Absorbed (J/G)	Temperature Rise (K)
Vaporization (1 atm)	20	None
Supercritical (5 atm)	3.88	1
	10.38	2
	18.99	3
	24.84	4

by the propagation or lack of propagation of the normal region. In general, this is determined by the details of construction.

There are a number of ways in which these disturbances can be prevented from raising the temperature of the superconductor enough to cause transition to the normal state. The most widely used of these are adiabatic stabilization¹ and cryogenic stabilization.²

Adiabatic stabilization is explained as follows: for current to flow in the bulk of a superconductor, magnetic flux must penetrate into the material. This flux flow causes heating of the superconductor and can result in magnetic instabilities referred to as flux jumps. A criterion for stability in the flux distribution can be determined by postulating a small change in flux, computing the temperature rise, and determining whether the resulting distribution is compatible with the temperature rise.

The result of this criterion is:³

$$d < \frac{\pi}{2j_{ch}} \left[\frac{\gamma c \{T_{ch} - T_b\}}{\mu_0} \right]^{\frac{1}{2}}$$

where $\mu_0 = 4\pi \times 10^{-7}$ H/m

γ = density of the superconductor (kg/m³)

c = specific heat of the superconductor (J/kgK)

T_{ch} = critical temperature of the superconductor at zero current in the presence of a magnetic field

T_b = bath temperature

j_{ch} = superconductor critical current density in the presence of a magnetic field

d = diameter of the superconducting filament

In this approach attention is concentrated on the superconductor itself, and heat transfer is neglected; hence the name adiabatic stability criterion. The criterion defines a critical filament size, so that if the diameter of

the superconducting filaments is less than that given by the above expression, flux jumps do not occur. If it is greater, then the energy dissipated in a flux jump may heat the material beyond its critical temperature, resulting in the formation of a normal region. If no alternative means for dissipating the subsequent heating is provided (e.g., a parallel, sufficiently low resistance path for current flow), then this normal region rapidly propagates throughout the winding causing the whole magnet to go normal.

Typical values of the maximum strand size for NbTi are shown in Figure 3.1-2.

Another important element in stability considerations for conductors of this type is the effect of twisting the conductor. In an untwisted multi-filament wire exposed to transient field conditions, circulating currents are set up in the high conductivity normal matrix in which the superconducting strands are embedded. These shield the flux from the inside of the conductor. Under these conditions the small strands in the untwisted conductor result in an improvement of heat flow out of the individual strands; however, from the point of view of adiabatic stability, the whole conductor acts as a single large superconductor. Twisting the conductor allows the magnetic field to penetrate between the individual strands and avoids this problem. The amount of twisting required depends upon the rate of change of field $\dot{B}$, the matrix resistivity ρ , the diameter of the superconducting filament d , the critical current density of the material j_c , and a packing factor of superconductor in the conductor λ . The distance between nodes (180° twist) which is necessary to decouple the filaments is given by the expression:⁴

$$L \ll \left(\frac{\pi \lambda d \rho j_c}{B} \right)^{\frac{1}{2}}$$

Figure 3.1-2.

Critical Strand Diameter Versus Magnetic Flux Density for NbTi Superconductor at Two Operating Temperatures

Provided that this condition holds, the filaments are decoupled (for fields perpendicular to the conductor), and the adiabatic stability criterion is met.

As will be described later, twisting the conductor is essential also from the point of view of minimizing the ac losses. The basic conductor eventually chosen for the 8 Wb/m² design was a 1.87 μm (74 mils) diameter twisted multifilament wire with a filament diameter of 24.7 μm (2,046 filaments in a 1.8:1 copper to superconductor ratio composite) and a twist rate of about 60 twists per meter. This conductor meets the adiabatic stability criteria described above.

The adiabatic stability criteria discussed thus far assure stability against operational perturbations induced by "flux jumps." Other sources for operational disturbance exist, however. Even if conductor is used which is adiabatically stable (to flux jumps), the windings may exhibit a tendency to quench. This behavior is attributed to frictional heating resulting from localized relative conductor motion under the high stresses produced by the forces internal to the magnet--in short, a mechanically induced perturbation.

The above effect may be illustrated with the simple model shown in Figure 3.1-3. In this figure a composite conductor is subjected to a compressive stress σ_n and a force F_T , which tends to move the conductor. σ_n and F_T represent the internal condition of stress within the winding and can be interpreted as compression between adjacent turns or layers. When the force F_T becomes sufficient to overcome friction or to shear the bonding agent, motion occurs; and energy is dissipated through frictional heating or the release of strain energy in bond fracture. In a superconducting winding, the thermal diffusivity is limited, and estimates indicate that the

Figure
3.1-3

Simple Model to Illustrate Frictional Heating of a
Composite Conductor as a Result of Relative Motion

temperature rise may easily be sufficient to initiate a normal region. Estimated temperature rises as a function of distance moved and stress level are shown in Figure 3.1-4. It points out the need for a design with low compressive or bearing stress in the windings as well as for a fully stabilized design which prevents propagation of small normal regions within the windings.

The pulsed magnetic fields, nuclear heating and cyclical mechanical loading present in an operational Experimental Power Reactor may induce instabilities in the conductor. To prevent any of these instabilities from initiating a self propagating normal region, the coil design presented herein is based on cryostatic stabilization which prevents the propagation of normal regions. This approach insures the reliability that will be required of the EPR magnet system.

All successful large (high stored energy) coils built to date have been fully stabilized (cryostatically stable). A fully stabilized conductor is one in which enough highly conducting normal material (matrix or substrate) is used to shunt the current around the superconductor and to conduct heat away should the superconductor go normal. Enough cooling is also provided so that the temperature of the matrix does not exceed the transition temperature of the superconductor for the ambient field and current density conditions even if a part of the superconductor is completely normal and the current is flowing entirely in the matrix. Under these conditions, the superconductor can cool down to below the transition temperature, thereby allowing current to transfer back from the matrix to the superconductor.

Thus, if for any reason (e.g., heat generated locally by frictional motion) a part of the conductor is raised temporarily above the transition temperature and the current is forced to flow in the matrix surrounding the

Figure
3.1-4

Curves of Constant Temperature Rise as a Function of Normal Stress and Distance Moved for Relative Conductor Motion in a Simple Model; Time Required for the Motion to Occur Is Also Shown.

superconductor, the current quickly transfers back into the superconductor and the normal region does not propagate.

The amount of matrix which must be added depends upon the resistivity of the matrix (for the ambient field), the operating current level, the heat transfer characteristics to the liquid helium bath, and the ratio of the cross-sectional area of the matrix to the cooled perimeter of the wire. Basically, the amount of matrix must satisfy the following relationship:

$$\frac{\rho I^2}{Ap} < q$$

where: I = operating current (Amps)
 ρ = resistivity of matrix (Ωcm)
 A = cross-sectional area of matrix (cm^2)
 p = cooled perimeter of conductor (cm)
 q = critical heat flux (W/cm^2)

The critical heat flux is the maximum heat flux which can support nucleate boiling. (Nucleate boiling occurs with small interface temperature differences typically of the order of 1 K or less.) Above the critical heat flux, transition to film boiling occurs, and the interface temperature difference rises to tens of Kelvins as shown in Figure 3.1-5. A temperature rise of this magnitude is unacceptable in view of the critical temperature limitations of present day superconductors.

The critical heat flux is a function of the geometry of the cooling channels. For well ventilated flat plates, it is about $0.8 \text{ W}/\text{cm}^2$. However, for small channels (e.g., in superconducting windings) it is a function of the channel length to hydraulic diameter ratio (l/d). For an l/d of about 15, it is reduced to about $0.35 \text{ W}/\text{cm}^2$ as shown in Figure 3.1-6.⁵ In a magnet there is considerable uncertainty over the achievable heat flux locally

Figure
3.1-5

Boiling Heat Transfer Characteristic for Liquid Helium I for Well
Ventilated Vertical Flat Plates.

Figure 3.1-6. Peak nucleate boiling heat flux for vertical channels in liquid helium (continuous heating conditions). (From: Johannes, op cit).

within the circuitous helium passages of a winding. However, results can be obtained on small scale models. Higher apparent local values are possible if multidimensional effects are included. However, a conservative value of 0.35 W/cm^2 was used in this design.

3.1.4 Stabilizer Material

As far as stabilizer material is concerned, at first sight it appears that aluminum might have distinct advantages over copper. With high purity aluminum zero field resistivities at liquid helium temperature of $3 \times 10^{-11} \Omega\text{m}$ or better can be attained. This is a factor of 5 better than can be achieved with copper. The implication is that for similar surface heat fluxes a conductor made using an aluminum stabilizer could be run at higher current densities than a conductor using a copper stabilizer. This would result in economy of material and winding volume. Without taking into account radiation effects, the poor mechanical properties of high purity aluminum and its developmental nature, aluminum has not been used in large stabilized conductors, whereas there is much experience with copper stabilized conductors.

In reality because of the nuclear radiation environment of the conductor (10^{17} neutrons/cm² year), the advantages of an aluminum stabilizer are largely lost due to increases in its resistivity. While this increase in resistivity may be removed by annealing, the magnet would have to be shut down and warmed up--a very undesirable procedure. A specific example will illustrate these effects. Copper with an initial (i.e., zero irradiation) zero field resistivity of $1.7 \times 10^{-10} \Omega\text{m}$ (100 times lower than its value at room temperature) will be compared with high purity aluminum with a zero irradiation, zero field resistivity of $0.30 \times 10^{-10} \Omega\text{m}$ (1,100 times lower than its value at room temperature). Initially, then, the copper resistivity

is 5.7 times greater than the aluminum resistivity. The subsequent increase in resistivity, which was derived from data given in reference 6, is summarized in Table 3.1-2. It can be seen that before irradiation the aluminum is better than copper by factors of 5.7 and 8.6 at zero field and 8 T, respectively. However, after a neutron fluence of 10^{17} neutrons/cm², at zero field the copper is better than the aluminum and at 8 T the aluminum is only about 30 percent better than the copper. This is not very significant in view of the very poor mechanical properties of high purity aluminum which would require special reinforcements or cladding in order to render it useful in a conductor. Hence, unless the conductor is annealed much more frequently than once per year, there is no advantage in using high purity aluminum, especially in view of the fact that most of the material in a coil is subjected to fields much lower than the maximum value.

Thus the relative effects of irradiation on copper and on aluminum coupled with the much more highly developed technology of copper stabilized conductors lead us to a choice of copper for the stabilizer for the conductor.

3.1.5 Conductor

3.1.5.1 Current Level

The choice of operating current depends on a number of factors. In this case the main factors considered were: allowable voltages within the coil, desirable charge and discharge times, power supply rating and ac loss considerations as well as ease of manufacture.

High currents lead to low voltage coils with small numbers of turns. Because of the large conductor cross section, the ac losses are higher. Low current coils lead to high voltages and large numbers of turns and lower ac losses.

Table 3.1-2

Comparison of Resistivities of Copper and High Purity Aluminum
at Various Fields and Neutron Fluences

		Resistivity, Copper $10^{-10} \Omega m$		Resistivity, High Purity Aluminum $10^{-10} \Omega m$		ρ_{Cu}/ρ_{Al}	
Neutron Fluence neutrons/cm ²	Flux Density Wb/m ²	0	8	0	8	0	8
	0	1.7	6.1	0.30	0.71	5.7	8.6
	10^{17}	2.5	6.8	3.2	5.1	0.78	1.3

From a system point of view, the most effective conductor is one that operates at a voltage that is as high as possible consistent with the integrity of the insulation and safety considerations.

Figure 3.1-7 shows the electrical circuit to charge and discharge the system. Since it is desirable to be able to discharge the system more rapidly than to charge it, the circuit in Figure 3.1-7 is used.

During charge the power supply voltage is applied across the whole torus consisting of 16 coils connected in series. During discharge the power supply is disconnected from the circuit and the coils are discharged, again in series, through dump resistors located between coils. (During charge these resistors are shorted out.) The voltage drops across the coils and the resistors are of opposite polarities and so the maximum potential difference between ground and any part of any coil in the torus is the voltage drop across one coil. By using this arrangement, the voltage which was applied across all the coils during charge can be applied across each coil on discharge. This results in a discharge time 16 times shorter than the charge time without increasing the electric stress on the coil insulation during discharge.

Table 3.1-3 summarizes the charge/discharge times and power requirements for an 8 T EPR system with an energy stored of 25.9 GJ as a function of allowable maximum voltage with respect to ground and of conductor current. The voltage which heads the four columns is the charge voltage (applied across the whole torus). For each conductor current the charge time given is for the voltage applied across the torus, and the discharge time is for the voltage applied across each of the 16 coils. The power is the peak power required during charge.

It was felt that charge times of the order of 60 minutes would be desirable and that a 10 MW power supply would be a reasonable size.

Figure 3.1-7

Schematic Showing the Electric Circuits to be Used When Charging and Discharging the Torus. The arrangement shown allows the voltage across each coil on discharge to be equal to the voltage across the torus on charge.

Table 3.1-3

Charge and Discharge Voltages - 8 T Magnet

Coil Energy = 25.9×10^9 J

	<u>500 V</u>	<u>1 kV</u>	<u>2 kV</u>	<u>10 kV</u>
5,000 A - charge (min.)	345	173	87	17
- power (MW)	2.5	5	10	50
- discharge (min.)	21.6	11	5.44	1.06
10,000 A - charge (min.)	173	87	43	9
- power (MW)	5	10	20	100
- discharge (min.)	11	5.44	2.68	.56
15,000 A - charge (min.)	115	58	29	6
- power (MW)	7.5	15	30	150
- discharge (min.)	7.19	3.63	1.81	.375
20,000 A - charge (min.)	87	43	22	4.5
- power (MW)	10	20	40	200
- discharge (min.)	5.44	2.68	1.375	.281

Furthermore, because of the very poor electric breakdown characteristics of warm helium gas (which exist at the warm end of the electrical leads to each coil), the maximum voltage was limited to 1 kV. It can be seen from Table 3.1-3 that this eliminates the 5000 A conductor. Lower current conductors are smaller, and smaller conductors have lower ac losses. Since it was found that ac losses were the most significant source of heat input to the system and were necessitating the use of a larger than desirable refrigeration system, it was decided to use the lowest current consistent with voltage and charge time requirement. Therefore a current of 10 kA was selected for the design.

3.1.5.2 Conductor AC Loss Considerations

The conductor in superconducting magnet systems subjected to transient magnetic fields inevitably experiences heating because of ac loss which occurs within the superconducting strands and due to eddy currents in the copper. These losses have to be considered from two points of view:

1. The ac loss may be such that the local heating causes a great enough rise in temperature in the conductor that the superconductor goes normal.
2. The total heat load imposed on the system may be so great as to make the refrigeration system economically unattractive.

As will be shown later, local heating is not a problem in this system; however, the heat load on the refrigeration system makes necessary very careful consideration of the conductor design.

AC losses in composite superconductors fall into two categories: magnetization type losses which occur in the superconductor itself and eddy current type losses which occur both in the matrix between the superconducting strands and in any copper added to the composite for purposes of

stabilization. As explained previously, for stability reasons a large overall copper to superconductor ratio is desirable. For economic reasons it is preferable to manufacture a low copper to superconductor ratio composite and add the required copper stabilizer afterwards. For such a conductor there will be eddy current ac losses in the copper stabilizer, magnetization losses in the superconducting filaments, and coupling (eddy) losses in the matrix between the filaments. For each of these cases there are losses associated with both the perpendicular and parallel components of field. The formulae for the instantaneous power generation per unit volume for each of these losses are shown in Table 3.1-4. These formulae are applicable to circular geometry and require modification to varying degrees to be applicable to practical rectangular geometries. However, these formulae were used in the initial stages of the conductor design to obtain initial estimates of (i) the relative magnitudes of losses due to the various mechanisms and (ii) the total loss for the torus as a function of size of components. Figure 3.1-8 summarizes the results of this analysis. The various losses are shown as averages over a cycle and are plotted against size. The size of each of the components requires the correct interpretation.

The magnetization and coupling losses that occur in the matrix between filaments are plotted versus the size of the monolith containing the superconductor and matrix (copper to superconductor ratio of 1.8). These losses represent the internal loss within the monolithic element and do not take account of eddy currents between monolithic elements which should be minimized.

The stabilizer losses⁰ are the eddy currents in the normal matrix, which is usually added to the conductor containing the superconductor for stability

Table 3.1-4

Instantaneous Power Generation Due to Various
Loss Mechanisms in Round Wires

Field Orientation with Respect to Conductor		
Loss Mechanism	Perpendicular	Parallel
Hysteresis in Superconductor	$p = \frac{2}{3\pi} \lambda j_c \dot{B} d$	$p = \frac{\lambda j_c \dot{B} d}{6}$
Eddy Currents in Matrix Due to Current Flow Between Filaments of Superconductor (Coupling Losses)	$p = \frac{\dot{B}^2 r_o^2}{\rho x_o^2}$	$p = \lambda j_c \dot{B} r_o \left[\frac{(1+x_o^2)^{1/2} - 1}{4x_o} \right]$
Eddy Currents in Copper Stabilizer	$p = \frac{\dot{B}^2 r_o^2}{4\rho}$	$p = \frac{\dot{B}^2 r_o^2}{8\rho}$

p = instantaneous power/unit volume (W/m^3)

$\dot{B}$ = instantaneous rate of change of field (Wb/m^2s)

ρ = resistivity of stabilizer (Ωm)

j_c = critical current density in superconductor (A/m^2)

λ = fraction of superconductor in composite

d = diameter of superconducting filaments (m)

r_o = radius of conductor (m)

$x_o = 2\pi r_o / \ell_p$ where ℓ_p = twist pitch (m)

Figure 3.1-8

Power Dissipation Due to AC Losses for Whole of
Torus as a Function of Conductor Size

reasons. The stabilizer can be in one piece, in which case the size corresponds to its diameter. However, if it is elongated or in several parts that are either insulated or have a high resistance between them, then the size is that of the individual components.

Also shown in Figure 3.1-8 is the total allowable heat leak to the system. Table 3.1-5 shows the values of the parameters used as input to the formulae shown in Table 3.1-4. The volumes of conductor used were preliminary estimates based on only one grade of conductor being used throughout the magnet. Of necessity, average values of properties varying from one region to another in the magnet were used. Nevertheless, the plot is very useful. It can be seen that as far as the superconducting composite is concerned, the major source of power loss is that due to eddy currents flowing in the normal matrix (between the filaments of superconductor) under the influence of the perpendicular field. By looking at the total loss in the composite and in the stabilizer and comparing it with the total allowable system heat leak, indications are that the largest component of the composite should be less than about 3.8 mm (0.15 in.) in diameter and that of the stabilizer less than about 12.7 mm (0.5 in.). A more detailed analysis taking into account variations in material properties throughout the magnet imposes more severe restrictions on sizes, limiting composites to dimensions of the order less than 2.5 mm (0.1 in.) diameter and stabilizer to dimensions less than about 3.8 mm (0.15 in.) diameter.

The current carrying capacity of 2.5 mm (0.1 in.) diameter 1.8-to-1 copper-to-superconductor-ratio composite in an 8 T field is about 1,000 A if operated at about 75 percent of short sample capacity. Since the desired operating current is about 10 kA, then approximately ten of these 2.5 mm (0.1 in.) diameter strands must be incorporated in some way into one conductor.

Table 3.1-5

Values of Parameter Used in Applying Formulae of
Table 3.1-4 to Produce Figure 3.1-8

Change in flux density, ΔB	0.5 T
Charge time, Δt_1	1.0 s
Discharge time, Δt_2	2.0 s
Cycle time, T	90 s
Matrix and stabilizer resistivity, ρ	$5.0 \times 10^{-10} \Omega m$
Superconductor critical current density, j_c	$80 \times 10^7 A/m^2$
Composite copper to superconductor ratio	1.8
Number of filaments in composite, n	180
Circumference to twist length ratio, x_0	0.126 (2 twists/inch in .508 mm (.02 in.) diameter wire)
Volume of copper stabilizer	$131 m^3$
Volume of composite	$17.9 m^3$
Volume of niobium titanium	$6.40 m^3$

They must be incorporated in such a way that the coupling ac losses between strands is minimized. Further analyses were conducted which allowed the various ac losses to be calculated for rectangular geometries. These are summarized by the formulae shown in Table 3.1-6.

It can be seen that for the case of coupling due to the perpendicular field component the ac power loss is proportional to the square of the length of the conductor face perpendicular to the perpendicular field component. It is also proportional to the square of the twist length of the strands. These observations indicate that the individual wires should be incorporated in such a way that they are transposed over as short a twist length as possible and that the dimension of the face perpendicular to the transverse field component should be as small as possible.

Examination of the formula for the ac loss due to the component of field parallel to the conductor shows that the loss is directly proportional to the area of cross section of the conductor and inversely proportional to the twist length. Thus this ac loss is unaffected by the aspect ratio of the conductor. However it should be noted that, in contrast to the perpendicular field coupling ac loss, this loss is reduced by increasing the twist length. Thus there is a conflict in design requirements, and there is some optimum twist length that minimizes the sum of these two losses.

It should be remembered that these coupling losses are between the individual strands of the group. They are in addition to the coupling losses between filaments within each strand.

3.1.5.3 Conductor Selection

For a given number of strands and if they must be transposed, the minimum thickness which can be achieved is two-strand diameters. This could be either in the form of a flat (non-tubular) braid or a flattened cable. Since

Table 3.1-6

Average Power Dissipation Over a Cycle Due to Various
Loss Mechanisms in Rectangular Conductors

Coupling Loss

Perpendicular Field

$$p_{av} = \frac{\Delta B^2 \ell_p^2}{64\rho} \cdot (b/a)^2 \left[1 + \frac{1}{(1 + b/a)} \right]^2 \cdot \frac{(\Delta t_1 + \Delta t_2)}{\Delta t_1 \Delta t_2 T} \cdot V_{comp}$$

Parallel Field

$$p_{av} = \frac{2\Delta B \lambda j_c}{\ell_p} \cdot \frac{ab}{T} \cdot V_{comp}$$

Eddy Loss

Perpendicular Field

$$p_{av} = \frac{4\Delta B^2}{\pi^2 \rho} \cdot b^2 \cdot \frac{(\Delta t_1 + \Delta t_2)}{\Delta t_1 \Delta t_2 T} \cdot V_{cu}$$

Parallel Field

$$p_{av} = \frac{2\Delta B^2}{\pi^2 \rho} \cdot b^2 \cdot \frac{(\Delta t_1 + \Delta t_2)}{\Delta t_1 \Delta t_2 T} \cdot V_{cu}$$

- p_{av} = power in torus averaged over a cycle (W)
- ΔB = change in vertical field (Wb/m²)
- ℓ_p = twist length (m)
- ρ = matrix resistivity (Ωm)
- λ = fraction of superconductor in composite
- j_c = critical current density in superconductor (A/m²)
- Δt_1 = charge time of applied field (s)
- Δt_2 = discharge time of applied field (s)
- T = cycle time (s)
- V_{comp} = volume of copper-superconductor composite in torus (m³)
- V_{cu} = volume of copper stabilizer in torus (m³)
- b = dimension of conductor face at which perpendicular field component is directed (m)
- a = dimension of conductor face parallel to the perpendicular field component (m)

cabling is cheaper than braiding, a fully transposed two-strand diameter thickness cable, commonly referred to as either a Rutherford cable or a Roebel transposed cable, was selected as the superconducting part of the 10 kA conductor. Seventeen strands is a standard number for a Rutherford cable and so this configuration was selected as the superconductor for the 8 T region of the coil. Seventeen strands leads to a strand diameter of 1.87 mm (.074 in.). This diameter is already less than the guideline of 2.54 mm (0.1 in.) indicated by the analysis previously described, but to further reduce the magnetization loss the number of filaments in each strand was increased from 180 to 2,046. This number of strands is within the state of the art, and conductor similar to that described above is routinely produced at MCA in large quantities. This number of filaments in such a wire diameter leads to a filament diameter of 24.7 μm .

3.1.5.4 Copper Stabilizer Configuration

A description of the reasoning behind the selection of the form of the copper stabilizer will now be given. As described previously, for full stabilization the correct combination of area of cross section of copper and surface area of copper is required to limit the surface heat flux to a certain value should all the current in the superconductor be transferred to the copper. A high surface to volume ratio is desirable for efficient cooling, and this can be achieved with a high aspect ratio slab of copper. However, as previously indicated, so as to minimize eddy current ac loss, such a slab must be oriented so that it presents its smallest face to the perpendicular component of magnetic field. The smaller the small dimension is, then the better the design is from the point of view both of reducing ac loss and of cooling. However, for practical manufacturing and handling considerations the aspect ratio of the copper cannot be too large. A slab

of copper with dimensions about 38.1 mm (1.5 in.) by 6.35 mm (0.25 in.) was taken as being a reasonable compromise between the conflicting requirements. Figure 3.1-9 schematically illustrates the evolution of the design from here. If the superconducting Rutherford cable were joined to the stabilizing copper slab as in Figure 3.1-9a, the eddy current circulation due to the parallel field would be as shown, and the characteristic conductor dimension for ac losses would be t . Since for a given cross-sectional area the ac loss is proportional to the square of this dimension, it clearly is advantageous to reduce it as much as possible. One way of reducing it is shown in Figure 3.1-9b. The stabilizing copper slab has been cut in two and the two halves placed on opposite sides of the superconducting Rutherford cable. Now over much of the conductor the eddy currents are constrained to flow in regions with a smaller characteristic distance. The situation can be still further improved by building the Rutherford cable around an insulator so that eddy currents cannot flow through it as shown in Figure 3.1-9c. In this figure the characteristic dimension over the majority of the conductor is half of that in Figure 3.1-9a. This results in only one quarter of the eddy current loss due to the parallel component of field when compared with placing all the conductor on one side. Because of the transverse load resulting from the large vertical field component, there are loads exerted on the stabilizer perpendicular to the long face. Because of this the space between the two slabs of copper stabilizer must be filled with an electrically insulating material capable of taking a bearing load.

The final version of the conductor is as shown in Figure 3.1-9d. In this version the solid stabilizing copper has been replaced with a compacted solder-filled Rutherford cable made from copper wire. The advantages of doing this are threefold. First, it is easier to wind; second, the result-

Figure 3.1-9

Schematic Illustrating Evolution of Design of Copper Stabilizer

ing conductor effectively has a low modulus; and third, the conductor would be available in much longer continuous lengths. These will be elaborated upon in the following paragraphs.

The height of the conductor is about 38.1 mm (1.5 in.), and the minimum bend radius in the windings is about 1.9 m (75 in.). If solid conductor were to be bent on edge around this radius, the resulting strain in the conductor would be about 1 percent. Copper yields at strains much lower than this and in yielding, work hardens. The work hardening results in a decrease in electrical conductivity. Thus in addition to the difficulty of winding on edge, the conductor would require subsequent annealing to regain the required electrical conductivity. This could be avoided by making the stabilizer in preformed and annealed sections. However, because of the non-circular coil shape, different conductor section shapes would be required for various position around the coil. Furthermore, these shapes would be different from one layer to the next; finally a large number of joints would be necessary. The concept is complicated and economically unattractive. Even if the solid copper stabilizer could be bent without reducing the electrical conductivity significantly, joints would have to be made more frequently than is desirable because of limitations on available lengths of commercially available copper sections.

The final reason for going to a non-solid copper stabilizer concerns the mechanical support of the forces. Because of the interaction of the toroidal field and the conductor current, the conductors experience a force perpendicular to the conductor in the plane of the coil. The turns elongate and at the same time bear against the steel support structure which also elongates. If relative movement between the conductor and the support structure is prevented, these elongations will be equal. It can be easily shown that under such conditions the tensile stresses in the conductor and

the support structure will be in direct proportion to their moduli of elasticity. The stress in the conductor must remain below the yield strength of the copper which is less than 10 kpsi. The Young's modulus for steel is about 50 percent greater than that of copper; therefore, if the stabilizer is solid, then the stress in the steel is limited to less than 15 kpsi. Since at cryogenic temperatures 310S stainless steel has a yield strength of about 110 kpsi, the steel would be under utilized if the modulus of elasticity of the conductor were determined by that of copper. One way of preventing this restriction is to make the copper in the form of a solder-filled cable. Under such conditions the effective modulus of elasticity of the cable is much smaller, and the stainless steel structure can take a much greater proportion of the load.

To summarize: cryogenic stability dictates a certain area of cross section of copper and surface area of conductor; ac losses dictate that the conductor should have a high aspect ratio and be oriented with the small face perpendicular to the field; and for structural reasons the components of the conductor should be made in the form of a Rutherford cable.

3.1.5.5 Conductor Grading

The physical properties of superconductor and copper are such that as magnetic field is increased superconductor critical density decreases and copper magneto-resistivity increases. Thus in the higher field regions of a coil larger cross sections of superconductor and stabilizer are required in order to meet critical current and stability criteria than are required in the lower field regions. This means that if the high field conductor were used throughout the coil then the coil would be overdesigned in the lower field regions; more copper and more niobium titanium would be included than would be necessary. This situation would be wasteful of space and

money. It also vastly increases the ac losses, magnetization losses being directly proportional to critical current density which increases with decreasing field and eddy current losses being inversely proportional to resistivity which decreases with decreasing field. To avoid this situation it is necessary to grade the conductor; i.e., to have several different conductor configurations, each configuration being sized for the maximum field it will see when placed in the coil.

Figure 3.1-10 shows various conductor distribution functions. This particular figure is for a torus with a circular cross section, but the distributions for a D-shaped torus would be very similar. Curve 1 shows the relative amount of material at each field as a function of the fraction of maximum field. This curve is normalized in such a way that the area below it is unity. It is immediately obvious that there is relatively more material in the lower field regions. Curve 2 is the integral of curve 1 and represents the fraction of material exposed to lower fields. It can be seen that 90 percent of the material sees a field less than 60 percent of the maximum field and that 80 percent of the material sees a field less than 40 percent of the maximum. Unfortunately, in order to grade according to this distribution there would have to be changes in conductor configuration along any given turn (the distribution functions reflect the fact that the field changes as any turn is followed around the coil). Such grading is impractical and the best that can be done economically is to follow curve 3 which shows the fraction of turns (or layers) exposed to lower fields as a function of maximum field.

A choice of three grades of conductor was decided on. Grades would be designed for maximum fields of 8.0, 5.3, and 2.7 T. This is a compromise between matching the conductor to the field distribution through the coil and minimizing the number of different conductors which must be manufactured.

Distribution of windings and turns as a function of magnetic field.

Figure 3.1-10

The two lower field grades would be made in the same way as the high field grade; i.e., by combining a superconducting Rutherford cable made of niobium titanium copper composite with two stabilizing Rutherford cables made of copper. The dimensions of the wire components of the cables would be the same for all three grades. The different areas of cross section of the composite and of the copper necessary for the different grades would be obtained by appropriately varying the number of these component wires. Figure 3.1-11 is a schematic showing the construction features and dimensions of the three grades, and Table 3.1-7 summarizes the characteristics of the three conductor grades.

3.1.5.6 AC and Nuclear Heating Effects on Stability

As was described above, the conductor configuration was selected primarily on the basis of minimizing the load on the refrigerator due to the generation of heat by ac losses. The total heat load due to ac losses generated in the conductor selected will be dealt with elsewhere in this report. This heat load is the average over a cycle. The other important aspect of ac loss is the impact on conductor stability due to the instantaneous heat load.

Because the rate of change of field is highest during the one-second charge time of the poloidal field coils, the most severe instantaneous heating occurs then. The magnitude of the ac loss per unit volume of conductor varies with position in the coil. This is because the properties which determine the ac losses; i.e., the matrix and stabilizer resistivities and the superconductor critical current density, are functions of magnetic field strength which is a function of position within the windings. The surface area per unit volume of conductor also varies throughout the windings, in this case in three discrete steps which correspond to the three

Figure 3.1-11

Various Conductor Grades for the 8 T Design

Table 3.1-7

8 T Magnet

Conductor Specifications

Operating Temperature 4.2 K

Characteristics of Component Strands

Material	Niobium titanium in copper matrix
Copper to Superconductor ratio	1.8:1
Diameter	.187 cm (73.6 mils)
Filament Diameter	24.7 μm

Characteristics of Stabilized Conductor

Type of Construction	Rectangular stranded cable conductor (Rutherford type) with central insulated divider stabilized with additional stranded copper
----------------------	---

Grade	1	2	3
Maximum Field, T	8.0	5.3	2.7
Operating Current, kA	10.0	10.0	10.0
Critical Current, kA	13.3	13.8	14.2
Number of Component Strands	17	10	6
Overall Copper to Superconductor Ratio	15.0	19.8	28.4
Conductor Current Density, kA/cm ²	3.74	4.64	5.79
Superconductor Current Density, kA/cm ²	60.0	102	170
Surface Heat Flux, W/cm ²	0.35	0.35	0.35
Overall Width, cm	.96	.96	.96
Overall Height, cm	3.69	2.99	2.42

grades of conductor. It is therefore not immediately obvious which point in the coil has the highest conductor surface heat flux. Therefore, calculations were performed at three positions around the coil. Position one corresponds to the peak field position of the coil, position three is diametrically opposite to position one, and position two is intermediate. At each position two calculations were performed for each conductor grade, one calculation for the highest field seen by that grade at that position and one for the lowest field. The ac loss formulae used were based on the formulae given in Tables 3.1-4 and 3.1-5. Property values corresponding to the local magnetic fields were used. A pulsed field of magnitude 0.5 T was assumed at all positions. Figure 3.1-12 summarizes the results of the analysis. It can be seen that for all points the surface heat flux is very small in comparison with the heat flux which would be produced should all the current flow in the stabilizer (about 0.35 W/cm^2). The smallest heat flux (which occurs at the peak field position) is about 0.011 W/cm^2 . The largest (which occurs in the zero field regions of the coil) is about 0.036 W/cm^2 . The conclusion to be drawn from this is that ac losses are not large enough to cause the conductor to go normal.

A second source of local heating which must be considered is that due to nuclear radiation. The worst position in the magnet from this point of view is that nearest the plasma; i.e., at the inner winding radius. The maximum intensity of radiation is $1 \times 10^{-6} \text{ W/g}$. Using parameters appropriate to the conductor in the high field region (grade 1), this translates into a surface heat flux of about $0.16 \mu\text{W/cm}^2$. This is trivial as far as the stability of the conductor is concerned.

Surface Heat Flux in various Regions of the Coil Due to AC Losses During the One-Second Charge Period of the Vertical Field Coils

3.1.6 Electrical Insulation

Electrical insulation between turns and layers in any superconducting magnet must be able to withstand high mechanical loads at cryogenic temperatures. If the conductor in the magnet is to be cryostatically stabilized, the insulation must at the same time be in such a form that helium can easily circulate through the windings. Finally, if the magnet is part of a fusion device with a tokamak geometry, the mechanical integrity of the insulation must be maintained over long periods in a high radiation environment with cyclic loading.

Polymers, reinforced or not, have long been used in superconducting magnets with satisfactory performance. Such materials combine good dielectric strength at cryogenic temperatures, good mechanical strength, and ease of manufacture. The widely used epoxy-fiberglass laminate G10 has a compressive strength of 345 MN/m^2 and an ultimate tensile strength of 262 MN/m^2 at room temperature. Another epoxy-fiberglass material (resin system Epon 828/A) is reported as having an ultimate tensile strength of 274 MN/m^2 at room temperature, 602 MN/m^2 at 20°K before irradiation, and 584 MN/m^2 after having absorbed 1.1×10^8 rads; i.e., it retained 97 percent of its original strength. Other structural laminates using different resin systems were reported as having their ultimate tensile strength increased.

The radiation absorption rate for this system is 1×10^7 rads/year, and the lifetime of the system is ten years, which leads to a lifetime dose rate of 1×10^8 rads. Therefore, it appears that an epoxy-glass laminate such as G10 would be very suitable for a radiation environment.

The largest bearing stress is that caused by the unbalanced radially inward force which appears in the inter-layer insulation. At the worst point in the windings the magnitude of this stress is about 38 MN/m^2 . Under

fault conditions should one coil be de-energized while the remaining 15 coils maintain full current, the worst sideways load would cause a stress in the inter-turn insulation less than 38 MN/m^2 quoted above. Therefore, under dc conditions G10 appears to be very suitable for this environment.

The largest cyclic loads (due to the pulsing of the vertical field coils) appear at the top and bottom of the coil where the toroidal field coil current is perpendicular to the field due to the poloidal coils. At this point the maximum stress in the inter-turn insulation is about 2.62 MN/m^2 . The magnetic field will be pulsed 3×10^6 cycles during the lifetime of the coil. For this number of cycles the stress limit for epoxy-glass composites is 310 MN/m^2 which is very much greater than the 2.62 MN/m^2 quoted above. Again, therefore, structurally G10 would be a suitable material.

The dielectric strength of G10 at room temperature is quoted in manufacturers' literature as being 500 V/mil. It will be arranged that during the fastest discharge of the torus the voltage across any coil will not exceed 1 kV. Thus in theory some tens of mils of G10 insulation should be more than adequate. In practice the more stringent requirement is set by the necessity to circulate liquid helium freely through the windings. Because of this, between turns there will be 3/16-inch of G10 and between layers 1/8-inch of G10 arranged in an open lattice similar to that shown in Figure 3.1-13.

Figure 3.1-13. Schematic Illustrating Method of Interlayer and Interturn Insulation

3.2 STRUCTURE

3.2.1 Introduction

This section presents the development of the structural EPR components with respect to the applied loadings and the essential considerations of materials, fabrication, and cost. The major components so designed are the cold toroidal field (TF) coil, intercoil structure, bucking cylinder, dewar, and gravity supports.

The factors of safety contained in the Work Statement have been used in this study; i.e., 2 to 3 on elastic behavior, 5 on buckling, and 10 to 100 cycles on fatigue. In addition, a factor of 1.25 on elastic behavior (or approximately 2.0 on ultimate tensile strength) has been used for the infrequent loads occurring under fault conditions.

The Maximum Shear-Stress Theory of Failure has been used for the static analyses. This theory assumes that elastic behavior ceases and yielding begins when the maximum shear stress in the general state becomes equal to the maximum shear stress at yield in the uniaxial state.

Due to the limited work scope, many detailed analyses and design studies were not performed. These include the iterative analyses for optimization of the cold coil structure and optimization of the room temperature vessel or coil/intercoil structure, as well as studies to determine the optimum fabrication procedure for the bucking cylinder. Gravity supports, location of the power leads, and liquid helium tower are preliminary since the location of EPR peripherals are not fixed.

Two design concepts have been evaluated for the 8 T machine, one a warm central cylinder configuration; the other, a cold central cylinder design. The warm central cylinder machine has a room temperature bucking cylinder, TF coils in individual dewars and warm intercoil structure (see

Figure 3.2-1). The cold central cylinder design has all major components cold within a common dewar (see Figure 3.2-2).

Each of these configurations has distinct advantages: the integral coil/dewar units of the warm central cylinder design facilitate the removal of coils for repair or replacement. Individual dewars also remove the necessity for the rapid shutdown of the EPR should a major vacuum leak occur and, in fact, allow continued operation, if desired. The warm central cylinder design minimizes the amount of refrigerated structure and allows eddy current losses to be dissipated at room temperature. Further, complete fabrication and testing of each unit is performed at the factory, not on site, at considerable monetary savings, enhanced reliability, and integrity.

The cold central cylinder design has the advantage of a shorter toroidal radius over the warm central cylinder. The absence of dewar walls between adjacent coils means that some 25.4 cm of additional space is available for conductor placement. The resulting coil cross section is a squat trapezoid, radially more compact than that of the warm central cylinder. This tends to reduce the major radius which in turn produces a more concentrated field with less conductor. Another advantage is that differential contraction problems between major components during cooldown do not arise.

Tables 3.2-1,2 contain a summary of overall mechanical characteristics of the major components for both warm and cold central cylinder designs. You will note that the warm central cylinder tabulation lists two configurations. The difference is that the original design uses heavy box beams as part of the room temperature vessel to connect the intercoil structures at the top and bottom of the machine for torsional equilibrium and to maintain the 90° access region between coils. To pare some weight, the warm central cylinder "alternate" was developed. The box beams were replaced by an

Fig. 3.2-1A 8 Tesla Warm Central Cylinder Design

Fig. 3.2-1B 8 Tesla Warm Central Cylinder Design

Fig. 3.2-2A 8 Tesla Warm Central
Cylinder Design

Fig. 3.2-2B 8 Tesla Cold Central Cylinder Design

Table 3.2-1

Summary of Mechanical Characteristics of Major Components:
Warm Central Cylinder Designs: Original and Alternate

I. Cold Coil Structure

A. Design Pressure

1. Internal (LHe containment): 0.69 MN/m² (100 psia)
2. External: vacuum

B. Weight Per Coil

1. Conductor: 0.651×10^5 kg (1.43×10^5 lbs)
 2. Structure: 1.825×10^5 kg (4.02×10^5 lbs)
- Total: 2.476×10^5 kg (5.45×10^5 lbs)

C. Material

1. Conductor: NbTi/Copper
2. Structure: 310S Stainless Steel

D. Overall Coil Size

1. Height: 14.15 m (557 inches)
2. Width: 10.39 m (409 inches)
3. Thickness: 1.14 m (45 inches)

II. Room Temperature Vessel (with radiation shield)

A. Design Pressure

1. Internal: vacuum
2. External: 0.10 MN/m² (14.7 psia)

B. Weight Per Coil

	<u>Original</u>	<u>Alternate</u>
1. Room Temp Vessel	1.123×10^5 kg (2.47×10^5 lbs)	0.570×10^5 kg (1.26×10^5 lbs)
2. Radiation Shield	0.037×10^5 kg (0.08×10^5 lbs)	0.037×10^5 kg (0.08×10^5 lbs)*
Total	1.160×10^5 kg (2.55×10^5 lbs)	0.607×10^5 kg (1.34×10^5 lbs)

C. Material

1. Room Temp Vessel: Nitronic 33 Stainless Steel
2. Radiation Shield: Aluminum
3. Low Conduction Spacers: G10

D. Overall Size

1. Height: 19.34 m (761 inches)
2. Width: 10.87 m (428 inches)
3. Thickness: 1.88 m (74 inches)

Table 3.2-1 (Concluded)

III. Intercoil Structure

A. Weight Per Coil	<u>Original</u>	<u>Alternate</u>
1. (2) Outer Panels	$0.117 \times 10^5 \text{ kg}$ ($0.25 \times 10^5 \text{ lbs}$)	$0.238 \times 10^5 \text{ kg}$ ($0.52 \times 10^5 \text{ lbs}$)
2. (2) Inner Panels	$0.084 \times 10^5 \text{ kg}$ ($0.19 \times 10^5 \text{ lbs}$)	$0.173 \times 10^5 \text{ kg}$ ($0.38 \times 10^5 \text{ lbs}$)
Total	$0.201 \times 10^5 \text{ kg}$ ($0.44 \times 10^5 \text{ lbs}$)	$0.411 \times 10^5 \text{ kg}$ ($0.90 \times 10^5 \text{ lbs}$)

B. Material: Nitronic 33 Stainless Steel

C. Overall Size

1. Outer Panel	<u>Original</u>	<u>Alternate</u>
Length	7.92 m (312 inches)	11.05 m (435 inches)
Width	3.28 m (129 inches)	3.81 m (150 inches)
Height	1.73 m (68 inches)	3.18 m (125 inches)
2. Inner Panel		
Length	6.17 m (243 inches)	8.70 m (342 inches)
Width	2.54 m (100 inches)	3.43 m (135 inches)
Height	1.47 m (58 inches)	2.73 m (108 inches)

IV. Bucking Cylinder

A. Weight: $2.268 \times 10^5 \text{ kg}$ ($5.00 \times 10^5 \text{ lbs}$)

B. Material: Inconel 706

C. Overall Size

1. Height: 7.28 m (287 inches)
2. Width: 4.95 m (195 inches)

V. Gravity Support - Cold Coil Structure

A. Weight Per Coil: $0.06 \times 10^5 \text{ kg}$ ($0.13 \times 10^5 \text{ lbs}$)

B. Material (Heim column)

1. Inner Cylinder: G10
2. Middle Cylinder: Aluminum
3. Outer Cylinder: G10
4. Hinge: Titanium

C. Overall Size

1. Height: 2.54 m (100 inches)
2. Width: 1.14 m (45 inches)

Table 3.2-2

Summary of Mechanical Characteristics of Major Components

Cold Central Cylinder Design

I. Cold Coil Structure

A. Design Pressure

1. Internal (LHe containment): 0.69 MN/m^2 (100 psia)
2. External: vacuum

B. Weight Per Coil

1. Conductor: $0.592 \times 10^5 \text{ kg}$ ($1.30 \times 10^5 \text{ lbs}$)
2. Structure: $1.995 \times 10^5 \text{ kg}$ ($4.40 \times 10^5 \text{ lbs}$)
- Total: $2.587 \times 10^5 \text{ kg}$ ($5.70 \times 10^5 \text{ lbs}$)

C. Material

1. Conductor: NbTi/Copper
2. Structure: 310S Stainless Steel

D. Overall Coil Size

1. Height: 13.56 m (534 inches)
2. Width: 9.86 m (388 inches)
3. Thickness: 1.84 m (72.5 inches)

II. Dewar

A. Design Pressure

1. Internal: vacuum
2. External: 0.10 MN/m^2 (14.7 psia)

B. Weight

1. Room Temperature Vessel: $7.838 \times 10^5 \text{ kg}$ ($17.26 \times 10^5 \text{ lbs}$)
2. Radiation Shield: $1.010 \times 10^5 \text{ kg}$ ($2.23 \times 10^5 \text{ lbs}$)
- Total: $8.848 \times 10^5 \text{ kg}$ ($19.49 \times 10^5 \text{ lbs}$)

C. Material

1. Room Temperature Vessel: Nitronic 33 Stainless Steel
2. Radiation Shield: Aluminum
3. Low Conduction Spacers: G10

D. Overall Size

1. Height: 18.94 m (746 inches)
2. Width: 26.42 m (1,040 inches)

Table 3.2-2 (Continued)

III. Intercoil Structure (Cold)

A. Weight Per Coil

1. (2) Outer Panels: 0.234×10^5 kg (0.52×10^5 lbs)
2. (2) Inner Panels: 0.127×10^5 kg (0.28×10^5 lbs)
- Total: 0.361×10^5 kg (0.80×10^5 lbs)

B. Material: 310S Stainless Steel

C. Overall Size

1. Outer Panel

Length: 10.67 m (420 inches)
Width: 3.71 m (146 inches)
Height: 3.38 m (133 inches)

2. Inner Panel

Length: 8.99 m (354 inches)
Width: 2.92 m (115 inches)
Height: 2.67 m (105 inches)

IV. Bucking Cylinder (Cold)

A. Weight: 2.939×10^5 kg (6.47×10^5 lbs)

B. Material: 310S Stainless Steel

C. Overall Size

1. Height: 7.28 m (287 inches)
2. Width: 5.31 m (209 inches)

V. Gravity Support - Cold Coil/Intercoil Panel Structure

A. Weight Per Coil: 0.07×10^5 kg (0.15×10^5 lbs)

B. Material (Heim Column)

1. Inner Cylinder: G10
2. Middle Cylinder: Aluminum
3. Outer Cylinder: G10
4. Hinge: Titanium

C. Overall Size

1. Height: 2.54 m (100 inches)
2. Width: 1.27 m (50 inches)

VI. Gravity Support - Cold Bucking Cylinder

A. Weights

1. Truncated Cone: 0.127×10^5 kg (0.28×10^5 lbs)
2. Heim Columns: 0.07×10^5 kg (0.16×10^5 lbs)

B. Material

1. Truncated Cone: 310S Stainless Steel
2. Heim Columns: See V.B.

Table 3.2-2 (Concluded)

C. Overall Size

1. Truncated Cone

Height: 1.78 m (70 inches)
Width: 5.84 m (230 inches)

2. Columns (4)

Height: 2.54 m (100 inches)
Width: 0.69 m (27 inches)

intercoil shell structure extended to cover all space between coils except for large elliptical access ports. In all other respects, both warm central cylinder designs are the same.

3.2.1.1 Materials Selection

Materials selection for use at cryogenic temperature has been made on the basis of prior application in major cryogenic structures and/or adequacy of test data, demonstrated metallurgical stability under repeated cooling or plastic deformation, availability, yield strength, fabrication requirements and costs. The initial candidate materials were 310S, 21-6-9, A286, Kromarc 58 stainless steels, and Inconels X750, 706 and 718, which had been investigated as cold structures for other applications.⁷ Aluminum has not been considered for several reasons: its lower electrical resistivity would give rise to higher eddy current losses, high strength forms are not readily weldable, its lower elastic modulus limits its use in compression members, and its thermal expansion coefficient would generate differential thermal contraction problems.

Because 21-6-9 cryogenic test data indicates that this material undergoes an austenitic transformation to martensite at temperatures above 4 K, it was not considered further as a structural material at this time.

Kromarc 58 was also not considered further because it is not generally commercially available.

Of the remaining materials, Inconel 706 has been selected for the warm bucking cylinder; 310S, for the cold bucking cylinder. The impact of yield strength, fabrication, and cost will be discussed later. The cold coil structure will also be fabricated from 310S.

The selection of steels for the room temperature vessel and support structure is largely a measure of yield strengths, fabrication, and cost.

To this end, Nitronic 33 (18-3) stainless steel will be used for the room temperature vacuum vessel, intercoil shear panels, and gravity supports. Similar to 304L in fabrication requirements, Nitronic 33 has a 70 percent higher yield strength of 448 MN/m^2 (65 ksi) with only a 2 percent increase in cost, and it is readily available in plate form.

3.2.1.2 Loadings and Analysis Methods

Several different types of loadings must be considered in the design of a superconducting magnet. These include: gravity loads, seismic loads, thermal loads, thermally induced loads, loads by electrical transients, and magnetic loads experienced in operation. The magnetic loads are by far the greatest and are discussed in this section in detail. The remaining loads, which must be taken into consideration ultimately, are considered beyond the scope of this program and would be dealt with in depth during a detailed design phase. The overall characteristics of the preliminary design as presented in this report should not change to any large extent as a result of the consideration of these other loads.

The total force on a section of current carrying conductor(s)--for example, a portion of a magnet winding--may be found by integrating the Lorentz force over the volume of the section of interest. To obtain an analytical result in closed mathematical form, it must be possible to perform the following operations in the region:

1. Express $\vec{J}$ mathematically
2. Express $\vec{B}$ mathematically
3. Integrate $\vec{J} \times \vec{B}$

The procedure outlined by steps 1, 2, and 3 is tractable only in situations with particularly simple geometry. In most cases, one or more of these steps must be replaced by an approximating procedure.

For the EPR, all of the above steps were carried out numerically. The basis for the computation is that it is possible to express in closed form the magnetic field generated by a straight current filament of finite length. The above procedure is then replaced by:

1. Represent the entire current carrying winding by a model composed of current carrying "sticks" of finite length.
2. Select the coordinates of points at which the magnetic field is to be determined and calculate the field at each of these points by a vectorial sum of the contributions from each current "stick."
3. Determine the local components of the force per unit volume at a given point by forming the vector cross product of the current carried by the stick and total magnetic field calculated at that point and dividing by the winding cross-sectional area which the stick represents.
4. Find the total force in a selected direction on a section of winding by summing the proper components of the forces per unit volume multiplied by the incremental volumes they model over the section.

The current filament model used for force computation in this design study is discussed later under the toroidal field coil description.

3.2.2 Toroidal Field Coil

3.2.2.1 Coil Shape

The pure tension "D" shaped coil has been selected for this design study because it appears to be easier to fabricate and lateral loads in the central region are easier to support. Specifically, the existence of the cyclic out-of-plane forces around the entire perimeter of the coil coupled with large out-of-plane loads that result under fault conditions favor the use of a straight section that is easy to support and fabricate. The

fabrication and fitting of the intercoil structure favors the D-shaped coil when consideration is given to the automated replacement of a coil. Another consideration is simplicity of the bucking cylinder which must conform to the coil form in order to carry the large radial forces; this also favors the D shape.

3.2.2.2 Warm Central Cylinder Design Description

The design philosophy for the warm central cylinder design is to have an integrated cold coil/warm support structure, where the warm structure comprises the room temperature vessel and intercoil structure. The cold coil structure would carry essentially only in-plane loads occurring during normal conditions; the cyclic out-of-plane loads would be reacted by the warm support structure, as would be excessive loads induced during fault or test conditions. In this way, cold structure weight can be minimized with attendant savings in cold eddy current losses and fabrication costs. A detailed description of the coil and room temperature vessel interaction required for this approach is presented in the room temperature vessel description.

The coil cross section is fabricated from large structural plates to both facilitate winding of the conductor and avoid extensive machining operations on the coil structure itself. Structural support members are placed within the winding to prevent load accumulation and crushing of the conductor and insulation under both in-plane and out-of-plane forces. These members are sized to properly collect and transmit the magnetic forces to the center spine and side panels. For this study, the cross member thicknesses have been optimized to the extent that they reflect the reduction in in-plane forces going away from the straight leg of the D.

The warm central cylinder coil cross sections are shown in Figure 3.2-1b. The straight leg (or "nose") section C-C has a uniform trapezoidal shape; the other sections away from the nose have sidewalls constructed of bolted plate segments. This departure from a single plate is necessitated by fabrication considerations; i.e., rolling a 3-inch thick plate 50 inches wide into the shape of a truncated cone with continuously changing angle is complicated and expensive if close tolerances must be maintained. Thus, away from the nose, side panels are fabricated from bolted, flat plate cutouts. The plate segments are assembled in an interlocking fashion so that forces are transmitted by one member pressing upon another without relying upon the bolts.

Hardened structural bolts are employed throughout this machine, and with few exceptions, the 'locking cap method' is used to prevent their loosening. This method requires a metal ring to be placed around the head of the tightened socket head bolt. A special tool is then used to swage the ring into machined recesses in both the bolt head and the counterbore wall. This procedure assures a positive metal-to-metal lock, a system preferred over the use of organic locking compounds in high radiation environments. Although not specifically mentioned elsewhere, this is the type of locking device to be used for all bolts in the EPR designs.

3.2.2.3 Cold Central Cylinder Design Description

In this design, the cold coil structure, together with a cold intercoil structure, carries all loads, normal or otherwise. While coil construction is generally the same, stiffeners have been added along the coil side panels to collect the out-of-plane loads and transmit them to the flanged top and bottom plates. Cross-sectional views of this TF coil configuration are shown in Figure 3.2-2b. A comparison with the warm central cylinder cross

sections in Figure 3.2-1b shows the cold central cylinder coil sections to be much more compact in the in-plane direction. This is the result of relocating conductor into the space occupied by dewar walls in the warm central cylinder design. While there is a loss of stiffness against in-plane bending in the cold central cylinder coil structure because of this, the rigidly attached intercoil shell structure effectively restores it.

3.2.2.4 Analysis

Structural analysis of D-shaped TF coils has been performed for normal, faulted, and test conditions, where normal comprises both steady state and cyclic modes. The normal condition with equal current in all 16 TF coils gives rise to 1) a constant tension around the D due to the cumulative body forces in the coil filaments, 2) an unbalanced radially inward force occurring along the straight nose of the D, and 3) a cyclic out-of-plane overturning moment caused by interaction with the pulsed poloidal field coils. This last load is assumed to result from the uniform vertical field of 0.4 T. Fault condition loads arise from fractional current in a single coil while the other 15 coils see full normal current. Variation of the fractional current from negative to positive critical provides all possible loading cases in a coil.

Appendix 1 contains a general analysis of the tension and bending moments in a toroidal coil system. One conclusion is that it is possible to design a tension or minimum bending structure only for a specific loading mode. If the system is then operated with other loading modes such as a single coil partially energized (as may occur during a quench) or energizing of a single coil by itself for test purposes during checkout testing, then in-plane bending is introduced. It is also shown in the Appendix that for a system design for no bending when all coils are operated

in series the worst loading condition as far as bending moment is concerned occurs if one coil is separately energized to full current with the other coils deenergized. Further, it is shown that the bending moment resulting from a quench in a toroidal array is equal to one-fourth the magnitude of the bending moment resulting from the coil energized by itself. A correction for use in the moment computations when the original coil shape is not free of bending is also presented.

Forces in each coil during normal and faulted operation and during test have been determined using the MCA STICK computer program. This code package accepts magnet structures modeled with cross sections containing a single current-carrying filament or a rectangular pattern of these filaments. A curvilinear "D" shaped coil is modeled by approximating the coil centerline with a series of straight segments (see Figure 3.2-3). Basic program output consists of the forces acting on each segment. These forces are then manipulated by means of post processing routines for the necessary summations. Figures 3.2-4 and 3.2-6 present the forces used in the coil analyses.

Observing symmetry, the upper half of the 16-coil toroidal field assembly was modeled in the STICK program. The resulting in-plane forces were sequentially applied to a planar finite element model of a single coil and forces, moments, and deflections thus obtained for normal and fault conditions. A planar model of the coil under test conditions was also run. Sizing of the coil structural members has been performed, keeping in mind the factors of safety on stress, buckling, and fatigue cited in the Work Statement.

310S stainless steel has been selected for use in the cold coil structure. This material is preferred for its good low temperature toughness properties, higher cold yield strength to cost ratio than other candidate materials and prior use in cryogenic structural applications.

Figure 3.2-3

Stick Model of D-Shaped TF Coil

a) Steady State In-Plane Loads

b) Pulsed Out-of-Plane Loads

Figure 3.2-4

8 T Magnetic Loads: Normal Condition

Figure 3.2-5

8 T Magnetic Loads: Fault Condition
(loads shown on coil adjacent to faulted coil with zero current)

Figure 3.2-6

3 T Magnetic Loads: Test Condition (Self-Energized)

3.2.3 Room Temperature Vessel (Warm Central Cylinder Design only)

3.2.3.1 Introduction

The room temperature (RT) vessel surrounds the entire cold coil structure in the warm central cylinder design. It contains the radiation shield and G10 blocks, both fixed and adjustable, the latter to maintain geometric relationships between the vessel and coil. Structurally, the room temperature vessel is required to transmit compressive nose loads to the bucking cylinder, out-of-plane loads to the intercoil structure, and to provide additional in-plane restraint for the coil under fault and test conditions. A reinforced service manifold in the top of the vessel provides coil access for the vapor-cooled power leads, LHe reservoir, LHe fill and vent lines, and safety relief system.

3.2.3.2 Analysis--Normal Condition

Radial compressive loads pose no particular problem as the vessel is loaded uniformly along the straight leg of the D and is supported by the bucking cylinder over that length.

The cyclic out-of-plane loads, however, create large overturning moments in each coil, antisymmetric top to bottom. The cumulative result is a large torsional moment acting about the vertical axis of the EPR machine, the top half tending to go counterclockwise; the lower half, clockwise, as viewed from above.

The transmission of this torque from top to bottom of the toroid assembly can be through the inside region-central bucking cylinder or through a structure going from top to bottom on the outside. Either one or a combination of both methods can be used.

Because the amount of material required to transmit the torque decreases with increasing radius and because torque transmittal at the larger radii

leads to stiffer torque structure (lower deflections), it was decided to transmit the torque in the outer section of the coils by using shear panels. These shear panels, located between the coils and the individual room temperature vessels, have been sized to react this torsional shear and maintain vertical coil alignment. Because of servicing and plasma access requirements, top and bottom shear panels are terminated at $\pm 45^\circ$ to the horizontal plane measured from the centroid of the plasma. The necessary force equilibrium connection between upper and lower panels is maintained by use of heavy box beams. These members, made from 12.7 and 20.3 cm (5- and 8-inch) thick steel plates, replace the nominal 3.8 and 5.1 cm ($1\frac{1}{4}$ - and 2-inch) thick room temperature vessel walls over the 90° access region. Sizing of these members at this time has been limited to shear/bending stress and buckling considerations. A more detailed design analysis would examine overall stiffness and deflection as well by means of a three-dimensional finite element analysis comprising the coils and entire support structure; i.e., bucking cylinder, shear panels, room temperature vessels, and gravity supports.

3.2.3.3 Analysis--Fault Condition

To design the room temperature vessel to provide additional in-plane support for the coil under fault and test conditions, it is necessary to identify coil displacements associated with each loading. A general description of coil behavior in a warm central cylinder is stated here and is shown pictorially in Figure 3.2-7.

A TF coil at room temperature in its as-built, or initial, condition contracts uniformly with cooldown. Assuming the coil nose is held tight against the front of the room temperature vessel and the vertical alignment of the coil centroid is held constant by use of properly designed Heim

columns,* the net displacements appear as shown in Figure 3.2-7. The dewar design--radiation shield, insulating and adjustment spacers, and room temperature vessel--does not impede this contraction process. For the 8 T coil, the total horizontal displacement is on the order of 2.5 cm (1.0 in.); the vertical displacement, 3.6 cm (1.4 in.).

When self-energized for test purposes, the coil attempts to go circular in shape. If unrestricted, the coil will deform until force equilibrium is attained within the coil structure. As mentioned earlier, this condition may generate in-plane bending moments four times larger than the maximum moments developed during a fault condition. The design of the room temperature vessel should, therefore, provide coil restraint during self-energization. To do this, moveable spacers are located on all faces of the stiffened room temperature vessel walls and are adjusted as described below.

Normally loaded in a toroidal array, each TF coil undergoes an inward radial translation as a function of the combined stiffness of the bucking cylinder, room temperature vessel, and intercoil structure. For the warm central cylinder design, this radial displacement will be less than 0.13 cm (0.050 in.). The straight leg or nose of the coil sees this while all other sections of the D, experiencing a radially outward em force, displace upward and/or outward in the plane of the coil. Figure 3.2-7c shows the overall coil deformation relative to the original room temperature form; Figure 3.2-8 presents a closer look at the coil displacement under normal loading.

It follows that with the dimensional profiles of the coil known for each of these cases, adjustable insulation/alignment blocks can be installed inside the room temperature vessel and set at initial values for safe transport of the coil RT vessel unit and for unrestricted in-plane movement of

* discussed in the section on gravity supports.

Figure 3.2-7. TF Coil Displacement Modes: Warm Central Cylinder Design

Figure 3.2-8

TF Coil Displacement Profile, Normal Loading

the coil relative to the vessel during cooldown. As cooldown progresses, the spring-loaded adjustment blocks located on the upper and lower thirds of the straight leg segment (see Figure 3.2-1a) press the cold coil structure radially inward until the nose is tight against the RT vessel (and, hence, the bucking cylinder). The blocks spaced along the exterior of the RT vessel are now positioned to the outermost displacement of the coil under normal loading. An in-plane displacement window is thus created such that the G10 blocks on the interior surface of the coil will contact the interior RT vessel wall only when unloaded and cold. The blocks on the exterior coil surface will compress against the exterior RT vessel wall only under abnormal loadings. As a result of these adjustments, abnormal displacements of the coil occurring during test and fault conditions will be resisted by the room temperature vessel as well as by the cold coil structure. Structural stiffeners have been added to the room temperature vessel plates to carry these additional loads.

While in-plane motion is unrestricted for normal operation, out-of-plane movement must be minimal; transverse coil motion will give rise to cyclic strain-controlled loading of the coil structure. Hence, provision has been made for restraining out-of-plane motion by means of spring-loaded insulation/alignment blocks spaced along the sides of the room temperature vessel. The effect of these blocks is to permit in-plane coil motion while effectively clamping the coil within the room temperature vessel in the out-of-plane direction. The coil is both protected during transportation and held securely in factory alignment.

The locations of the out-of-plane alignment blocks have been optimized for the cyclic normal operating loadings. As depicted in Figure 3.2-1a, alignment blocks are located on one vertical side of the room temperature

vessel while hand-fitted shims are positioned between the coil and RT vessel wall on the opposing side. These shims are carefully set in the factory to ensure uniform fit and, hence, proper out-of-plane force transferral from coil to RT vessel. The spring-loaded blocks are designed to hold the coil tight against the shimmed surface. Under normal operating conditions, the directions of the cyclic out-of-plane loads acting on each coil are known. Assume the current flow is upward along the straight leg of the D. On the upper half of the coil, the forces act towards the right; on the lower half, towards the left--as viewed from the outer D section towards the straight leg. Good design practice suggests that the spring-loaded blocks be located on the unloaded side of the coil to minimize fatigue problems; and the fixed shims, on the loaded side. Hence, the alignment blocks appear along the left side of the room temperature vessel on the upper half and along the right side on the lower half. Although primarily carrying low level compressive loads, the alignment blocks will be sized to carry maximum fault loads as well.

The narrow fabrication and alignment space between RT vessels will be filled so that the compressive forces arising during a fault condition along the straight leg are properly transmitted around the bucking cylinder.

For purposes of comparison, Figure 3.2-9, showing TF coil deflection modes for the cold central cylinder design, has been included. The basic difference is that larger radial displacements occur in the cold central cylinder design since the bucking cylinder now contracts with cooldown as well.

3.2.4 Bucking Cylinder

Central to the EPR structure is the bucking cylinder. Its sole purpose is to react the large radial compressive loads of the coils during normal or fault operation of the machine.

Figure 3.2-9. TF Coil Deflection Modes: Cold Central Cylinder Design

The cylinder has been sized as a right circular cylinder under uniform external pressure for normal loading (see Figure 3.2-10a). A central free space having a 2.15 m (84.65 in.) radius is required for the poloidal coils. This dimension was taken to be the inside radius of the warm bucking cylinder. The cold central cylinder concept requires additional central space for a dewar of about 12.7 cm (5 in.) on the radius, yielding a minimum inside radius for the bucking cylinder of 2.28 m (89.65 in.).

To compare the candidate materials, base prices for heat lot quantities were obtained in units of dollars per pound of material. Relative costs of a 7.315 m (24-foot) bucking cylinder were then computed. Total (base price) costs of cylinders made from six candidate materials in both warm and cold configurations are presented in Table 3.2-3.

Fault load analysis of the bucking cylinder is somewhat more involved. Under normal conditions, the mutual inductive coupling between TF coils creates an equal force in each coil directed radially inward. If one coil discharges, the symmetry is upset. Coils on either side of the faulted coil no longer have the out-of-plane attraction on the one side and tend to collapse toward the next set of coils. If the intercoil structure were not present, the coils would fall away from the faulted coil much like dominoes. However, by designing the intercoil structure to withstand compression (and buckling) as well as shear, the active TF coils will maintain their positions and permit the safe, continued operation or shutdown of the machine.

The fault load analysis considered for this study was the failure of a single coil in the toroidal array. The effective electromagnetic forces imposed on the remaining active coils as a result of this failure were determined from the STICK filament model of the torus. These out-of-plane forces were superimposed on a free body diagram of the active coils and

a) Normal Condition (R_i equal)

b) Fault Condition (R_i equal, OOP_i unequal)

Figure 3.2-10

Bucking Cylinder Loading Under Normal and Fault Conditions

Table 3.2-3

Bucking Cylinder Material Comparison - Normal Condition

8 T Design

WARM CENTRAL CYLINDER

Material	σ_y (ksi)	$[0.5 \times \sigma_y]$ c_{allow} (ksi)	(in) R_i	Wall Thickness	(in) R_o	(in ²) Area	Height=24 ft (x10 ⁶ in ³) Volume	(lb/in ³) ρ	(x10 ⁶ lbs) Weight	Heat Lot Base Price (\$/lb) Cost/lb	(x10 ⁶ \$) Total Cost [†]
310S	34.0	17	84.65	41.13	125.78	27,189	7.8305	.29	2.2708	1.50	3.4063
21-6-9	56.0	28	↓	18.93	103.58	11,196	3.2243	.283	0.9125	1.25	1.1406
A286	115.0	57.5	↓	7.81	92.46	4,343	1.2509	.286	0.3578	3.40	1.2164
X750	120.0	60	↓	7.44	92.09	4,129	1.1892	.298	0.3544	4.30	1.5239
706	145.0	72.5	↓	6.01	90.66	3,313	0.9540	.292	0.2786	4.10	1.1422
718	155.0	77.5	↓	5.59	90.24	3,070	0.8841	.297	0.2626	4.60	1.2078

COLD CENTRAL CYLINDER

310S	110	55	89.65*	8.70	98.35	5,133	1.4798	.29	0.4291	1.50	0.6437
21-6-9	185	92.5	↓	4.88	94.53	2,822	0.8128	.283	0.2300	1.25	0.2875
A286	140	70	↓	6.623	96.27	3,869	1.1141	.286	0.3186	3.40	1.0834
X750	130	65	↓	7.20	96.85	4,216	1.2141	.298	0.3618	4.30	1.5558
706	170	85	↓	5.35	95.00	3,102	0.8934	.292	0.2609	4.10	1.0695
718	190	95	↓	4.74	94.39	2,740	0.7891	.297	0.2344	4.60	1.0781

[†] excludes additional costs for size, heat treating, etc.

* includes 5 inches for central dewar

their intercoil structures. For simplicity, joints between the coils and intercoil structure were assumed pin-connected; only radial reactions were allowed at the point of coil/cylinder contact. A sketch of this arrangement is shown in Figure 3.2-10b. Using the equations of static equilibrium, the net radial loads on the bucking cylinder, F_i , were determined. A free body diagram of the bucking cylinder itself was then drawn. Noting the loading symmetry, the cylinder was considered as a semicircular arch with seven equally spaced radial loads. The boundary conditions are a horizontally guided end at the position of the faulted coil and a fixed condition at the other end, 180° opposed. This problem is redundant to the second degree and was solved by application of standard curved beam flexure formulas.

The results of this analysis indicated that the maximum moment occurs at the position of the faulted coil. To better understand this problem, a confirmatory finite element analysis was run. The resulting exaggerated displacement plot is presented in Figure 3.2-11. A comparison of the solid line (displaced) geometry to the dashed line (original) form shows the cylinder becoming egg-shaped, with the small end (and maximum moment) at the faulted coil location.

Candidate cylinder materials have been sized using an allowable stress of 80 percent yield (roughly 55 percent of the ultimate tensile strength). Their relative cost appears in Table 3.2-4 for both warm and cold central cylinder concepts. It appears that the fault condition controls the cylinder design for all feasible wall thicknesses. This is borne out by the graph of Figure 3.2-12 which compares the normal and fault cylinder thicknesses as a function of allowable stress.

On the basis of these sizing analyses, the need to minimize cylinder wall thickness for fabrication purposes and cost, Inconel 706 has been

Figure 3.2-11

Bucking Cylinder Finite Element Displacement Plot: Fault Condition

Table 3.2-4

Bucking Cylinder Material Comparison - Fault Condition
8 T Design

WARM CENTRAL CYLINDER

Material	σ_y (ksi)	$(0.8 \times \sigma_y)$ σ_{allow} (ksi)	(in) R_i	Wall Thickness	(in) R_o	(in ²) Area	Height=24 ft (x10 ⁶ in ³) Volume	(lb/in ³) ρ	(x10 ⁶ lbs) Weight	Heat Lot Base Price (\$/lb) Cost/lb	(x10 ⁶ \$) Total Cost [†]
310S	34.0	27.2	84.65	26.04	110.7	15,980	4.6023	.29	1.3347	1.50	2.0020
21-6-9	56.0	44.8	↓	18.85	103.5	11,142	3.2089	.283	0.9081	1.25	1.1351
A286	115.0	92.0	↓	12.22	96.9	6,969	2.0070	.286	0.5740	3.40	1.9516
X750	120.0	96.0	↓	11.92	96.6	6,786	1.9545	.298	0.5824	4.30	2.5044
706	145.0	116.0	↓	10.68	95.3	6,039	1.7392	.292	0.5078	4.10	2.0821
718	155.0	124.0	↓	10.28	94.9	5,800	1.6703	.297	0.4961	4.60	2.2820

COLD CENTRAL CYLINDER

310S	110	88	89.65*	12.84	102.5	7,751	2.2322	.29	0.6473	1.50	0.9710
21-6-9	185	148	↓	9.52	99.2	5,648	1.6268	.283	0.4504	1.25	0.5755
A286	140	112	↓	11.16	100.8	6,680	1.9239	.286	0.5502	3.40	1.8708
X750	130	104	↓	11.65	101.3	6,989	2.0127	.298	0.5998	4.30	2.5791
706	170	136	↓	9.99	99.6	5,941	1.7109	.292	0.4996	4.10	2.0483
718	190	152	↓	9.38	99.0	5,560	1.6013	.297	0.4756	4.60	2.1877

[†] excludes additional costs for size, heat treating, etc.

* includes 5 inches for central dewar

Figure 3.2-12
Bucking Cylinder Sizing

selected for the warm central cylinder design and 310S for cold central cylinder design. Both materials will keep the cylinder wall under .33 cm (13 in.).

The task of performing a buckling analysis of the cylinder under fault conditions with attention given to the method of cylinder assembly is beyond the scope of this study but will be performed during a more detailed design phase.

3.2.5 Intercoil Structure

The purpose of the intercoil structure is to provide rigid restraint for the toroidal field coils against 1) the cyclic overturning moment induced by the pulsed operation of the poloidal field coils and 2) the large compressive forces developed between the TF coils during fault conditions. The former loads are resisted in shear; the latter, in compression. The intercoil structure has been sized to safely carry these loads without yielding or buckling.

Each intercoil structure (reference Figure 3.2-1) consists of four 5.1 cm (2 in.) thick steel plates, rolled to match either the inside or outside surface of the room temperature vessel, cut to fit between the flanges of adjacent vessels and reinforced with steel stiffeners. These plates extend from the point of divergence of the room temperature vessels (near the bucking cylinder) to the start of the heavy box beam section of the room temperature vessels. The plates are symmetrically located, top and bottom, between adjacent coils. In the warm central cylinder design, these plates cover the intercoil space completely except for the reinforced access area cutouts.

To facilitate assembly and disassembly, the intercoil panels are designed to be bolted to overhanging flanges on the room temperature vessels.

Preformed strips of insulating composite provide the final surface matchup as well as maintain separation between the metal plates for minimum eddy current losses; the hardened shoulder bolts carrying the shear loads are also insulated, but with a higher strength material. These bolts are captive and spring-loaded to facilitate remote assembly/disassembly operations. Tapered pins are also provided to ensure correct shear panel-flange alignment before bolt up.

In the warm central cylinder design the intercoil structure is at room temperature and will be fabricated from Nitronic 33 plate, using Nitronic 35W filler wire for welds of high strength comparable to the base metal.

The intercoil structure of the cold central cylinder design is essentially the same as that for the warm central cylinder alternate; i.e., the intercoil space is entirely covered by shear/compression panels except for the access cutouts. The basic differences are 1) that the intercoil panels now clamp to flanges on the coils themselves and 2) being at 4 K, the panels are fabricated from 310S stainless steel.

3.2.6 Gravity Supports

Required for this study are gravity support systems for the bucking cylinder, coil structure, and room temperature vessel/intercoil structure. The purpose of these supports is to maintain proper alignment of the EPR components under all loading conditions with minimum heat leakage from the room temperature structure.

It is important to keep in mind that these support designs do not reflect the presence or placement of the several horizontal coils and other peripheral equipment essential to EPR operation, nor do they consider the concrete foundation as other than a flat slab. Such concerns are important but beyond the scope of this study.

The gravity support structures for the warm bore design are described here and are shown in Figure 3.2-13. Supporting the room temperature bucking cylinder is a circular array of steel columns. A truss arrangement of smaller steel members between adjacent columns and a series of I-beam ring stiffeners provide the necessary rigidity to preclude buckling.

The room temperature vessel supports are designed to carry the full weight of the vessel itself and its share of intercoil structure weight. Sufficient space is provided within these support members for the independent movement of the cold coil supports. Access panels are also provided should readjustments be necessary after the coils have been transported to the site.

The coil support system must transmit the gravitational load to the foundation while allowing unrestricted horizontal motion for relative thermal contraction between the coil and the RT vessel during cooldown. Heat losses must also be minimized. For a given structural configuration, the heat load is directly proportional to the cross-sectional area of the support and inversely proportional to its length. It is desirable, therefore, to use long, slender members to support the gravitational loads. If these members are to be used in tension, the design is relatively simple and an efficient structure is obtained. If the loads must be borne in compression, the buckling load of the support often becomes a limiting factor before the material is loaded to its design stress level.

The column support system chosen for this application consists of several nested cylinders of G10 and aluminum materials, a system discussed and analyzed in reference 8. This type of support has been successfully tested and incorporated in the large superconducting magnets. As Figure 3.2-13 shows, this support consists of three or more concentric cylinders. In general use, materials are selected such that the thermal contraction of

**3 MEMBER
LOW CONDUCTION
HEAT LEAK SUPPORT**

**5 MEMBER
LOW CONDUCTION
HEAT LEAK SUPPORT**

Figure 3.2-13

Illustration of Low Heat Leak Supports Consisting of Three and Five Members

the intermediate tube (for a three-column design) is equal to the sum of the contractions of the inner and outer tubes so that the overall height will not change when the column is cooled to operating conditions. For our purposes, the material and tube lengths will be selected so that the net thermal contraction results in an increase in column height equal to the known vertical contraction of the lower half of the coil structure. Consequently, all alignments made at room temperature during final factory assembly will be maintained when the magnet system is cooled down. For a three-tube configuration, the middle tube can be made from aluminum; the outer and inner tubes, from G10. This results in a low heat leak, temperature-stabilized column support, sometimes known as a Heim column.

Two such columns support each coil. Both columns are atop low friction pads to accommodate their respective inward radial motions during cooldown yet provide a stable footing. Hinges are provided at the top and bottom of the outer support to accommodate local coil deformation under load and thus minimize lateral loading of the support. The hinges are fabricated from titanium to take advantage of its ability to sustain large deflections without yielding.

The gravity support structures for the cold central cylinder design are shown in Figure 3.2-2a. Since the bucking cylinder is now cold, it too must be supported with low heat leak columns. To this end, four Heim columns will be used for the 8 T cylinder. Four columns have advantages over three in that symmetry is maintained among the 16-coil torus, thereby yielding identical lower dewar sections and minimizing fabrications costs. Further, the four-column design permits each column to be smaller in diameter and thus fit in the restricted space between central dewar shell and cold coil supports. Transmitting the bucking cylinder load to the offset

centerline of these support columns is a flanged truncated cone. This member provides a wider, more stable stance for the cylinder support system.

The cold coils are now integrally connected to the intercoil structure. The supporting Heim columns are designed to carry this increased load. Otherwise, their function is much the same as in the warm central cylinder design.

In the cold central cylinder design, the room temperature vessel does not carry magnetic loads and is a simple dewar. The structure is thus at room temperature and is essentially self supported. Refer to the section on dewar topology for more detailed information.

3.3 COIL PROTECTION

Under usual operation no protection is needed for the coil system; it is only in the case of failure or not normal operation that coil protection is required. The primary events that need to be considered in the design of a protection system are propagation of a resistive region within the windings (quench), electrical short circuits internal to the windings or from the windings to ground, large vacuum leaks, and failure of the cryogenic system to supply coolant.

The primary reasons for protection are the large magnetic energy stored in the system and the large amount of helium at 4.2 K. In the event of a malfunction, it is necessary to be able to have a protection scheme and procedures which allow controlled discharge of the magnetic energy as well as controlled removal of helium from the magnet system.

3.3.1 Quench

A self propagating resistive region within the windings can be initiated by several events: lack of coolant locally, interturn or turn to ground electrical short, large local heat input (due to any cause), etc. While the conductor and the helium supply are designed to prevent local resistive regions from propagating, we must postulate a large enough effect to overcome the basic stability of a cryostatically stabilized conductor.

The self propagating resistive region grows during the quench resulting in heating of the windings, potentially high voltages across the resistive region, and high rate of heating in the helium coolant. The severity of the quench and resultant damage depends on the maximum temperature in the windings after the current has been reduced to zero, the voltages that occurred during the quench, and the internal pressures generated due to heating the helium. In addition, the loading conditions which exist during

a quench must be examined to ensure that structures are adequate to take the load.

To estimate how rapidly the current must be reduced to zero in a quench, Figure 3.3-1 shows the temperature of the hottest spot in the resistive region as a function of initial current density and the discharge time (assuming an exponential decay in current). At constant final temperature, the required discharge time varies inversely as the square of the current density.

For the range of current densities considered in this reference design, the figure predicts discharge times required of the order of two minutes. However, two minutes is already long enough for thermal diffusion to take effect with the net result that longer times can be tolerated for discharge.

Discharge of the 25.9 GJ of energy stored by means of dumping the coil energy into 16 individual resistors connected in series between each of the 16 coils results in a maximum voltage of 1.84 kV for a 10 kA initial current. This rapid discharge of the coil system can be achieved readily without excessive voltages. With this mode of discharge all the coils remain in series so that no unusual loading condition is encountered during the discharge. There are other fault conditions however that must also be considered in the selection of the appropriate protection scheme.

3.3.2 Electrical Shorts

The electrical short is a likely occurrence in a large highly loaded coil. In practice, shorts have occurred in superconducting coils during fabrication, where they can be corrected, as well as after having been put into operation, where they are much harder to remove. In general, the term short circuit encompasses a large range of resistances. The behavior of a short circuit depends on its resistance.

Figure 3.3-1

Temperature at the end of a quench as a function of initial current density and quench time. Vertical lines 1 and 2 define the bounds for the designs considered in this report.

Let us examine the problems associated with discharging the coil system if an internal short existed within the coil. Figures 3.3-2 through 3.3-4 show the current versus time in a section of shorted winding during a two-minute time constant discharge for various values of the resistance of the short circuit. The figures show the case for a single shorted turn, for 100 shorted turns (corresponding approximately to one module), and for the full coil shorted. As is evident, if the resistance of the short is less than a critical value (which depends on how many turns are shorted), the current in the shorted section doesn't decay immediately and indeed may increase as a result of the discharge of the coil system.

In addition to the problem of increasing current, the short dissipates power. Figures 3.3-5 through 3.3-7 show the power dissipated in the short circuits during the two-minute discharge of the coil system.

3.3.3 Protection Circuit

Because of the problem with rapid discharge of the system if a short circuit exists, the protection circuit shown in Figure 3.3-8 is selected. During charge, the coils are connected in series across the power supply and are charged and discharged through use of the power supply. In this mode of operation, the circuit configuration is shown in Figure 3.3-9. The power supply is rated to charge the coil system in 86 minutes. Normal discharge is through a single resistor R_d in parallel with the power supply to keep all the coils in series.

If it is determined that no short exists, then rapid discharge can be achieved by opening the switches S (which are usually closed). This puts individual resistors R in series with each coil. However, if it is determined that a coil is shorted internally, a rapid discharge is undesirable. In this instance the normally open shorting switch S_s is closed across the

Normalized Current in Shorted Single Turn
During Discharge of System with a 2 Minute Time Constant

Figure 3.3-2

Normalized Current in Shorted 100 Turn Bundle
During Discharge of System with a 2 Minute Time Constant

Figure 3.3-3

Current in Shorted Coil (895 Turns)
During Discharge of System with 2 Minute Time Constant

Figure 3.3-4

Power Dissipated in Short of Single Turn
During System Discharge with 2 Minute Time Constant

Figure 3.3-5

Power Dissipated in Short of Single Coil (895 Turns)
During System Discharge with 2 Minute Time Constant

Figure 3.3-7

Figure 3.3-8

Circuit showing short-circuited coil
isolated from rest of system

Figure 3.3-9

Protective Circuit in Rapid Discharge of System with All Coils in Series

short circuited coil only; all other S_s switches are left in the open position as shown in Figure 3.3-8. The rest of the coil system can now be operated independent of the faulted coil.

At this point the faulted coil can either be discharged slowly by switching it across a discharge resistor bank that can be progressively increased by switching out resistors so that discharge can proceed at a rate determined by the resistance of the internal short circuit. If discharge is not possible in a reasonable amount of time, the helium coolant can be removed from the coil. This will make it resistive and should allow discharge of the coil.

The discharge of the rest of the system will induce higher currents into the faulted coil; so from this point of view, its discharge is best left until after the single coil has been discharged. If the main system current can be brought down slowly together with the faulted coil without inducing excessive currents, then a minimum of lateral loads will result. However, this cannot be relied upon, and the structural design should be such as to allow for lateral loads.

Should the short occur in a region where the main system is not at full current, further charging of the system with the coil shorted will inductively reduce the current in the shorted coil. This procedure can be used to bring the current of the coil down to a lower level where other procedures can take over.

In order to be able to successfully perform the total or partial inductive discharge of a short circuited coil, the rest of the system must be operational and not affected by the faulted coil. The proposed protection circuit allows a choice of discharge techniques depending on the specific failure experienced.

3.3.4 Cryogenic System Problems

Problems with the cryogenic system can take place in one of the following forms: local lack of coolant, shutdown of coolant supply to one or more coils, or large heat leak due to vacuum failure. As far as the coils are concerned, each of the above results in lack of cooling or the addition of heat to the coils. This may lead to resistive regions in the coils as well as high rates of heating of the helium coolant.

In order to reduce the magnitude of the heat transfer to liquid helium when vacuum failure occurs, it is desirable to have the vacuum space in sections, so that failure of one vacuum still allows the rest of the system to operate.

Further, appropriate rupture discs and vent sizes need to be provided to prevent excessive coolant pressures from developing internally.

It is generally desirable to discharge the coils in the case of a cryogenic system failure. However, if an unaccountably large heat input into only one of the coils is detected, the simplest procedure to follow is to inductively partially discharge that coil only as described above in the section dealing with shorted coils.

3.3.5 Interactions with the Coil Design

The main interaction of the protective circuit with the coil design takes place in the design of the structure to withstand operation of coils not in series, which introduces out-of-plane loads as well as in-plane bending moments. Also, enough safety factor must be provided in the structural design to allow energizing of the coil system in the event of a faulted (short circuited) coil.

Further, because it may be necessary to maintain the rest of the system operational in the case of a faulted coil, the system must be designed

in such a manner as to prevent one coil going normal from affecting its neighbors.

There is another general consideration that should be mentioned. If a design is used in which normal shields of high purity aluminum are incorporated, then to effectively shield the windings from transient magnetic fields requires time constant of the order of six minutes. Rapid discharge of one coil or the whole system in two minutes would induce currents in the shields that would try and keep the field constant. The structural effects of this need to be taken into account should a shielded design be used.

The procedures for handling cryogenic system failure faulted coils described in this section are preliminary in nature and require further detailed analysis before being finalized as part of a design. Nevertheless, this section has highlighted the major effect that a coil protection scheme has on the magnet system design. It is one of the major areas to be considered in coming up with an acceptable, reliable, safe and economic magnet system.

3.4 DEWAR TOPOLOGY

3.4.1 Introduction

The principal function of a dewar is to provide a thermal barrier between the cold structure and the operating environment. The dewar must insulate well enough to maintain an acceptable rate of heat leakage into the system. Structurally, the dewar is designed to carry gravitational and thermally-induced loads and to transmit all electromagnetic loads generated by the magnet system to their reaction points. It must also be designed to minimize the flow of eddy currents generated by the poloidal coils.

The dewar consists of a thermal radiation shield a fixed distance from the cold structure, a room temperature vessel, and gravity supports. Heat transfer studies yield the optimum spacing between the vacuum tight room temperature vessel and the radiation shield held at liquid nitrogen temperature by means of tracer tubing. The "optimum" is the physical geometry providing highest overall EPR system efficiency with consideration given to conductive heat losses through insulated spacers and gravity supports, radiative heat loss, the relative refrigeration requirements of helium and nitrogen, and dimensional impact on toroidal radius. This last factor affects the quantity and distribution of conductor in each coil, its size, weight and cost.

Containment for the liquid helium is provided for within the coil structure by means of a vacuum-tight stainless steel vessel surrounding each coil section. The description of this system is presented elsewhere in this report.

A separate service stack is provided for each coil and rises vertical above each coil. These stacks contain the vapor-cooled power leads, a LHe reservoir, the LHe fill and vent conducts, and pressure rupture disk.

A listing of the dewar (or room temperature vessel) weights, materials, and dimensions is provided in Tables 3.2-1 and 3.2-2, Summaries of Mechanical Characteristics of Major Components, for the Warm and Cold Central Cylinder concepts, respectively.

3.4.2 Warm Central Cylinder Design

Fundamental to the warm central cylinder EPR design is the use of an individual dewar for each coil. From the standpoint of coil testing/repair/replacement or overall vacuum integrity, this concept has definite advantages over a dewar connecting more than one coil. Each coil has stand-alone capability, a fact which greatly facilitates quality control inspections, achievement of vacuum integrity, and testing. Once installed in the EPR, subsequent replacement of one coil would cause minimum downtime since only warm inter-coil structures, not dewars, need be disassembled. The principal disadvantage of this design is the increase in toroidal radius necessitated by the dewar presence between adjacent coils. In this study, the dewar adds 12.7 cm (5 in.) at each side of a coil, of which 5.08 cm (2 in.) is occupied by the room temperature vessel plate; the remainder serves as vacuum space.

3.4.3 Cold Central Cylinder Design

The cold central cylinder dewar, consisting of vacuum vessel and radiation shield, encompasses all the major EPR components; i.e., TF coils, inter-coil structure, bucking cylinder, and the gravity supports. The dewar itself consists of two, roughly toroidal shell structures, one exterior to the major components and the other interior to the TF coils (see Figure 3.2-2a). The outer dewar wall generally follows the contour of converging to a vertical cylindrical section inside the bucking cylinder and then to a straight cylindrical section in the vicinity of the 90° access ports. The inside dewar is D-shaped in cross section and is connected to the outer dewar by

means of tubes of elliptical cross section. These elliptical access ports subtend an angle of 90° and are uniformly located between all the coils.

The dewar will be fabricated from one-half inch thick steel plate to meet the EPR rise time requirement. To minimize eddy current losses in these shells, both inner and outer vacuum vessels are designed in sections joined with insulated vacuum-tight flanges. Further, the shell stiffeners will be principally oriented in the meridional direction. The aluminum radiation shield is segmented as well but much more frequently to facilitate fabrication; it will be attached to the dewar interior with blocks of G10.

When the dewar sections are assembled on-site, they are canopy-seal-welded along the mating flange edges to provide vacuum integrity. The canopy seal consists of a narrow strip of thin gauge (0.05 - 0.08 cm) sheet metal which is given a slight curvature to angle the edges downward. In use, the metal strip is centered over the outer perimeter of the mating surfaces. A continuous seam weld is then made along both edges of the canopy, sealing the joint. Although this does reestablish an electrical connection between sections, the cross-sectional area of the canopy sheet metal is sufficiently small to ensure a high resistance. The insulated bolts are threaded into blind holes in the mating flange and then covered with bolt covers which are seal welded.

The outer dewar will be self-supporting for gravity loads by means of stiffened supports in the lower region. At this time, the inner dewar is assumed to rest on G10 blocks spaced along the lower interior surfaces of the TF coils, with additional support from the access port connection. However, once the room temperature plasma structure is defined, it would be

preferable to attach the inner dewar to it and eliminate the conduction heat leak through the G10 to the cold coil structure.

The one atmosphere pressure loading is the other major factor affecting the design of the dewar at this time.

A detailed design study of the EPR would require dewar analysis for seismic events and other postulated calamities. Such analyses would impact the thin-shelled cold central cylinder design much more than the heavy room temperature vessel construction of the warm central cylinder concept.

3.5. HEAT LOADS TO CRYOGENIC SYSTEM

3.5.1 Introduction

The heat input to the system results from the following:

- AC losses in the superconductor
- Eddy currents in the structure
- Heat conduction
- Nuclear radiation heating
- Thermal radiation
- Heat conduction in electrical leads

The AC losses in the conductor and the eddy currents in the structure are the largest source of heating. The original requirement for the system was for a pulsed field of 0.5 T in one second. A possible reduction by a factor of 5 in this value can be obtained by redesign of the poloidal coils. The philosophy used in carrying out the design was to design for the 0.5 T value. This results in a design that can be relaxed at a later date if necessary. Further, it brings out the features of a system designed for such a high value of pulsed field.

Because of the large eddy current and superconductor AC losses considerable effort was spent evaluating the use of a normal metal shield to limit the exposure of the low temperature components to the time varying field. The 90 second cycle time makes the requirements for such a shield severe. The analysis of these shields for the particular cycle time and magnetic field magnitude considered, and taking into account the nuclear radiation effect on resistivity, resulted in the conclusion that the use of a shield is not practical. The backup for this conclusion is included in this section of the report.

3.5.2 Magnetic Shields and Heat Input to Shields

Because AC losses in the superconductor and cold structure eddy currents were found to be the major source of power dissipation in the liquid helium for an 0.5 T pulsed field, serious thought was given to the possibilities of reducing these losses by using a magnetic shield.

The power dissipation in superconductors is proportional to dB/dt or $(dB/dt)^2$ depending on the particular loss mechanism involved, while eddy currents in the structure are proportional to $(dB/dt)^2$. Hence any reduction in the rate of change of field will result in a reduction in the power dissipation. The rate of change of field can be reduced by surrounding the conductor and cold structure in a conducting shell. When the shell is exposed to an external magnetic field, eddy currents are induced in the shell in such a direction as to cancel the applied field in the region of the superconductor. The conducting shell therefore constitutes the magnetic shield. The degree of cancellation of the applied magnetic field depends upon the thickness of the shell and the resistivity of the material from which it is constructed. The thicker the shell and the lower its resistivity then the more effective are its shielding properties.

Application of a magnetic shield therefore can result in the reduction of losses in the conductor. However, there is a price which must be paid for this reduction. First, there is the additional weight and fabrication complication due to the incorporation of a shield but, second, and more important, there are power losses in the shield itself which must be considered, especially if the shield is a cryogenic one. The total refrigeration power must be minimized; therefore the power dissipation due to AC losses in the conductor and due to eddy currents in any magnetic shield present must be considered together.

Analyses were conducted which allowed the power dissipation in the shield and AC losses in the superconductor to be calculated as a function of shield time constant. The results of these analyses are shown in Figures 3.5-1 to 3.5-10.

Figure 3.5-1 shows the shape of the pulse applied to the coils. A cycle consists of a linear rise in field from zero to a maximum in one second, followed by a 60-second flat top, followed by a linear fall in field to zero in two seconds, followed by a 27-second period during which the applied field is zero. The calculations were performed by resolving the applied field into a component parallel to the conductor and a component perpendicular to the conductor. Maximum values are given for each component in each of two zones of the magnet, and a spatial average of 70 percent of the maximum is given which is applicable for both zones and both components. In the calculations which lead to the results about to be presented advantage was not taken of this 30 percent reduction in field; an applied field of 0.5 Wb/m^2 was used for both components in both zones. This was regarded as being more realistic because the presence of magnetic shielding perturbs the applied field in the region of the shield and results in field concentrations. The field concentration effect and the spatial averaging are opposing effects, and further study is required to come up with the detailed net result.

Figure 3.5-1 also shows the model used for calculating shielding effects. It shows a winding bundle surrounded on all sides by a shell of thickness t , made of material of resistivity ρ and having sides with dimensions $2a$ and $2b$. Also shown are the parallel and perpendicular applied field components. It can be shown that the differential equation which describes the penetration of field into the region of the windings is:

PULSE SHAPE

Figure 3.5-1

Applied Pulse Shape and Model Used to Calculate Winding Shielding Effects

$$\frac{dB_i}{dt} + \frac{B_i}{\tau} = - \frac{dB_a}{dt}$$

where B_i is the field inside the shield due to the eddy currents induced in the shield walls, B_a is the applied field, and τ is a time constant which is a function of the thickness, dimensions, and resistivity of the shield. There are two time constants, one for the penetration of the parallel field component and one for the penetration of the perpendicular component.

The solution of this equation allows both the total field inside the shield and the induced current density in the shield walls to be calculated as a function of time. Knowledge of these two parameters then allows field attenuation factors inside the shield and eddy current heating in the shield walls to be calculated.

The applied field which is the function of time described above was modeled as a 90 term Fourier series and the differential equation was solved. From this solution the shield power dissipation as a function of shield time constant was obtained. This is shown in normalized form (normalized to the maximum dissipation) in Figure 3.5-2. It can be seen that the curve branches at longer time constants, the higher branch being for the initial cycle and the lower for conditions in effect after the system has been through a number of cycles. The transition time between initial cycle and steady state is simply the shield time constant τ , so for practical purposes we can focus our attention on the steady state curve.

Both parallel and perpendicular field components contribute to the power dissipation in the shield. The individual contributions and the total are plotted in Figure 3.5-3 as a function of the parallel field time constant. This figure is for conditions in effect after a number of cycles. It was assumed that the shield completely surrounded the helium container.

Figure 3.5-2. Average Power Dissipation in Shield

Figure 3.5-3. Refrigerator Power as a Function of Shield Time Constant for Shield Operating Temperatures of 20 K and 80 K

The scale on the left-hand side represents power dissipated in the shield at the operating temperature. The two scales on the right-hand side of the figure represent the refrigeration power required if the shield is at temperatures 20 K or 80 K. The ratio of refrigerator power to heat load used were 40 and 7.9 for 20 K and 80 K shield temperatures respectively. These factors represent an efficiency of 35 percent of Carnot efficiency. The value of 35 percent was selected as being the value which is achievable with large (~ 1 MW) commercial refrigerators. The shield power dissipation peaks at 1.6 MW at a four-second time constant; this represents 64 MW of refrigerator power for the 20 K shield and 12.6 MW for the 80 K shield.

As was stated earlier if a shield is to be beneficial then the power generated due to the shield must be significantly less than the reduction in power generated by AC losses in the conductor. Therefore AC loss attenuation factors were calculated using the 90 term Fourier series model for the applied field pulse shape and the solution of the differential equation governing the field inside the shield described previously. There are two kinds of AC loss to be considered: (1) a magnetization type loss where the power is proportional to dB/dt , and (2) an eddy current type loss where the power is proportional to $(dB/dt)^2$. The attenuation curves for each of these losses are shown in Figures 3.5-4 and 3.5-5, respectively. In both cases the attenuation is defined as the average power dissipated in the conductor over one cycle with a magnetic shield, divided by the power dissipated without a shield. Curves are plotted as a function of time constant again, and as before, each curve branches at higher time constants to show the situation during the initial cycle and after many cycles.

Figure 3.5-4. Relative Conductor Magnetization Losses as a Function of Shield Time Constant

Figure 3.5-5

Relative Eddy Current Losses as a Function of Shield Time Constant

Figure 3.5-4 (for magnetization type losses) shows that there is negligible attenuation for time constants less than ten seconds and that a 200-second time constant is required to get an attenuation factor of 10. Similarly, inspection of Figure 3.5-5 (for eddy current type losses) shows that an attenuation factor of 10 can be obtained with a seven-second time constant.

The significance of the shield power plots and the AC loss attenuation plots can be seen by examining Figures 3.5-6 and 3.5-7. These two figures show the total refrigeration power required for removing heat from both the shield and the conductor, as a function of time constant. Figures 3.5-6 and 3.5-7 are for 20 K and 80 K shields, respectively. Each figure has curves representing various possible AC losses ranging from zero to 100 kW at 4.2 K.

Figure 3.5-6 shows that for a 10 kW AC loss at 4.2 K, a 20 K shield results in higher required refrigeration power unless the shield time constant is greater than about 550 seconds. Similarly, for a 100 kW AC loss the shield time constant must be greater than 50 seconds. The greater the AC loss then the lower the shield time constant required for a net refrigeration power requirement.

For a typical AC loss of 50 kW a shield time constant (shield at 20 K) of greater than 100 seconds is required if a net reduction in refrigeration power is to be realized. It is of interest to see what this means in terms of a shield thickness. Figure 3.5-8 is a plot of shield resistivity versus shield thickness for various time constants. Also shown in this plot are the resistivities of various candidate engineering materials at various temperatures, background magnetic field, and neutron fluences. For example, at 20K, 8T and 10^{17} neutrons (conditions relevant to the higher field regions

Figure 3.5-6. Total Refrigeration Power as a Function of Shield Time Constant for a Shield Temperature of 20 K

Figure 3.5-7. Total Refrigeration Power as a Function of Shield Time Constant for a Shield Temperature of 80 K

Ordinate	Material	Temperature K	Neutron Fluence n/cm^2	Magnetic Flux Density (T)
1	304L SS	273	-	-
2	304L SS	77	-	-
3	304L SS	20,4	-	-
4	6061-T6 Al	273	-	-
5	6061-T6 Al	77	-	-
6	6061-T6 Al	20,4	-	-
7	High Purity Al (RR=1100)	273	-	-
8	High Pur. Al	77	-	-
9	High Pur. Al	20,4	10^{17}	8
10	High Pur. Al	20,4	10^{17}	0
12	High Pur. Al	20,4	0	8
13	High Pur. Al	20,4	0	0
14	OFHC Cu	273	-	-
15	OFHC Cu	77	-	8
16	OFHC Cu	77	-	0
17	OFHC Cu	20,4	10^{17}	8
18	OFHC Cu	20,4	0	8
19	OFHC Cu	20,4	10^{17}	0
20	OFHC Cu	20,4	0	0

Figure 3.5-8

Shield Time Constants as a Function of Shield Resistivity and Thickness

of the shield), OFHC copper has a resistivity⁶ of about 7×10^{-10} ohm-meters, and high purity (resistance at cryogenic temperature 1100 lower than its room temperature value) aluminum has a resistivity⁶ of about 5×10^{-10} ohm meters. It can be seen from Figure 3.5-8 that 100-second time constant shields made from OFHC copper or high purity aluminum would be 25 and 18 cm thick, respectively. Even though the 100-second time constant shields result in no net reduction in the required refrigeration power, they are already impractically thick. To obtain a significant reduction in required refrigeration power the shields would have to be much thicker or subjected to lower neutron fluences than the values quoted above.

If there were no irradiation of the shield by neutrons then a shield made of high purity aluminum might be worth considering. Unirradiated high purity aluminum (resistivity ratio 1100) has a resistivity of about 6.5×10^{-11} Ωm in an 8.0 T background field; i.e., about an order of magnitude smaller than irradiated material. This would therefore result in a shield thickness about ten times less than the value quoted above.

The situation is little better for an 80 K shield as inspection of Figures 3.5-7 and 3.5-8 will show. From Figure 3.5-7 it can be seen that for a 50 kW AC loss a time constant greater than 15 seconds is required if there is to be a net reduction in required refrigeration power. Figure 3.5-8 shows that the required shield thicknesses are 14 and 11.5 cm for OFHC copper and high purity aluminum, respectively.

These results are summarized in Table 3.5-1. This table shows the total refrigeration powers required for various magnitudes of AC loss (5 kW to 100 kW) for shields having time constants ranging from 0.1 seconds to 1,000 seconds and at different shield temperatures (20 K, 80 K, and 300 K).

TABLE 3.5-1

REFRIGERATOR POWER AND SHIELD THICKNESS AT SEVERAL TEMPERATURES AND AC LOSSES

AC Loss at 4.2 K					5 kW			10 kW		20 kW		50 kW		100 kW	
Shield Temp. (K)	τ (s)	Shield Thickness (inch)			Refrig. Power for Shield (MW)	Refrig. Power for AC Loss (MW)	Refrig. Power AC Loss & Shield (MW)	AC Loss	AC Loss & Shield	AC Loss	AC Loss & Shield	AC Loss	AC Loss & Shield	AC Loss	AC Loss & Shield
		HP Al	OFHC Cu	6061 T6 Al											
20	0	-	-	-	0	1.01	1.01	2.01	2.01	4.02	4.02	10.1	10.1	20.1	20.1
	0.1	0.033	0.046	0.9271	13.2	0.91	14.1	1.81	15.0	3.62	16.8	9.09	22.3	18.1	31.3
	1	0.335	0.460	9.271	53.2	0.31	53.5	0.61	53.8	1.23	54.4	3.08	56.3	6.13	59.3
	10	3.35	4.60	92.71	59.2	0.03	59.2	0.06	59.3	0.12	59.3	0.29	59.5	0.57	59.8
	100	33.5	46	927.1	10.6	-	10.6	-	10.6	-	10.6	-	10.6	0.01	10.6
	1000	335	460	9271	1.00	-	1.00	-	1.00	-	1.00	-	1.00	-	1.00
80	0	-	-	-	0	1.01	1.01	2.01	2.01	4.02	4.02	10.1	10.1	20.1	20.1
	0.1	0.140	0.1651	1.143	2.59	0.91	3.50	1.81	4.40	3.62	6.21	9.09	11.7	18.1	20.7
	1	1.40	1.651	11.43	10.5	0.31	10.8	0.61	11.1	1.23	11.7	3.08	13.6	6.13	16.6
	10	14.0	16.51	114.3	11.6	0.03	11.6	0.06	11.7	0.12	11.7	0.29	11.9	0.57	12.2
	100	140	165.1	1143	2.08	-	2.03	-	2.08	-	2.08	-	2.03	0.01	2.09
	1000	1400	1651	11,430	0.20	-	0.20	-	0.20	-	0.20	-	0.20	-	0.20
300	0	-	-	-	0	-	1.01	-	2.01	-	4.02	-	10.1	-	20.1
	0.1	1.651	1.016	2.567	-	-	0.91	-	1.81	-	3.62	-	9.09	-	18.1
	1	16.51	10.16	26.67	-	-	0.31	-	0.61	-	1.23	-	3.08	-	6.13
	10	165.1	101.6	266.7	-	-	0.03	-	0.06	-	0.12	-	0.29	-	0.57
	100	1651	1016	2657	-	-	-	-	-	-	-	-	-	-	0.01
	1000	16510	10160	26570	-	-	-	-	-	-	-	-	-	-	-

Also shown are the shield thicknesses required to realize these time constants for various shield materials. The resistivities used correspond to a fluence of 10^{17} neutrons and a magnetic field of 8 T. It can be seen that, in all cases to obtain a significant reduction in total refrigeration power, larger than desirable shield thicknesses are required.

The possibility of using a double shield (one shield at room temperature and one at cryogenic temperature) was briefly considered. Figure 3.5-9 shows the refrigeration power required for the cryogenic shield as a function of the cryogenic shield time constant for various room temperature shield time constants. Similarly, Figure 3.5-10 shows the ac loss attenuation factor for the same variables. In this case, too, it was found that for significant overall refrigeration power reductions, shield thicknesses were greater than desirable.

Finally, estimates were made of refrigeration powers that would be required should a superconducting shield be incorporated. The estimate indicated that the potential exists for significant power savings. Because of the lack of practical experience concerning superconducting shields on this large a scale such a shield is not recommended for this design. However, it is an area which deserves further thought.

The conclusion reached concerning magnetic shielding as a means of reducing overall refrigeration power requirements is that it is not desirable because of the inordinately large shield thicknesses which would be required to build an effective shield.

Figure 3.5-9. Refrigeration Power for a Double Shielded System

Figure 3.5-10

Relative Eddy Current Losses for a Double Shielded System

3.5.3 AC Losses in the Conductor

The impact of AC losses on the conductor design and the reasons which led to the choice of the conductors selected have been dealt with in Section 3.1.5. The following AC loss estimates were based on the conductors and formulae described in that section. The formulae allow the AC losses per unit volume of a given conductor to be calculated at a given field. The conductor in a coil, however, is exposed to a continuously varying field from zero to a maximum; and, furthermore, there are three different grades of conductor in the coil. An exact calculation of the total AC loss for the coils taking into account the actual distribution of conductor and field within the coil would be extremely difficult if an analytical integration were to be performed. Because of the uncertainties of material properties such accuracy is not justified. Therefore, an averaging process was used whereby the coil was split up into various regions which were characterized by a single value of magnet field. Figure 3.5-11 depicts the way in which the coil was subdivided.

It can be seen that there are four divisions (two of which are magnetically equivalent) circumferentially round the coil. Each of these circumferential divisions is divided into three subdivisions which correspond to the three grades of conductor. Each grade of conductor in each circumferential division is divided into two further divisions corresponding to a high field and low field region. The high field region and the low field region are each characterized by a single value of magnetic field. Therefore, calculations of AC loss are performed at 24 different positions in the coil. Since two sections of the coil are magnetically identical (2a and 2b) this reduces to calculating the AC losses for 18

Figure 3.5-11

Schematic Showing Location of the Three Different Conductor Grades and the Coil Sections Used in Computing the Conductor ac Loss.

different positions. The AC losses were calculated separately for four different kinds of AC loss, and for each kind a calculation was performed for the losses due to perpendicular and parallel field components. The results of these calculations are given in Table 3.5-2. The material properties used at the various fields are given in Table 3.5-3 as are the areas of cross section and lengths of the various components of the conductor. The calculations were performed for a pulsed field magnitude of 0.5 T, and the detailed breakdown of AC losses shown in Table 3.5-2 are for this value. The total AC loss for the whole torus under these conditions is about 60 kW. Below the main table the subtotals corresponding to particular AC loss mechanisms are given for the case where the pulsed field magnitude is 0.35 T (which includes the space average of 0.7 of the maximum value of 0.5 T). The total AC loss corresponding to this pulsed field excursion is about 36 kW. It can be seen that approximately 33 percent of the AC loss is due to coupling between strands due to the parallel field component and a further 20 percent due to coupling between filaments caused by the parallel field component.

Also shown in the table are values corresponding to a reduction by a factor of 5 of the maximum value of pulsed field. This results in a total AC loss of 3.6 kW--a value which is much more tractable.

Because the AC losses depend so strongly on the rate of change of magnetic field, the reduction by a factor of 5 would relax many of the conductor and other design features required for a 0.5 T pulsed field design.

Table 3.5-2.

AC Loss in Whole Torus in kW by Specific AC Loss Mechanism

Conductor Grade	Coil Section	Field Region of Grade	Characteristic Field (Wb/m ²)	Coupling in Individual Strands		Coupling Between Strands		Eddy Currents in Stabilizer		Magnetization in NbTi		Total
				B _⊥	B	B _⊥	B	B _⊥	B	B _⊥	B	
1	1	H	8.00	0.340	0.035	0.206	0.258	0.272	0.018	0.012	0.010	1.15
1	1	L	5.33	0.451	0.062	0.272	0.454	0.361	0.024	0.022	0.017	1.66
1	2	H	3.95	1.086	0.161	0.656	1.197	0.870	0.057	0.058	0.045	4.13
1	2	L	2.63	1.350	0.228	0.816	1.693	1.081	0.071	0.082	0.064	5.39
1	3	H	2.41	0.700	0.122	0.423	0.908	0.560	0.037	0.044	0.034	2.83
1	3	L	1.61	0.808	0.165	0.488	1.227	0.647	0.042	0.059	0.047	3.48
2	1	H	5.33	0.282	0.038	0.161	0.291	0.307	0.020	0.014	0.011	1.12
2	1	L	2.67	0.419	0.070	0.239	0.531	0.457	0.030	0.025	0.020	1.79
2	2	H	2.63	0.845	0.134	0.481	1.086	0.921	0.060	0.048	0.038	3.61
2	2	L	1.32	1.066	0.224	0.607	1.809	1.163	0.076	0.080	0.063	5.09
2	3	H	1.61	0.506	0.103	0.288	0.787	0.551	0.036	0.037	0.029	2.34
2	3	L	0.80	0.582	0.164	0.332	1.248	0.635	0.041	0.059	0.046	3.11
3	1	H	2.67	0.236	0.039	0.127	0.286	0.380	0.025	0.141	0.111	1.35
3	1	L	0	0.362	0.217	0.194	1.582	0.581	0.038	0.779	0.612	4.36
3	2	H	1.32	0.602	0.134	0.323	1.021	0.966	0.063	0.048	0.377	3.20
3	2	L	0	0.724	0.435	0.389	3.164	1.162	0.076	1.557	1.223	8.73
3	3	H	0.80	0.328	0.092	0.177	0.704	0.528	0.034	0.331	0.260	2.45
3	3	L	0	0.362	0.217	0.194	1.582	0.581	0.038	0.779	0.612	4.36
$\Delta B = 0.5 \text{ T}$ Total =				11.05	2.64	6.37	19.83	12.02	0.79	4.18	3.28	60.2
$\Delta B = 0.35 \text{ T}$ Total =				5.41	1.85	3.12	13.88	5.89	0.39	2.93	2.30	35.8
$\Delta B = 0.10 \text{ T}$ Total =				.442	0.106	0.255	0.793	0.481	0.032	0.836	0.656	3.60

Table 3.5-3

Material Properties Used in Calculation of AC Losses for 8 T Coil

<u>B (Wb/m²)</u>	<u>ρ (10⁻¹⁰ Ωm)</u>	<u>j_c (10⁷ A/m²)</u>
8.00	5.06	80
5.33	3.82	141
3.95	3.17	186
2.67	2.57	257
2.63	2.55	263
2.41	2.46	282
1.61	2.13	381
1.32	2.02	438
0.80	1.85	604
0	1.68	1,420

Conductor Dimensions Used

<u>Conductor Grade</u>	<u>Area NbTi 10⁻⁶ m²</u>	<u>Area Composite 10⁻⁶ m²</u>	<u>Area Stabilizer 10⁻⁶ m²</u>	<u>Conductor Length (Torus) 10³ m</u>
1	16.7	30.0	221	128
2	9.81	17.7	188	128
3	5.89	10.6	156	128

3.5.4 Eddy Currents in the Structure

The eddy currents must be considered from two points of view:

1. Eddy current heating at cryogenic temperature
2. Delay distortion of the magnet field produced by poloidal field windings

The eddy current heating can be one of the major sources of loss for a system designed to withstand maximum pulsed fields of 0.5 T with a rise time of 1 second and a decay time of 2 seconds.

The particular pulse duty cycle specified for this design has a 1-second rise time, 60-seconds flat top, 2-seconds decay, and 27-seconds before the start of the next pulse. (Total cycle time of 90 seconds). The instantaneous eddy current heating is 60 times larger than the average heating over a whole cycle. So that two aspects need to be considered -- (1) the load on the refrigerator, and (2) the instantaneous effect on the cooling passages and coolant supply.

Figures 3.5-12 and 3.5-13 show the basic nose cross sections of the warm central cylinder and cold central cylinder design. These cross sections are subjected to pulsed fields that are parallel to the conductor as well as to fields in the plane of the coil directed perpendicular to the conductor.

The pulsed parallel field tends to induce currents in the plane of the cross section. Consequently, in order to reduce the magnitude of these the cold coil structure must be put together in such a way as to prevent large eddy currents from flowing. This is shown in Figure 3.5-13 which shows the insulated joints in the cold coil structure. These joints are all bolted and do not form part of the helium container; as such they can be insulated without too much trouble.

Figure 3.5-12

Cross section of 8 T warm central cylinder design showing required insulation to minimize eddy currents

Figure 3.5-13. Cross section of 8 T cold central cylinder design showing required insulation to minimize eddy currents in the nose area

The computed structural eddy current losses for the designs using insulated joints are shown in Table 3.5-4. The table shows the eddy currents in the cold coil structure for both warm and cold central cylinders. For the cold central cylinder design the table also includes the eddy current losses in the intercoil structure.

As is readily evident the eddy currents are a major source of loss for a system designed for 0.5 T maximum pulsed field. The table also shows the reduction of eddy current losses if the maximum pulsed field is reduced by a factor of 5.

The second consideration is the instantaneous surface heat flux from the structural plates due to eddy current heating. Table 3.5-4 also shows the heat generated per unit surface area of structural plate at the point where the eddy currents are highest. This point is located on the edge of the large base trapezoidal cross section. This is the amount of heat that would have to be removed from the surface of the plate without taking into account specific heat or lateral heat conduction. Both of these do indeed take place so that the peak values will be reduced.

The peak heat generation of heat fluxes of 0.91 and 1.30 W/cm² for a pulsed field of 0.5 T points to the fact that careful design of the steel plate-coil case interface is required in order to prevent hot spots during the peak power period (which is 1-second long). As can be seen from the table reduction of the pulsed field to 0.1 T reduces the surface heat fluxes to negligible values.

The central cylinder from a structural point of view supports the balanced or unbalanced radial forces of the individual coils. Structurally, it is best designed as a solid structural cylinder. However, to the poloidal coils this type of design represents a shorted secondary winding, which delays or prevents coupling to the plasma.

TABLE 3.5-4

EDDY CURRENTS IN COLD STRUCTURE

	$\Delta B = 0.5 \text{ T}$ Max		$\Delta B = 0.1 \text{ T}$ Max	
	<u>Average Over Cycle</u>	<u>Peak (1 sec Duration)</u>	<u>Average Over Cycle</u>	<u>Peak (1 sec Duration)</u>
Eddy Current Losses (kW)				
Warm Central Cylinder	26.1	1570	1.04	62.8
Cold Central Cylinder				
Coil Structure	99.3	5960	3.97	240
Intercoil Structure	<u>19.5</u>	<u>1170</u>	<u>0.781</u>	<u>47</u>
Total	120	7130	4.75	287
Local Eddy Current Losses Per Unit Surface (W/cm^2)				
Warm Central Cylinder	.015	0.91	0.0006	0.036
Cold Central Cylinder	.022	1.30	0.0008	0.052

In addition to the problem of the delay in the flux penetration to the plasma there is also the problem of power loss in the central cylinder. If it is at room temperature the primary detrimental effect is the additional power required from the poloidal coil power supply. However, if it is at cryogenic temperatures the additional refrigeration power has to be considered.

Figure 3.5-14 illustrates the magnitude of the eddy current problems in a solid cylinder. This shows the power dissipated in the cylinder for 50 and 25 volt-second pulses generated by the poloidal coils during a 1-second pulse. For the design range of interest the eddy currents are prohibitive and the cylinder must be segmented.

The segmentation used is illustrated in Figure 3.5-15. The cylinder is made up of insulated circular segments that are bolted and pinned together. This interrupts the main flow of eddy current in the cylinder, as well as provides for a relatively straightforward fabrication and assembly of the central cylinder.

The design of the structural joints between all structural components takes into account the eddy current problem by means of electrically insulated interfaces that prevent closed loops in parallel with the poloidal coils.

For both the warm and cold central cylinder the following interfaces are insulated:

1. Nose to central cylinder
2. Coil case to coil case in nose area
3. Coil case to intercoil structure.

The above effectively prevent a short-circuited secondary from existing in the system. However, the individual components are still subjected to parallel and perpendicular fields which must penetrate and exit each cycle from the metal parts.

Figure 3.5-14

Eddy Current Losses in a Solid Central Cylinder Due to Pulsed Fields of Poloidal Coils

Figure 3.5-15
Bucking Cylinder Assembly

In order to provide maximum design flexibility aluminum has not been considered for structural use because of its low electrical resistivity when compared with stainless steel.

In addition to the interfaces mentioned above, the cold central cylinder requires segmenting of the room temperature vacuum container in order to prevent poloidal closed loops.

3.5.5 Other Heat Loads and Summary

The other sources of heat leak are the conduction through supports resisting magnetic and gravitational forces, thermal radiation, nuclear radiation and current lead losses. As shown in the summary heat input Tables 3.5-5 and 3.5-6, only thermal conduction in the warm central cylinder design is significant when compared with the heat load due to ac losses with a 0.5 T maximum pulsed field.

A pair of optimized vapor-cooled leads operating at its design current boils off liquid helium at the rate of 3.3 l/hr/kA. This is equivalent to 2.29 W of heat in terms of latent heat of vaporization; however, since the gas is not returned cold to the refrigerator, the refrigeration power is approximately five times what it would be if it were returned as gas at 4.2 K. The equivalent heat load contains the factor of five. Therefore, 16 pairs of leads, each operating at 10 kA, creates an effective heat leak of 1.8 kW.

Heating due to nuclear radiation has been estimated with the expression

$$Q = Q_0 e^{-0.01 m}$$

where $Q_0 = 1 \times 10^{-6}$ W/g at the surface closest to the plasma.

m = interposed mass in g/cm^2 along a line perpendicular to the surface facing the plasma

Table 3.5-5
Summary of Heat Input from all Sources
8 T Warm Central Cylinder Design

<u>Source</u>	<u>Maximum Pulsed Field</u>	
	<u>0.5 T</u>	<u>0.1 T</u>
Heat Load at LHe (4.2 K)		
Conductor AC Loss	60.2 kW	3.6 kW
Structure Eddy Current	26.1	1.0
Thermal Radiation	0.1	0.1
Thermal Conduction	15.3	15.3
Nuclear Radiation	0.5	0.5
16 Pairs of Electrical Leads	<u>1.8*</u>	<u>1.8*</u>
Total	104 kW	22.3 kW
Refrigeration Power at 300 K	25 MW	6.6 MW
Heat Load at LN ₂ (76 K)		
Thermal Radiation	35 kW	35 kW
Thermal Conduction	<u>350</u>	<u>350</u>
Total	385 kW	385 kW
Refrigeration Power at 300 K	3.3** MW	3.3** MW
Total Refrigeration Power	28.3 MW	9.7 MW

* Equivalent heat load

** Assumes 35 percent Carnot efficiency

Table 3.5-6

Summary of Heat Input from all Sources
8 T Cold Central Cylinder Design

<u>Source</u>	<u>Maximum Pulsed Field</u>	
	<u>0.5 T</u>	<u>0.1 T</u>
Heat Load at LHe (4.2 K)		
Conductor AC Loss	54.8 kW	3.3 kW
Structure Eddy Current	118.8	4.8
Thermal Radiation	0.2	0.2
Thermal Conduction	0.1	0.1
Nuclear Radiation	0.7	0.7
16 Pairs of Electrical Leads	<u>1.8*</u>	<u>1.8*</u>
Total	176 kW	10.9 kW
Refrigeration Power at 300 K	40 MW	3.6 MW
Heat Load at LN ₂ (76 K)		
Thermal Radiation	40 kW	40 kW
Thermal Conduction	<u>1</u>	<u>1</u>
Total	41 kW	41 kW
Refrigeration Power at 300 K	0.3** MW	0.3** MW
Total Refrigeration Power	40.3 MW	3.9 MW

* Equivalent heat load

** Assumes 35 percent Carnot efficiency

Integrating this formula with respect to the total path traversed through the magnet yields

$$Q = 1.0 \times 10^{-4} (1 - e^{-0.01 M}) \text{ W/cm}^2$$

where M is the total mass traversed in g/cm^2 . Q is the heat input to the coil per unit area of the cold structure perpendicular to the plasma. In the warm central cylinder design, the cold mass is essentially that of the conductor and cold coil structure. In the cold design, however, additional nuclear heating is obtained from the presence of the cold intercoil structure. The heat loads from nuclear radiation are thus found to be 0.5 kW and 0.7 kW for the warm and cold central cylinder designs, respectively. These losses are small compared to the heat leak due to ac losses.

Thermal radiation heat loads were calculated to the helium region and to the radiation shield maintained at 76 K. Using the expression

$$\frac{q}{A} = \frac{\sigma E}{2-E} (T_1^4 - T_2^4) \text{ W/cm}^2$$

and taking the surface emissivity, E , as 0.05, the total radiation heat loads for the warm central cylinder design were found to be 0.1 kW at 4.2 K and 34.3 kW at 76 K. Similar calculations for the cold central cylinder design yielded 0.2 kW at 4.2 K and 39.3 kW at 76 K. Once again, the greater area of cold structure in the latter design causes the increased heat leak.

Similar calculations were performed for a vacuum space filled with superinsulation. For the warm central cylinder design, this reduced the thermal radiation heat loads to 0.01 kW at 4.2 K and 3.2 kW at 76 K. While the addition of insulation appears to be beneficial, its principal advantage would be in reducing the heat load at 4.2 K; each watt gained here necessitates more than 200 watts of refrigeration power at 300 K to be removed.

Countering the addition of the insulation are the increased complications in structural design and fabrication. These, together with the fact that the thermal radiation heat loads at 4.2 K without insulation are very small compared to ac and eddy current losses, led to the decision not to put insulation in the vacuum space.

Thermal conduction heat leak is caused by conduction through supports to the cold structure. For this design study, G10 epoxy laminate has been specified as the primary support medium throughout; its design stress has been taken as 152 MN/m^2 .

The warm central cylinder design is subject to conduction losses through the gravity supports and the supports necessary to resist the forces directed radially inward and those directed out-of-plane due to either pulsed operation or fault loadings. The cold central cylinder design is relatively free from conduction heat leaks, with losses only through the gravity supports and the blocks maintaining the radiation shield spacing relative to the vacuum vessel.

The thermal conduction heat loads were calculated from the expression

$$Q = \frac{A}{\ell} \int_{T_1}^{T_2} k dt \quad (\text{watts})$$

where the thermal conductivity integrals for G10 were assumed equivalent to those of glass; namely, 0.175 W/cm from 76 K to 4.2 K and 1.82 W/cm from 300 K to 76 K. In the warm design, the nominal dewar vacuum space is 7.5 cm. Within it, the G10 is heat stationed at 76 K at a distance of 3.0 cm measured from the 300 K end. In regions away from the straight leg, the overall length of the out-of-plane supports vary between 7.6 cm and 22.9 cm (see Figure 3.2-1b). These values led to heat leaks of 11.0 kW at 4.2 K

and 172 kW at 76 K along the straight leg, 4.3 kW and 179 kW through the out-of-plane supports, and 0.03 kW and 0.3 kW in the gravity supports. As shown in Table 3.5-5, the total conductive heat loss at 4.2 K, though significant, is still less than 20 percent of the combined ac and eddy current losses for the warm cylinder design with a 0.5 T maximum pulsed field.

Thermal conduction calculations for the cold central cylinder design yielded losses through the gravity supports for the bucking cylinder and coil assemblies of 0.1 kW at 4.2 K and 0.3 at 76 K and losses at the radiation shield supports of 0.5 kW at 76 K. These heat leaks are trivial compared to the ac and eddy current losses with a 0.5 T pulsed field.

Tables 3.5-5 and 3.5-6 summarize all these heat loads along with those of electromagnetic origin.

3.6 FABRICATION PLAN

3.6.1 Introduction

This section presents the overall fabrication plans for the 8 T warm and cold central cylinder designs. The plans for each differ primarily with respect to the sequence of assembly of major components since the key parameters such as conductor, winding technique, coil design and overall structural design are essentially the same for both concepts. A detailed review of specific aspects of these fabrication plans is presented in Appendix II.

The warm central cylinder design is characterized by coils with individual dewars, warm intercoil structure and a warm central cylinder. This arrangement is shown in Figure 3.6-1. The cold central cylinder design has all coils, intercoil structure and central cylinder cold within a large vacuum vessel.

In the development of the fabrication plans, emphasis was placed on maximizing off-site fabrication and assembly. Since all of the coil designs presented can be constructed with facilities currently available in industry the plan takes full advantage of this fact. The major points of component buildup and assembly for both warm and cold designs are presented in Figure 3.6-2.

The terms referred to in the warm central cylinder design are further defined in Figure 3.6-3. In brief, the fundamental component is a single "coil module" comprising a D-shaped conductor winding and liquid helium vessel. Eight of these are carefully positioned and shimmed within a mechanical structure of bolted and insulated construction. This assembly is a complete coil minus dewar and is known as the "cold coil structure". With the addition of an individual load-

Figure 3.6-1 Major Component Arrangement

QC, Involving Both Inspection and Tests Appropriate to Each Component, Will be Performed Following Each Operation.

Figure 3.6-2
FABRICATION PLAN

COIL MODULE
(Off-Site)

COLD COIL STRUCTURE
(Off-Site)

COIL ASSEMBLY
(Off-Site)

MAGNET ASSEMBLY MODULE
(Off-Site)

T. F. MAGNET ASSEMBLY
(On-Site)

Figure 3.6-3

T. F. MAGNET ASSEMBLY ELEMENTS

carrying dewar (or "room temperature vessel") the "coil assembly" is complete after being cooled down and fully tested. Two such coil assemblies are carefully aligned in a fixture simulating the reactor pad and central cylinder on-site. Intercoil shear panels are then bolted in place. The result is a "magnet assembly module" ready for shipment by barge to the reactor site.

Once these are in place on-site, the remaining sets of intercoil panels are positioned around the central (or bucking) cylinder previously installed. The warm central cylinder magnet assembly is then effectively complete.

The fabrication plan for the cold central cylinder design differs in that each coil assembly is not contained in an individual vacuum vessel and the shipping unit or magnet assembly module consists of two such assemblies bolted together with intercoil shear panels. Performance testing will be done at the factory by using a room temperature vacuum vessel large enough to accomodate a single coil. In this design, the central (bucking) cylinder and dewar sections are shipped separately to the site. Of necessity, the large dewar is assembled section-by-section in sequence with the arrival of the magnet assembly modules.

The fabrication plans establish separate work stations for each major build, test or assembly point at the off-site facility. In this way, one coil module can be assembled on a single winding machine, while another module is undergoing mechanical and electrical tests, and another is being installed in the cold coil structure. The net result is an assembly line operation of coil components, optimizing equipment, manpower and scheduling. Further, this component approach facilitates repair or replacement of discrete elements at any stage of buildup should it be necessary.

The decision to ship two joined coil assemblies as a magnet assembly module was bounded by the desirability of maximizing assembly off-site on the one hand and the sizeable mass of the module on the other. In the same manner, the decision was made to fabricate, align and assemble the central cylinder off-site and ship it as a single unit. The odd sets of intercoil shear panels would be premachined off-site, the final machining and fitup on-site being performed with the aid of transfer drilling templates.

Another factor bearing on the fabrication plan development was the need to generate structural component designs compatible with remote maintenance requirements for coil repair or replacement. For the warm central cylinder design, this necessitated bolted intercoil shear panels, bolted and seal-welded room temperature vessels and bolted cold coil structures. The cold design was similarly designed with bolted and seal-welded dewar construction, bolted intercoil shear panels and bolted cold coil structures.

3.6.2 Conductor Fabrication

The conductor design chosen for the 8 T magnet system is shown in Figure 3.6-4. It consists of a stranded rectangular type NbTi superconducting cable bounded by a similar rectangular OFHC copper cable on either side. These copper stabilizing cables extend above and below the superconducting cable; an epoxy resin material is used to fill the gaps so formed and transmit the electromagnetic forces from the conductor to the cold structure. The net result is a compact rectangular-shaped conductor.

The tin-coated NbTi composite strands are fabricated by standard manufacturing techniques as outlined in Table 3.6-1. This process

Figure 3.6-4 Schematic Illustrating Method of Interlayer and Interturn Insulation

Table 3.6-1

Work Flow Sequence for Superconducting Wire and Cable

<u>Operation</u>	<u>Steps</u>
Incoming Material Inspection*	Visual and dimensional inspection of all materials to verify conformity with specifications.
Component Preparation*	Chemical cleaning, two water rinses, chemical rinse and forced air drying of all components followed by visual and dimensional inspection to assure cleanliness and precision fit. Store copper components in inert atmosphere to prevent oxidation.
Billet Assembly	Stack array of matrix tubes and solid fillers. Insert superconducting alloy rods into matrix tubes. Press nose into extrusion cylinder. Install array in extrusion cylinder. Insert solid fillers to pack cylinder. Photograph for Q.C. record. Press lid into extrusion cylinder. Electron beam weld cylinder seams.
Billet Extrusion and Initial Drawing	Extrude to approx. 2½" dia. Crop extrusion ends. Take Q.C. sample (part rod at mid-length). Cold work reduction on draw bench to .818" dia.
Wire Fabrication and Cabling	Cold work reduction. Major heat treatment. Twisting. Cold work final size reduction. Tinning. Short sample testing. Cable about core. Short sample testing. Shipment to composite fabrication.

-
- * Operations are common to:
- 1) superconducting alloy rod
 - 2) copper matrix tubes
 - 3) copper filler rods
 - 4) extrusion cylinder, nose and lid

routinely employed at MCA allows over 2000 NbTi filaments to be drawn and twisted into one strand. During processing, the cable undergoes several heat treatments to optimize the current-carrying capacity of the superconducting wire and to maximize the copper resistivity ratios. These heat treatments also serve to stress relieve the wire.

Once completed, lengths of superconducting cable and stabilizing copper cables are to be shipped on separate spools to the off-site facility for coil module fabrication.

The insulation proposed consists of a filled epoxy resin material such as G10. This is to be molded in finite lengths to the required shapes. During the winding phase of the coil module, the resin is locally heated to soften it, then pressed into place in the conductor. Additional epoxy insulation is placed between conductors to provide helium passages. This material will be manually inserted in the pattern shown in Figure 3.6-4 during the winding phase.

At specified points throughout the conductor manufacturing process, quality control inspections and tests are to be performed. These are discussed in Appendix II.

3.6.3 Coil Winding

The major considerations implicit in the successful winding of the coil modules are conductor cleaning, splicing, tensioning, inspection and testing. A brief description of the overall procedure is presented below; refer to Appendix II for details.

A coil winding station is shown schematically in Figure 3.6-5. The operational sequence is to mount spools of superconducting cable and copper stabilizer at the left, draw them through guides to position the three cable components, locally heat the previously

Figure 3.6-5

Schematic Diagram of Coil Module Winding Process

solder-filled cables to form a single unitized conductor, insert the epoxy filler, clean, tension and wind the conductor.

An exploded view of the joint geometry for this rectangular cable is depicted in Figure 3.6-6. The joint is designed to retain the overall rectangular dimensions of the conductor. The required length of each mating superconductor is prepared as shown, then soft-soldered in a specially-designed fixture to ensure mechanical and electrical integrity.

The tensioning is to be minimized since the coil designs are predicated upon the winding being free to bear against the cold structure when loaded. Therefore, the only pretension in the conductor will be that required to efficiently and economically wind the coil modules. The tensioning schedule to be used would be determined by analyses and confirmatory tests performed during a detailed design program.

A schematic of the coil module fabrication sequence is presented in Figure 3.6-7; Figure 3.6-8 illustrates the configuration of the winding machine. This machine would be designed to accommodate the four discrete D-shaped coil modules, hence, the split block forms and mechanical jacks.

The end result of the winding process would be the completed coil module shown in Figure 3.6-9. This module would be transferred to the next station for inspection and testing.

3.6.4 Coil Assembly

Following the above QC operations, the coil module would be moved to the coil assembly area and inserted into the waiting cold coil structure depicted in Figure 3.6-10. A sketch of the coil assembly

Figure 3.6-6 Exploded View of Conductor Joint

Figure 3.6-7 Coil Module Fabrication Sequence

Figure 3.6-8 Coil Module Winding Machine (Conceptual Drawing)

Figure 3.6-9

Cross Section of Typical Coil Module

Figure 3.6-10

Cold Coil Structure: Plan View

station for the warm central cylinder design is presented in Figure 3.6-11. Refer to Appendix II for a thorough discussion of this phase of assembly. An inspection and test plan diagram covering the entire coil assembly process is included here as Figure 3.6-12.

3.6.5 Magnet Assembly Module

Once the coil assemblies are complete, they would be moved to the alignment bay of the off-site facility for careful fitup of the gravity supports and intercoil shear panels. Figures 3.6-13 and 3.6-14 suggest the type of fixtures and alignment equipment that will be required for this critical task. A sketch of a typical intercoil shear panel is presented in Figure 3.6-15.

3.6.6 Central Cylinder

The central cylinder is to be fabricated from forged and machined ring segments which are then assembled and aligned with thin sheets of insulation between them and, finally, pinned and bolted to form a rigid, transportable structure. Such an assembly is shown in Figure 3.6-16.

3.6.7 Dewar Fabrication

As previously indicated, the dewars for the warm central cylinder design are formed by the room temperature vessel and radiation shield surrounding each coil. To function as a dewar, the room temperature vessel must be vacuum tight. This is accomplished by means of seal-welding at the junction of the mating structural flanges of the vessel. Figure 3.2-1b shows the flange and seal-weld detail.

The dewar for the cold central cylinder design is a large vacuum vessel encompassing the toroidal system. For ease of fabrication, shipping, and assembly on site, the dewar will be constructed in sec-

tions. (See Figure 3.6-17.) The upper section covering the top and outer portions of the D-shaped coils will be made in eight segments, as will the lower sections underneath the coils. The dewar section lining the inside diameter of the central cylinder will be made in one piece. The D-shaped toroidal vessel that fits inside the coils and follows the clear bore perimeter will be made in eight pieces as well. Not shown are the dewar sections which line the elliptical access ports between all the coils. All sections bolt together at insulated flanges which are subsequently seal-welded.

The assembly procedure for this dewar is necessarily interactive with the progress of the toroidal field magnet assembly on site. Refer to Appendix II for a description of this procedure.

Figure 3.6-11

Cross Section of Coil Assembly

Figure 3.6-12

Inspection and Testing Plan Flow Diagram, Coil Assembly

Figure 3.6-13

Side View of Coil/Intercoil Shear Panel Alignment and Assembly at Factory

Figure 3.6-14

Top View of Coil/Intercoil Spacer Alignment and Assembly at Factory

Figure 3.6-15

Intercoil Shear Panel
Cold Central Cylinder Design

Figure 3.6-16
Bucking Cylinder Assembly

Figure 3.6-17

Dewar Sections for Cold Central Cylinder Design

3.7 MAINTENANCE FEATURES

3.7.1 Introduction

This section discusses the maintenance features of the 8 T warm bucking cylinder design and cold bucking cylinder design, based on the assumption that the general approach to maintenance for the EPR is by use of remote handling apparatus. Furthermore, it is assumed that these remote handling apparatus include a 9.09×10^5 kg (1,000 ton) capacity remotely-operated overhead crane/manipulator with a shielded personnel cab, floor-mounted remote manipulators for servicing the vertical positions and the lower areas of the magnet assembly. This section also outlines the steps that are necessary in order to remove and repair or replace a TF coil.

3.7.2 Warm Bucking Cylinder Design

From a maintenance viewpoint the magnet system can be considered to consist of four major component parts; i.e., bucking cylinder, bucking cylinder support, intercoil shear panels, and TF coil assemblies. Of these only the TF coil assemblies are active components and, consequently, require the most consideration from a maintenance point of view. The other three are passive; i.e., they are structural components only and not an active part of the magnet system.

The torque structure characteristics of the magnet assembly make it virtually impossible to repair the TF coil assemblies in place due to space limitations. This means the coil assembly must be removed to the hot cell (i.e., shielded available repair area) and repaired. It can then be replaced by a new coil assembly.

The coil design proposed for this program is one that facilitates the repair and replacement of damaged coils. The windings of the coil are of a modular design, and therefore, after detection, the defective module can

be removed and replaced by a new module. This avoids discarding an entire coil winding. A proposed maintenance procedure is as follows. After identification of the defective module, the factory is activated to produce a replacement module. Since there is a spare coil assembly on hand, the turn-around time for a new coil module could be a relatively short period of time. This means that a large number of completed spare coil modules would not be required, resulting in lower spare parts inventory costs. By the same token, a complete coil winding assembly would take much longer to fabricate and would probably have to be shipped with its structure. This plan would also help to minimize radioactive waste material.

The following is a basic outline of the steps that would be taken to remove a coil module and replace a new module once the coil assembly has been removed to the hot cell:

1. Detect defective coil module
2. Remove coil assembly cover plate
3. Remove G10 spacers and radiation shield sections
4. Unbolt side plate of cold structure
5. Remove defective coil module
6. Install new coil module which has been pretested at the factory
7. Test coil module electrically
8. Reinstall radiation shield sections and G10 spacers
9. Reset spring-loaded adjustment bolts on room temperature vessel
10. Final test of coil
11. Set aside as spare coil assembly

Commercially-available remote apparatus to perform several of the above procedures are available today. A developmental program to design and fabricate or modify existing commercial equipment would have to be undertaken to provide complete remote maintenance apparatus.

The design of the magnet assembly also provides for all the interfacing electrical and cryogenic connections to be external to the magnet assembly shields to facilitate their maintenance. This is based on the assumption that technicians could approach and handle these connections after a short period of time (e.g., 24 hours) safely.

The basic steps for removing and replacing a defective coil assembly is as follows. (This assumes the system has been deenergized and all LHe and LN₂ has been removed, as well as all connections external to the shield being disconnected. In addition, it is assumed all peripheral equipment has been moved out of the way).

1. Remove intercoil shear panels on either side of the defective coil
2. Remove coil assembly tie-down bolts at base
3. Lift and remove coil assembly to hot cell
4. Remove epoxy-resin filler pieces from nose area
5. Lift new coil assembly into position, align, and bolt to base
6. Machine threaded holes for spring-loaded shoulder bolts of shear panels using transfer drill template
7. Install intercoil panels
8. Cast epoxy-resin filler material between adjacent coils in nose area
9. Reconnect all electrical and cryogenic connections and prepare for cooldown

Careful consideration must be given to remote maintenance features which are consistent with the reactor operation cost. The following provisions for remote maintenance are inherent in the design.

1. Structural bolts are locked into position using a "locking cap" technique which is in use in present-day fusion nuclear power plants
2. Structural welds do not have to be removed from maintenance, i.e., only seal welds have to be cut
3. The intercoil shear panels have tapered alignment pins and spring-loaded bolts to ensure easy and proper assembly and disassembly

4. Tubes are provided which allow the epoxy resin filler material to be manually mixed out of the "hot" area and directed to the space between the noses of the coils
5. Components can be replaced, e.g., coil modules
6. Guides to facilitate the use of remote maintenance apparatus are provided

3.7.3 Cold Bucking Cylinder Design

The general comments with respect to maintenance features made earlier about the warm bucking cylinder design also apply to the cold bucking cylinder design.

An outline of basic steps that would be taken to remove a coil module and replace a new module is as follows:

1. Detect faulty coil
2. Remove the appropriate octant of the upper section of the dewar by cutting the seal weld
3. Remove faulty coil to the hot cell for repair
4. Unbolt the side plate of the cold structure
5. Remove defective coil module
6. Install new coil module
7. Test coil module electrically
8. Replace cold structure side plate and fasten in place
9. Final test of coil
10. Set aside as spare coil assembly

Comments--The cold bucking cylinder design has one major advantage over the warm bucking cylinder design; i.e., there are no spring-loaded adjustment bolts, G10 spacers, or radiation shields to remove and replace, which are difficult tasks to perform remotely, implying cost and time savings. On the other hand, removing an entire section of the dewar

(cold design) as compared to removing one side of one coil dewar (warm design) appears to be more difficult.

The downtime for the cold bucking cylinder design will probably be longer than that for the warm bucking cylinder design; i.e., a spare coil could be installed immediately after the faulty coil was removed for the warm bucking cylinder design; however, since the dewar is common to all coils the new coil must be installed and the upper section must be replaced, welded, and leak-tested before the magnet assembly can be energized.

In order to properly evaluate the pros and cons of each design with respect to maintenance features all aspects of the magnet assembly and system design must be studied. This is beyond the scope of this program and will be done during a future detail design program.

REFERENCES FOR SECTION 3

1. Wilson, M. N., et al, Experimental and Theoretical Studies of Filamentary Superconducting Composites, Rutherford High Energy Laboratory Preprint, RPP/A73, November 1969.
2. Stekly, Z. J. J.; Thome, R. J.; Strauss, B.; Principles of Stability in Cooled Superconducting Magnets, Proceedings 1968 Summer Study on Superconducting Devices on Accelerators, Brookhaven National Laboratory, BNL 50155, 1968, p. 748.
3. Ref. cit. Wilson.
4. Smith, P. F.; Wilson, M. N.; Walters, C. R.; Lewin, J. D.; BNL 50155, p. 913.
5. Johannes, C., Recent Advances in Heat Transfer to Helium I, Proceedings ICEC3, Berlin, 1970, p. 97.
6. Oak Ridge National Laboratory, ORNL-DWG-75-11056R.
7. Wells, J. M., et al, Structural Materials for Cryogenic Applications, Research Report 76-904-CRYMT-R1, NBS Contract No. CS-1-8304, April 1976.
8. Heim, J. R., The Heim Column, A Low Heat Leak Temperature Stabilized Support, NAL Report TM-334A.

4. CONCEPT FOR MAXIMUM PRACTICAL FIELD

4.1 Factors Determining Maximum Practical Field

The field level for the high field designs was selected as 12 T, an NbTi conductor operating at 2.5 K, utilizing the factors leading to this choice are: the ability of the conductor to operate at fields up to 12 T at temperatures below 4.2 K; the size, power requirements, and cost of refrigeration system to maintain the 2.5 K operating temperature; and the structure required for an EPR magnet system operating at 12 T.

The choice of NbTi as the superconductor was based on: its ready availability in large production quantities, its ease of fabrication into a high current carrying conductor, its mechanical integrity, its ability to operate in the high field strengths at reduced temperatures, and its relatively low conductor costs. The 12 T point was selected because available data showed this to be a reasonable operating limit for NbTi. It further appeared to be a reasonable limit when the massiveness of the required structure, which increases as the square of the magnetic field, is considered.

The choice of an operating temperature of 2.5 K imposes no burden on refrigerator technology nor does it result in exorbitant refrigerator costs. The primary consideration that must be given to operation at the lower temperature is the power that will be required to operate the refrigerator.

NbTi conductor and coil technology is well established and voluminous data on the operation of coils and conductors of various sizes and shapes is readily available. These data provide the background for a design that makes full use of currently available conductor technology and which as a consequence virtually eliminates all of the conductor related questions for a 12 T EPR toroidal field coil system.

As has been pointed out in the discussion of conductors for the 8 T design in this report, the amount of material actually exposed to the maximum field at the winding is very small. For a 12 T coil only 2.5 percent of the material is exposed to a magnetic field higher than 10 T. The situation is complicated by the fact that each turn of the winding has a variable field along its length, so that the maximum field occurs only in a relatively small region of the magnet. This naturally results in excess current carrying capacity everywhere in the magnet except in the very small region exposed to the high field. It is therefore possible to design using well known conductor characteristics to maintain the desired margin at the maximum field point knowing that much larger safety factors exist throughout the rest of the coil.

The conductor current densities (superconductor plus stabilizer) for the 12 T designs presented in this report range from a high of $6,050 \text{ A/cm}^2$ in the low field region to $2,540 \text{ A/cm}^2$ in the high field region. The current density in the superconductor in the high field region is $2.68 \times 10^4 \text{ A/cm}^2$, which is well below the critical value of $4.2 \times 10^4 \text{ A/cm}^2$.

In view of the fact that only a small fraction of the windings are actually exposed to the high fields and because high current density is not at a premium in EPR size magnets as it is in smaller magnets, the practical upper field limit for the effective utilization of NbTi is therefore higher in EPR type coils than it is for smaller scale magnets.

Historically, with few exceptions, superconducting coils have been designed to operate at 4.2 K, the boiling point of liquid helium at atmospheric pressure. The main reason for this is that 4.2 K occurs naturally by operating a system at atmospheric pressure. Temperatures lower than 4.2 K are generally obtained by starting at 4.2 K and pumping down to the desired pressure.

It has been recognized for many years that temperatures lower than 4.2 K led to improved coil performance (for NbTi as well as for Nb₃Sn). However, the design uncertainties associated with the large superconducting magnet systems built to date have restricted the use of these lower temperatures to relatively small laboratory type systems.

In large Tokamak magnet systems in which the coils are subjected to pulsed fields, the operating temperature should be that most suitable to achieve the overall system goals. In the case of NbTi conductors, lower temperatures significantly extend the magnetic fields at which they can be effectively used.

The lower temperatures however are achieved at the expense of additional power expenditure to run the room temperature helium compressors which are part of the refrigeration cycle.

For the particular system being considered, the pulsed field of 0.5 T for one second results in ac losses in the superconductor as well as eddy currents in the structure. The resultant heat load is by far the major component for the designs considered. Consequently, the capacity of the refrigerator is directly proportional to the square of the rate of change of pulsed magnetic field. Operation at 2.5 K requires approximately 70 per cent more refrigerator power than operation at 4.2 K with the same heat load. This 70 percent higher capacity represents a 44 percent increase in the cost of the refrigeration system. A change in the rate of change of the pulsed magnetic field of 30 percent would result in the same change in the refrigerator system power and cost. The refrigerator cost and power consumption depends more strongly on the pulse shape than it does on the refrigeration temperature.

Prohibitive heat loads can be reduced by relatively small changes in the pulsed magnetic field.

Figures 4.1-1 and 4.1-2 are plots depicting the refrigerator power and costs as a function of heat load for temperatures of 2.5 K and 4.2 K, respectively. The estimated values of heat load for the 12 T system are from 271 to 338 KW corresponding to refrigerator powers of 108 to 120 MW. Essentially, all of this heat load can be directly attributed to the rate of change of the pulsed magnetic field squared. A reduction in the exposure of the TF coils to this pulsed field by a factor of 5 either by shielding or simply by reducing the pulsed field itself would make the refrigerator power requirements relatively unimportant.

The conclusion that can be drawn relative to the refrigeration load for the 12 T design is that the power penalty of the refrigerator is due primarily to the pulsed magnetic field and only secondarily to the operation at lower temperatures.

The last primary considerations in choosing 12 T as an upper practical field were structural in nature. Because the forces in a system of fixed geometry and size scale as the square of the magnetic field, the 12 T magnet has loads approximately 2.25 times larger than an 8 T system of the same size. It was felt that while no real limit exists for the field that can be generated, fields higher than 12 T would require structural scaleups that are beyond reasonably straightforward extrapolations of today's superconducting coil technology.

4.1.1 Comparison of Major Design Parameters with 8 T Design

In selecting 12 T as the high field design, a comparison can be made of the major design parameters between the 8 T and 12 T designs. Table 4.1-1 shows the main parameters for the two designs, both using a cold central cylinder to react the radial forces.

Figure 4.1-1

Estimate of Installed Refrigerator Power as a Function of Heat Load at 4.2 K and 2.5 K

Figure 4.1-2

Estimated Refrigerator Costs as a Function of Total Heat Load
at 4.2 and 2.5 K

Table 4.1-1
Comparison of Characteristics of Cold Central Cylinder
Designs at 8 and 12 T

	<u>8 T</u>	<u>12 T</u>	<u>12 T/8 T</u>
Major Radius, m	7.57	7.84	1.04
Horizontal Bore, m	7.5	6.8	0.91
Vertical Bore, m	11.25	10.2	0.91
Magnetic Energy, GJ	25.9	73.8	2.85
Total Ampere-Turns (mA)	141	259	1.84
Hoop Tension/Coil (MN)	83.4	189	2.26
Radial Centering Force/ Coil (MN)	272	705	2.59
Force/Unit Length (nose region) (MN/m)	38	107	2.81
Lateral Force ($\frac{1}{2}$ coil) Due (MN) to Poloidal Field/Coil	30.8	54.6	1.77
Torque Due to Poloidal Field/ Coil (MN-m)	302	465	1.54
Lateral Force Under Fault (one coil) (MN)	264	740	2.80
Lateral Force Under Fault/length			
Nose Area (MN/m)	12.6	35.6	2.8
Low Field Area (MN/m)	3.3	10.8	3.27

Geometrically, the coils are similar; however, as is evident from the table, the system energy is up by a factor of 2.85 which affects the charging and discharging of the coil as well as the protection system. The total ampere turns are up by 1.84. This means significantly more conductor as well as higher ac losses due to the higher conductor volume.

With the exception of the lateral force (and torque) due to interaction with the poloidal field, all the forces are up by factors of 2.26 to 3.27 clearly showing the increase in structural support required. Specifically, the force per unit length in the nose region is up by a factor of 2.81 which is directly related to the bearing load the superconducting windings are subjected to and dictates the use of additional support of the windings.

4.2 WINDINGS

4.2.1 Cooling and Conductor Stability

The 12 T coil is designed to operate at 2.5 K using nucleate boiling helium to cool and stabilize the conductor. Since there is only a relatively small region that is actually exposed to the high field, special cooling could be provided for this region alone instead of attempting to operate the coil as a whole at 2.5 K. This has not been investigated in detail, and no account of this possibility has been taken in the designs.

The reasons for selecting a design with interconnecting cooling passages and using boiling helium is the same as was discussed for the 8 T design and will not be repeated here.

While the short sample characteristics of NbTi at lower temperatures are available, the question still remaining is how to design a conductor to operate at 2.5 K rather than 4.2 K. This can be answered by reviewing stability tests described in Reference 1. These tests used a heater embedded in the windings of a fully cryostatically stable (at 4.2 K) winding to determine onset of resistance with heater power and subsequent recovery (or lack thereof) as a function of temperature. The results are reproduced in Figure 4.2-1, which shows the curves of onset of resistance upon increasing heater power (solid curves labelled takeoff). Also shown are the heater powers at which recovery back to the superconducting state occurs (dotted lines). For any given temperature at the low current range, resistance and recovery occur at the same heater power; at the high current range there is no recovery.

The purpose of including the data in this report is to illustrate the effects of temperature on the winding stability rather than to specifically discuss coil behavior with a heater embedded.

Fig. 4.2-1. Heater Power at Takeoff and Recovery for Several Bath Temperatures

All of the takeoff curves, which are a measure of the cooling within the windings, vary very little as the temperature changes. Further, the recovery curves which are a direct measure of the conductor stability also vary less than 10 percent.

It can be concluded from this data that for the temperature range 2.4 to 4.2 K no major changes in conductor stability occurred for the fully stabilized coil tested.

As a result of this, the 2.5 K conductor was designed from a stability point of view as if it were to operate at 4.2 K (at the higher current density corresponding to the lower temperature). All other aspects of stability (such as strand sizes and conductor twisting) are equally as valid for the 12 T design as for the 8 T and will not be repeated here.

4.2.2 Conductor

In selecting the specific 12 T conductor, there are considerations such as ac losses and selection of copper cable as the stabilizer in which the 12 T considerations are the same as those for the 8 T design. However, such factors as higher stored energy, larger total forces per unit length, and operation at different temperature and higher field are specific to the 12 T design, and the discussion will focus only on those features in which the 12 T design considerations differ significantly from the 8 T design.

The selection of the operating current level is a compromise between larger currents which result in larger conductors, lower operating voltages, and higher conductor ac losses and lower currents which result in smaller conductors, higher operating voltages, and lower conductor ac losses.

Table 4.2-1 summarizes the charge and discharge characteristics as a function of current level. It shows at each current level the charging time and power supply rating for a given charge voltage for all the coils

Table 4.2-1

Charge and Discharge Voltages - 12 T Magnet

Coil Energy = 73.8×10^9 J

	<u>500 V</u>	<u>1 kV</u>	<u>2 kV</u>	<u>10 kV</u>
5,000 A - charge (min.)	984	492	246	49
- power (MW)	2.5	5	10	50
- discharge (min.)	61.5	30.75	15.4	3.06
10,000 A - charge (min.)	492	246	123	24.6
- power (MW)	5	10	20	100
- discharge (min.)	30.75	15.4	7.7	1.54
15,000 A - charge (min.)	328	164	82	16.4
- power (MW)	7.5	15	30	150
- discharge (min.)	20.5	10.25	5.13	1.03
20,000 A - charge (min.)	246	123	61.5	12.3
- power (MW)	10	20	40	200
- discharge (min.)	15.4	7.7	3.84	.77

in series. It also shows the discharge time (at the same voltage) if the system is discharged by inserting individual resistors in series between the coils.

The selection of 10,000 A as the design current was very heavily weighted by conductor ac loss when exposed to 0.5 T pulsed field (one second rise time). Consideration of the information in Table 4.2-1 above would normally have led to selection of a higher operating current. Because of the high field, more conductor grading is necessary in order not to unduly penalize the portions of the coil exposed to lower fields.

The conductors chosen for operation at each field are shown in Figure 4.2-2, and their characteristics are summarized in Table 4.2-2.

There are six different grades of conductor, with three high field grades at 10 T, 11 T and 12 T. The lower field grades are essentially the same as for the 8 T design.

To illustrate the margin in temperature and current density of the design points with respect to the transition into the normal state, Figure 4.2-3 shows the superconductor current density as a function of field and temperature. It shows the design points at 2.5 K at the respective values of field and current density. The margins in temperature and current density are similar to the 8 T design operating at 4.2 K.

Note: Dimensions in cm

Figure 4.2-2

Conceptual Conductors for 12 Wb/m² Torus Design

Table 4.2-2

12 T Magnet

Conductor Specifications

Operating Temperature 2.5 K

Characteristics of Component Strands

Material	Niobium titanium in copper matrix
Copper to Superconductor Ratio	1.8:1
Diameter	.187 cm (73.6 mils)
Filament Diameter	24.7 μm

Characteristics of Stabilized Conductor

Type of Construction	Rectangular stranded cable conductor (Rutherford type) with central insulated divider stabilized with additional stranded copper					
Grade	1	2	3	4	5	6
Maximum Field, T	12.0	11.0	10.0	8.0	5.3	2.7
Operating Current, kA	10.0	10.0	10.0	10.0	10.0	10.0
Critical Current, kA	13.7	13.4	14.1	20.8	13.5	14.8
Number of Component Strands	38	19	14	13	6	4
Overall Copper to Superconductor Ratio	10.5	16.4	20.4	20	33	42
Conductor Current Density kA/cm ²	2.54	3.26	3.56	3.90	5.17	6.05
Superconductor Current Density, kA/cm ²	26.8	53.7	72.9	78.5	171	254
Surface Heat Flux, W/cm ²	0.35	0.35	0.35	0.35	0.35	0.35
Overall Width, cm	0.962	0.962	0.962	0.962	0.962	0.962
Overall Height, cm	5.25	4.23	3.88	3.57	2.72	2.33

Critical Current Density of MCA Production Grade NbTi Composite Conductor
Showing 12 T Design Points

Figure 4.2-3

4.2.3 Winding Configuration

The winding configuration is similar to the 8 T design with cooling passages formed between conductors and between layers by means of G-10 insulating strips (see Figure 4.2-4).

The conductor is layer-wound (because of the need for grading) into winding modules, each of which is enclosed in a vacuum-tight thin walled case of stainless steel. These winding modules are inserted in a stainless steel support structure similar to the 8 T design, however, due to the larger forces on the conductor more internal support is required in the 12 T design.

The cross section of a winding module is shown in Figure 4.2-5 and is essentially the same as for the 8 T design except for the fact that different conductor is used.

The helium passages are all interconnected within a winding module to provide the maximum amount of helium to any potential hot spots.

The use of winding modules in the design potentially allows adjustment of the refrigeration system to provide more helium to those modules exposed to the highest fields or with the highest heat loads. This possibility requires further study, and was not included in the designs.

Figure 4.2-4. Schematic Illustrating Method of Interlayer and Interturn Insulation

Figure 4.2-5

Cross Section of Typical Coil Module

4.3 STRUCTURE AND DEWAR

The loads that the 12 T systems must be designed for are of the same type as the 8 T.

Under normal operation in a balanced system the loads on the coils are all in the plane of each coil. These result in a centering force in the windings in the nose region and a tension load in the windings not in the nose region similar to the loads present in a solenoid. For the design presented herein these centering forces are reacted by a central cylinder. The windings themselves are of a D shape which means that a tension (or minimum bending) structure can be used to support the windings.

In addition to the forces the coils exert on themselves, the fields imposed by poloidal coils result in an out-of-plane force on each coil that is equal and opposite on the top and bottom of each coil. A simple way of looking at these loads is that they generate a moment on the top of the coil system in a direction opposite to that generated on the bottom of the coil system. These moments must be connected by a structure which transmits the torque from the top of the coil to the bottom. Since the force per unit length on a coil is equal to its ampere turns times the externally applied vertical magnetic field of 0.4 T, it scales directly as the coil ampere turns. This torque can be transmitted either inside by the central support cylinder or outside by a torque structure. Because of space limitations, and because less material is required, the torque is transmitted through the outside.

For the 12 T case as well as the 8 T, two primary alternate designs were carried through; one with a warm central cylinder, the other a cold central cylinder.

For the warm central cylinder the coils are in individual room temperature dewars, and the torque structure is formed by the walls of the

individual dewars as well as by warm intercoil shear structure. For the case of a cold central cylinder the torque structure is formed by means of the walls of the cold coil and intercoil structure. From an eddy current viewpoint, the use of a cold central cylinder is less desirable. This is because the cold central cylinder design results in a large cold structural mass that is perpendicular to the pulsed field.

Other loads that the coils are subjected to are the loads resulting from operation of coils not in series. This may occur in several instances---the decay of current in an individual coil during a quench, individual coil energizing during testing. Operation in non-series mode results in out of plane loads in the coil with a current different from the rest of the system, as well as large out-of-plane load on coils adjacent to it. The change in the plane-loading configuration during charging or discharging a simple coil requires that the minimum bending structure be designed to withstand bending, or allow it to deflect to a minimum bending shape. The support of the out-of-plane loading requires additional intercoil structure over and above that already present for the torque transmittal.

Except for the necessary dimensional changes the 12 T design and its fabrication has the same overall configuration and material selection as the 8 T design.

Because of the general problem of lack of space to put structure and windings in the nose region (straight section of the D), a higher design stress level was used in certain instances than for the 8 T design.

Table 4.3-1 summarizes the material and design stress for the various components of the two 12 T designs---warm central cylinder and cold central cylinder. Also included is the conductor bearing stress which is a maximum in the straight leg of the D.

Table 4.3-1

12 T Material and Design Stress Summary

<u>Central Cylinder Design</u>				
<u>Component</u>		<u>Warm</u>		<u>Cold</u>
Cold Structure	Inconel 718	655 MN/m ²	Inconel 718	655 MN/m ²
	310S Stainless	379 MN/m ²	310S Stainless	379 MN/m ²
Conductor	NbTi/Cu Composite	38 MN/m ² *	NbTi/Cu Composite	30 MN/m ² *
Central (Bucking) Cylinder	Inconel 706	800 MN/m ²	310S Stainless	607 MN/m ² +
Intercoil Structure	Nitronic 33	193 MN/m ²	310S Stainless	379 MN/m ²
Dewar	Nitronic 33	193 MN/m ²	Nitronic 33	193 MN/m ²

*Bearing stress

+Fault loads control design; Design stress = .8 yield stress.

Equivalent design stress under normal operation <.5 yield stress.

Table 4.3-2 together with Figures 4.3-1 and 4.3-2 summarize the mechanical characteristics of the 12 T design using a warm central cylinder, which allows relatively easy removal of individual coils from the system. Because of the decrease in complexity in the dewar and intercoil structure, the access area is elliptical in shape rather than rectangular, and provides somewhat less than full access over the 90° angle.

Because the space is required between coils in the straight length of the D for individual dewars, the coil cross section is deep. The dimensions were arrived at by fixing the bore of the central region and the required horizontal bore in the coil itself, so that the major radius of the system varies as the depth of winding is changed. Of all the designs the 12 T warm central cylinder has the deepest cross section and the largest major radius.

For further discussion of design details of the 12 T warm central cylinder refer to the corresponding section of the 8 T design discussion.

The 12 T cold central cylinder design with a common single dewar was generated primarily to reduce the total depth of winding and decrease the major radius somewhat. In addition, the large centering forces and lateral forces in this design can be supported by cold central cylinder and cold intercoil structure. This reduces heat conduction heat load.

Table 4.3-3 together with Figures 4.3-3 and 4.3-4 summarize the 12 T cold central cylinder design. As can be seen, the major radius of the system is smaller than for a warm central cylinder design. Because of the significant decrease in weight and complexity of the torque transmission structure, this design also utilizes an elliptical access area rather than a rectangular one.

Fig. 4.3-1 12 T Warm Central Cylinder Design
Major Dimensions

Fig. 4.3-2 12 T Warm Central Cylinder Design: TF Coil Cross Section Along Straight Leg

Table 4.3-2

Summary of Mechanical Characteristics of Major Components:

12 T Warm Central Cylinder Design
(Elliptical Lateral Access Area)

I. Cold Coil Structure

A. Design Pressure

1. Internal (LHe containment): 0.69 MN/m^2 (100 psia)
2. External: vacuum

B. Weight Per Coil Assembly

1. Conductor: $1.37 \times 10^5 \text{ kg}$ ($3.01 \times 10^5 \text{ lbs}$)
 2. Structure: $3.03 \times 10^5 \text{ kg}$ ($6.68 \times 10^5 \text{ lbs}$)
- Total: $5.40 \times 10^5 \text{ kg}$ ($9.69 \times 10^5 \text{ lbs}$)

C. Material

1. Conductor: NbTi/Copper
2. Structure: 310S Stainless Steel & Inconel 718

D. Overall Coil Size

1. Height: 14.26 m (461 in)
2. Width: 10.86 m (428 in)
3. Thickness: 1.42 m (56 in)

II. Room Temperature Vessel (with radiation shield)

A. Design Pressure

1. Internal: vacuum
2. External: 0.10 MN/m^2 (14.7 psia)

B. Weight Per Coil Assembly

1. Room Temperature Vessel: $0.946 \times 10^5 \text{ kg}$ ($2.09 \times 10^5 \text{ lbs}$)
 2. Radiation Shield: $0.061 \times 10^5 \text{ kg}$ ($0.135 \times 10^5 \text{ lbs}$)
- Total: $1.007 \times 10^5 \text{ kg}$ ($2.22 \times 10^5 \text{ lbs}$)

C. Material

1. Room Temperature Vessel: Nitronic 33 Stainless Steel
2. Radiation Shield: Aluminum
3. Low Conduction Spacers: G10

D. Overall Size

1. Height: 19.45 m (766 in)
2. Width: 11.34 m (446 in)
3. Thickness: 2.17 m (85 in)

Table 4.3-2 (Continued)

III. Intercoil Structure

A. Weight Per Coil

1. (2) Outer Panels: 0.476×10^5 kg (1.05×10^5 lbs)
2. (2) Inner Panels: 0.346×10^5 kg (0.76×10^5 Lbs)
- Total: 0.822×10^5 kg (1.81×10^5 lbs)

B. Material: Nitronic 33 Stainless Steel

C. Overall Size

1. Outer Panel:

Length: 960 m (378 in)
Width: 3.66 m (144 in)
Height: 3.35 m (132 in)

2. Inner Panel

Length: 7.61 m (300 in)
Width: 2.83 m (111 in)
Height: 2.53 m (100 in)

IV. Bucking Cylinder

A. Weight: 3.642×10^5 kg (8.022×10^5 lbs)

B. Material: Inconel 706

C. Overall Size

1. Height: 6.38 m (251 in)
2. Width: 6.00 m (236 in)

V. Gravity Support - Cold Coil Structure

A. Weight Per Coil: 0.09×10^5 kg (0.20×10^5 Lbs)

B. Material (Heim Column)

1. Inner Cylinder: G10
2. Middle Cylinder: Aluminum
3. Outer Cylinder: G10
4. Hinge: Titanium

C. Overall Size

1. Height: 2.54 m (100 in)
2. Width: 1.14 m (45 in)

Fig. 4.3-3 12 T Cold Central Cylinder Design
Major Dimensions

Fig. 4.3-4 12 T Cold Central Cylinder Design: TF Coil Cross Section Along Straight Leg

Table 4.3-3

Summary of Mechanical Characteristics of Major Components:

12 T Cold Central Cylinder Design

(Elliptical Lateral Access Area)

I. Cold Coil Structure

A. Design Pressure

1. Internal (LHe containment): 0.69 MN/m^2 (100 psia)
2. External: vacuum

B. Weight Per Coil

1. Conductor: $1.24 \times 10^5 \text{ kg}$ ($2.74 \times 10^5 \text{ lbs}$)
2. Structure: $2.80 \times 10^5 \text{ kg}$ ($6.16 \times 10^5 \text{ lbs}$)
- Total: $4.04 \times 10^5 \text{ kg}$ ($8.90 \times 10^5 \text{ lbs}$)

C. Material

1. Conductor: NbTi/Copper
2. Structure: 310S Stainless Steel & Inconel 718

D. Overall Coil Size

1. Height: 13.39 m (527 in)
2. Width: 9.65 m (380 in)
3. Thickness: 2.11 m (83.0 in)

II. Dewar

A. Design Pressure

1. Internal: vacuum
2. External: 0.10 MN/m^2 (14.7 psia)

B. Weight

1. Room Temperature Vessel: $7.70 \times 10^5 \text{ kg}$ ($16.98 \times 10^5 \text{ lbs}$)
2. Radiation Shield: $1.00 \times 10^5 \text{ kg}$ ($2.19 \times 10^5 \text{ lbs}$)
- Total: $8.70 \times 10^5 \text{ kg}$ ($19.17 \times 10^5 \text{ lbs}$)

C. Material

1. Room Temperature Vessel: Nitronic 33 Stainless Steel
2. Radiation Shield: Aluminum
3. Low Conduction Spacers: G10

D. Overall Size

1. Height: 19.14 m (754 in)
2. Width: 26.68 m (1,050 in)

III. Intercoil Structure (Cold)

A. Weight Per Coil

1. (2) Outer Panels: $0.468 \times 10^5 \text{ kg}$ ($1.04 \times 10^5 \text{ lbs}$)
2. (2) Inner Panels: $0.254 \times 10^5 \text{ kg}$ ($0.56 \times 10^5 \text{ lbs}$)
- Total: $0.722 \times 10^5 \text{ kg}$ ($1.60 \times 10^5 \text{ lbs}$)

Table 4.3-3 (Continued)

III. Intercoil Structure (Cold)-(Continued)

B. Material: 310S Stainless Steel

C. Overall Size

1. Outer Panel

Length: 9.27 m (365 in)

Width: 3.56 m (140 in)

Height: 3.56 m (140 in)

2. Inner Panel

Length: 7.87 m (310 in)

Width: 2.41 m (95 in)

Height: 2.48 m (98 in)

IV. Bucking Cylinder (Cold)

A. Weight: 4.41×10^5 kg (9.73×10^5 lbs)

B. Material: 310S Stainless Steel

C. Overall Size

1. Height: 6.10 m (240 in)

2. Width: 6.00 m (236 in)

V. Gravity Support - Cold Coil/Intercoil Panel Structure

A. Weight Per Coil: 0.10×10^5 kg (0.22×10^5 lbs)

B. Material (Heim Column)

1. Inner Cylinder: G10

2. Middle Cylinder: Aluminum

3. Outer Cylinder: G10

4. Hinge: Titanium

C. Overall Size

1. Height: 2.54 m (100 in)

2. Width: 1.27 m (50 in)

VI. Gravity Support - Cold Bucking Cylinder

A. Weights

1. Truncated Cone: 0.190×10^5 kg (0.42×10^5 lbs)

2. Heim Columns: 0.10×10^5 kg (0.22×10^5 lbs)

B. Material

1. Truncated Cone: 310S Stainless Steel

2. Heim Columns: See V.B.

C. Overall Size

1. Truncated Cone

Height: 1.78 m (70 in)

Width: 6.64 m (261 in)

2. Columns (4)

Height: 2.54 m (100 in)

Width: 0.69 m (27 in)

4.4 COIL PROTECTION

The 12 T coil systems require protection against the same faults recognized for the 8 T system. Since the current densities of the 12 T and 8 T systems are of the same order, the main difference is the higher magnetic field energy.

For failure of the cooling system (or vacuum failure), rapid discharge of all the coils (in series) is required in a time fast enough not to cause overheating in the conductor and yet slow enough not to cause internal arcing due to high voltages which might exist across any normal regions in the winding.

For electrical faults, the faulted coil must be isolated and separately discharged either by putting it across resistors or by removing the helium supply to the coil and making it fully resistive.

Further details of this procedure as well as the circuitry used are discussed in Section 3.3 on the protection of the 8 T designs.

It was shown in the discussion of the 8 T design that discharge of a superconducting winding with the current densities used with a time constant larger than 120 seconds can cause overheating. Therefore, the discharge of the 12 T system with approximately three times the stored energy must be done either by increasing the discharge voltage which is generally undesirable or by further breaking up the system to lower the voltage.

In addition to using resistors in series between each coil, a set of leads is brought out at the midpoint of each coil as shown in Figure 4.4-1.

Table 4.4-1 compares the voltages during rapid discharge with and without the extra resistors to the voltages existing under normal charge and discharge.

The choice between providing higher voltage ground insulation around the modules and providing one extra set of leads in each coil (16 extra

pairs of leads) is one between extra insulation and extra lead heat load. However, these leads need not be designed for steady-state operation and as such can be designed to provide minimum heat leak while not carrying current. They need only be designed to provide the 2 minute time constant rapid discharge.

The rapid discharge of a coil with an internal short circuit may induce large currents in the faulted coil. These result in heating as well as large in-plane forces on the coil itself and out-of-plane forces on its neighbors. Because of the induced current problem it is not possible to predict the optimum means to discharge a coil with an internal short circuit so as to minimize additional damage.

The procedure proposed is to isolate the faulted coil from the rest of the system and to keep the system energized while the shorted coil is discharged.

The discharge of the faulted coil itself should first be attempted by slow discharge by connecting resistors across the coil terminals. If the internal short is such that the coil will not discharge using this procedure, resistance must be induced in the windings artificially. This can be done by having heaters within the windings which can be energized. Along with this, the helium supply should be reduced or completely interrupted to drive additional regions normal.

The protection of a large coil system of this magnitude is a complex undertaking and the above concepts need to be further evaluated (as well as alternates considered in order to develop confidence in protection schemes for EPR size magnets).

Figure 4.4-1 Schematic Showing Additional Series Discharge Resistor Between Two Coil Halves to Reduce Discharge Voltage.

Table 4.4-1

Operation of 12 T Protection System

	<u>Time</u>	<u>Max Coil Terminal Voltage</u>	<u>Max Voltage to Ground</u>	<u>Max Power Level</u>
Normal Charge:				
All Coils in Series	4.1 hrs.	62.5 V	1 kV	10 MW
Normal Discharge:				
All Coils in Series	4.1 hrs.	62.5 V	1 kV	10 MW
Rapid Discharge:				
All Coils in Series with Individual Resistor	2 min.	7.68 kV	7.68 kV	-
Each Coil in Two Halves Each in Series with a Resistor	2 min.	3.84 kV	3.84 kV	-

4.5 OTHER HEAT LOADS AND SUMMARY

The other sources of heat leak considered are directly analogous to those evaluated for the 8 T designs; i.e. conduction through supports resisting magnetic and gravitational forces, thermal radiation, nuclear radiation and current lead losses. Tables 4.5-1 and 4.5-2 summarize the heat loads determined for the 12 T warm and cold central cylinder designs, respectively. It is apparent that the larger loads are those from AC losses and eddy currents resulting from the pulsed field of 0.5 T. Heat input from thermal and nuclear radiation are insignificant by comparison. Electrical lead losses are also small. In short, only thermal conduction loads in the warm central cylinder design are significant compared to the heat loads of electromagnetic origin.

Table 4.5-1

Summary of Heat Input from All Sources
12 T Warm Central Cylinder Design

<u>Source</u>	<u>Max. Pulsed Field</u>	
	<u>0.5 T</u>	<u>0.1 T</u>
Heat Load at LHe (2.5K)		
Conductor AC Loss	133.1 KW	6.0 KW
Structure Eddy Current	90.0	4.0
Thermal Radiation	0.2	0.2
Thermal Conduction	36.7	36.7
Nuclear Radiation	0.6	0.6
16 Pairs of Electrical Leads	1.8*	1.8*
Total	271 KW	49.3 KW
Refrigeration Power at 300 K	96 MW	21 KW
Heat Load at LN ₂ (76 K)		
Thermal Radiation	43 KW	43 KW
Thermal Conduction	940	940
Total	983 KW	983 KW
Refrigeration Power at 300K	8+ MN	8+ MN
Total Refrigeration Power	104 MW	29 MW

*Equivalent heat load

+Assumes 35% Carnot efficiency

Table 4.5-2

Summary of Heat Input from All Sources
12 T Cold Central Cylinder Design

<u>Source</u>	<u>Max. Pulsed Field</u>	
	<u>0.5 T</u>	<u>0.1 T</u>
Heat Load at LHe (2.5 K)		
Conductor AC Loss	121.3 KW	5.4 KW
Structure Eddy Current	214.1	8.6
Thermal Radiation	0.2	0.2
Thermal Conduction	0.1	0.1
Nuclear Radiation	0.8	0.8
16 Pairs of Electrical Leads	1.8*	1.8*
Total	338 KW	16.9 KW
Refrigeration Power at 300 K	117 MW	8.2 MW
Heat Load at LN ₂ (76 K)		
Thermal Radiation	38 KW	38 KW
Thermal Conduction	1	1
Total	39 KW	39 KW
Refrigeration Power at 300 K	0.3+ MW	0.3+ MW
Total Refrigeration Power	117.3 MW	8.5 MW

*Equivalent heat load

+Assumes 35% Carnot efficiency

4.6 FABRICATION PLAN

This section discusses the fabrication plan of the 12 T magnet design that has been tentatively chosen for use on this program. In essence, the plan that was outlined for the 8 T magnet design in Section 3.6 and Appendix II can be applied directly in this case. The major components, secondary components, and miscellaneous parts remain the same with respect to function, quantity, and geometry. However, because the generated magnetic field is considerably greater, the weight of the structure increases by 30 to 40 percent which means that the dewar components and other parts also increase in size and weight.

The increase in size and weight of the structure, dewar components and other parts does not present any insurmountable manufacturing problems as such but requires more material and fabrication time with respect to shaping, machining, welding, handling, etc. and consequently implies longer manufacturing schedules and higher costs.

4.7 MAINTENANCE FEATURES

The maintenance features of the 8 T design that were discussed in Section 3.7 also can be applied directly to the 12 T design. Basically speaking, the only difference between the two is the difference in size and weight; i.e., the 12 T design is considerably larger than the 8 T design. This means that the lifting capacity of the overhead crane should increase accordingly as well as capacities and sizes of other remote apparatus.

Section 4 References

1. Stekly, Z. J. J.; Lucas, E. J.; deWinter, T. A.; Strauss, B.; DiSalvo, F.; Results of Tests for an 88 kg, 51 cm Bore Diameter Solenoid, JAP Vol. 39 No. 6, May 1968.

5. COST AND SCHEDULE ESTIMATE

5.1 Cost Estimate

Sections 3 and 4 of this report respectively describe design concepts of 8 T and 12 T EPR toroidal coil systems.

Three different concepts are presented in Section 3 for the 8 T magnets. The first two deal with systems that make use of separate vacuum vessels for each of the sixteen coils that make up the toroids. These two systems differ in the shape of the access area. The first, Design I, makes use of a rectangular-shaped access area; while the second, Design Ia, makes use of an oval-shaped access area. The third concept described in Section 3 is for a system that makes use of a common vacuum vessel for all sixteen coils of the torus, Design II.

Two concepts are presented in Section 4 for the 12 T systems. These differ in that the first makes use of separate vacuum vessels for each of the sixteen coils, Design III; while the second makes use of a common vacuum vessel for all sixteen coils, Design IV.

Tables 5.1-1 through 5.1-5 contain the estimated costs for each of the five systems mentioned above. The costs are broken down to cover the major assemblies that make up each magnet system. Tables 5.1-6 through 5.1-10 list the bulk materials that will be used in the fabrication of each of the major assemblies, where appropriate, together with the estimated labor hours required for the fabrication of these assemblies. Engineering costs for analysis, design, drafting, and vendor surveillance are presented as a single item. All costs are in millions of 1976 dollars.

Descriptions of each of the major assemblies listed in Tables 5.1-1 through 5.1-10 can be found in Sections 3 and 4 of this report.

Table 5.1-1

Estimated Costs for the 8 T Warm
Central Cylinder Design: Design I
(Millions of 1976 Dollars)

<u>Component</u>	<u>Material Costs</u>	<u>Labor Costs</u>	<u>Total Costs</u>
Coil Module	12.3	7.3	19.6
Cold Coil Structure	10.6	16.1	26.7
Room Temperature Vessel	5.0	10.0	15.0
Radiation Shield	0.1	0.3	0.4
Coil Assembly	-	3.0	3.0
Intercoil Shear Panels	0.9	1.8	2.7
Magnet Assembly Module	-	3.0	3.0
Central Cylinder	2.5	1.8	4.3
Engineering			<u>10.0</u>
GRAND TOTAL			84.7

Table 5.1-2

Estimated Costs for the 8 T Warm
Central Cylinder Design: Design Ia
(Millions of 1976 Dollars)

<u>Component</u>	<u>Material Costs</u>	<u>Labor Costs</u>	<u>Total Costs</u>
Coil Module	12.3	7.3	19.6
Cold Coil Structure	10.6	16.1	26.7
Room Temperature Vessel	2.5	5	7.5
Radiation Shield	.1	.3	.4
Coil Assembly	-	2.5	2.5
Intercoil Shear Panels	1.8	3.6	5.4
Magnet Assembly Module	-	2.7	2.7
Central Cylinder	2.5	1.8	4.3
Engineering			<u>10.0</u>
GRANT TOTAL			79.1

Table 5.1-3

Estimated Costs for the 8 T Cold
Central Cylinder Design: Design II
(Millions of 1976 Dollars)

<u>Component</u>	<u>Material Costs</u>	<u>Labor Costs</u>	<u>Total Costs</u>
Coil Module	11.2	6.7	17.9
Cold Coil Structure	11.6	17.6	29.2
Coil Assembly	-	2.1	2.1
Intercoil Shear Panels	2.3	3.5	5.8
Magnet Assembly Modules	-	2.3	2.3
Central Cylinder	1	1.6	2.6
Dewar Vessel	2.3	4.8	7.1
Engineering			<u>10.0</u>
GRAND TOTAL			77.0

Table 5.1-4

Estimated Costs for the 12 T Warm
Central Cylinder Design: Design III
(Millions of 1976 Dollars)

<u>Component</u>	<u>Material Costs</u>	<u>Labor Costs</u>	<u>Total Costs</u>
Coil Module	24.8	13.8	38.6
Cold Coil Structure	25.5	26.7	52.2
Room Temperature Vessel	4.2	8.3	12.5
Radiation Shield	.2	.6	.8
Coil Assembly	-	4.4	4.4
Intercoil Shear Panels	3.6	7.2	10.8
Magnet Assembly Module	-	4.7	4.7
Central Cylinder	3.3	2.9	6.2
Engineering			<u>12.0</u>
GRAND TOTAL			142.2

Table 5.1-5

Estimated Costs for the 12 T Cold
Central Cylinder Design: Design IV
(Millions of 1976 Dollars)

<u>Component</u>	<u>Material Costs</u>	<u>Labor Costs</u>	<u>Total Costs</u>
Coil Module	22.6	12.5	35.1
Cold Coil Structure	23.5	24.6	48.1
Coil Assembly	-	3.3	3.3
Intercoil Shear Panels	4.2	6.4	10.6
Magnet Assembly Module	-	3.6	3.6
Central Cylinder	1.6	2.4	4.0
Dewar Vessel	2.4	4.8	7.2
Engineering			<u>12.0</u>
GRAND TOTAL			123.9

Table 5.1-6

Materials and Estimated Labor Hours for Fabrication
of 8 T Warm Central Cylinder Design: Design I

(Thousands of Hours)

<u>Component</u>	<u>Materials</u>	<u>Estimated Labor Hours</u>
Coil Module	Stabilized NbTi	
	Superconductor	293
	304L Stainless Steel	74
Cold Coil Structure	310S Stainless Steel	800
Room Temperature Vessel	Nitronic 33	495
Radiation Shield	Aluminum	16
Coil Assembly	-	150
Intercoil Shear Panels	Nitronic 33	89
Magnet Assembly Module	-	150
Central Cylinder	Inconel 706	87
	Nitronic 33	4

Table 5.1-7

Materials and Estimated Labor Hours for Fabrication
 of 8 T Warm Central Cylinder Design: Design Ia
 (Thousands of Hours)

<u>Component</u>	<u>Materials</u>	<u>Estimated Labor Hours</u>
Coil Module	Stabilized NbTi	
	Superconductor	293
	304L Stainless Steel	74
Cold Coil Structure	310S Stainless Steel	800
Room Temperature Vessel	Nitronic 33	250
Radiation Shield	Aluminum	16
Coil Assembly	-	125
Intercoil Shear Panels	Nitronic 33	180
Magnet Assembly Module	-	135
Central Cylinder	Inconel 706	87
	Nitronic 33	4

Table 5.1-8

Materials and Estimated Labor Hours for Fabrication
of 8 T Cold Central Cylinder Design: Design II

(Thousands of Hours)

<u>Component</u>	<u>Materials</u>	<u>Estimated Labor Hours</u>
Coil Module	Stabilized NbTi Superconductor	265
	304L Stainless Steel	70
Cold Coil Structure	310S Stainless Steel	880
Coil Assembly	-	105
Intercoil Shear Panels	310S Stainless Steel	174
Magnet Assembly Module	-	113
Central Cylinder	310S Stainless Steel	81
Dewar	Nitronic 33	216
	Aluminum	28

Table 5.1-9

Materials and Estimated Labor Hours for Fabrication
of 12 T Warm Central Cylinder Design: Design III
(Thousands of Hours)

<u>Component</u>	<u>Materials</u>	<u>Estimated Labor Hours</u>
Coil Module	Stabilized NbTi Superconductor	612
	304L Stainless Steel	74
Cold Coil Structure	310S Stainless Steel	999
	Inconel 718	334
Room Temperature Vessel	Nitronic 33	416
Radiation Shield	Aluminum	27
Coil Assembly	-	220
Intercoil Shear Panels	Nitronic 33	362
Magnet Assembly Module	-	236
Central Cylinder	Inconel 706	140
	Nitronic 33	6

Table 5.1-10

Materials and Estimated Labor Hours for Fabrication
of 12 T Cold Central Cylinder Design: Design IV

(Thousands of Hours)

<u>Component</u>	<u>Materials</u>	<u>Estimated Labor Hours</u>
Coil Module	Stabilized NbTi Superconductor	558
	304L Stainless Steel	70
Cold Coil Structure	310S Stainless Steel	923
	Inconel 718	307
Coil Assembly	-	165
Intercoil Shear Panels	310S Stainless Steel	818
Magnet Assembly Module	-	180
Central Cylinder	310S Stainless Steel	121
Dewar	Nitronic 33	212
	Aluminum	27

5.2 Program Schedule

Figure 5.2-1 is an overall schedule for a program to design and construct a toroidal field superconducting magnet system for the Experimental Power Reactor. It applies approximately to all the concepts presented. As shown, the program is divided into four tasks. The first task will cover a period of six months and will be devoted to: a review of the work accomplished to date; the verification of the availability of the industrial capabilities required to fabricate the magnet system; and, an identification of those areas requiring further development.

Based on the information gathered under Task 1 an Engineering Design will be generated under Task 2. This task will have a duration of one year. In addition to producing the engineering design, the development work identified under Task 1 will be initiated.

Task 3 will be devoted to: the completion of the required development work and a detailed design; a survey of qualified vendors; and, the issuance of bids followed by purchase orders. It has a planned duration of fifteen months with a possible extension to twenty-one months.

Task 4 will be devoted to the fabrication of the coil system. Figures 5.2-2 and 5.2-3 are more detailed breakdowns of the work that will be accomplished under this task. Figure 5.2-2 is relevant to the Warm Central Cylinder Designs and Figure 5.2-3 to the Cold Central Cylinder Designs.

TASK \ ELAPSED YEARS	1	2	3	4	5
1. REVIEW CONCEPTUAL DESIGNS AND LCP RESULTS ESTABLISH INDUSTRIAL CAPABILITIES IDENTIFY AREAS OF DEVELOPMENT
2. ENGINEERING DESIGN ESTABLISH QUALITY ASSURANCE PROCEDURES SURVEY VENDORS ESTABLISH PRODUCTION SCHEDULES ORDER LONG LEAD TIME ITEMS PERFORM SUPPORTING EXPERIMENTS INITIATE REQUIRED DEVELOPMENT WORK
3. FINAL DESIGN COMPLETE REQUIRED DEVELOPMENT WORK SELECT VENDORS ISSUE RFPs & NEGOTIATE CONTRACTS FOR ALL PARTS PROCURE SPECIAL TOOLING
4. FABRICATE COIL SYSTEMS MONITOR VENDOR PERFORMANCE PERFORM QA TESTS ASSEMBLE COILS SHIP COILS

FIGURE 5.2-1
EXPERIMENTAL POWER REACTOR TOROIDAL MAGNET SYSTEM

Figure 5.2-2
Manufacturing Schedule for Warm Central Cylinder Designs

Figure 5.2-3
Manufacturing Schedule for Cold Central Cylinder Designs

6. UNCERTAINTIES IN THE DESIGN

The uncertainties in the designs can be broken down into those primarily oriented toward the conductor and its cooling and those primarily oriented toward the structural components, including the dewar.

The fact that this design study was performed over a period of six months limited the degree of iteration that could be obtained in the designs presented. The designs presented were the ones arrived at during the study, however, further study and detailed analysis as well as feedback would undoubtedly improve the designs as more and more details are considered toward a finalized design concept.

As such the uncertainties can be of the type that might be resolved by further design optimization or analysis on the one hand, as well as of the more fundamental uncertainties which occur when a design is based on assumptions that may be subject to change. Both types of uncertainties exist in the design presented. The following is a brief discussion of these major uncertainties.

6.1 Magnitude And Rate Of Change Of Pulsed Field

The single most important uncertainty is the magnitude and rate of change of the pulsed field as well as its direction and spatial distribution. While the specified values for the study of 0.5 T in a 1 second ramp with an optional reduction to 0.1 T in a 1 second ramp are unambiguous and provide no uncertainty, the fact that the maximum value of the pulsed field determines the torque reaction requirements and that its rate of change affects the choice of conductor and the resultant ac losses places these variables at the head of the uncertainty list. Their interaction with the design is direct and of primary importance.

The magnitude of the pulsed field determines the lateral forces exerted on the top and bottom of the coils---these result in higher structural weights and higher conduction heat loads in the individual dewars. In addition, at 0.5 T in 1 second the conductor ac losses and structural eddy currents which are proportional to the square of the pulsed field magnitude exceed all other heat loads, consequently they also determine the refrigeration power level.

The optional reduction to 0.1 T reduces both the maximum torque as well as the heat loads, however, this is done using poloidal coils inside the toroidal field coils. While this is generally desirable from the toroidal field coil point of view, the complete system interaction needs to be considered.

While the structural eddy current losses in a structural component can be readily calculated from a spatial distribution of poloidal pulsed field, the simple specification of 0.5 T maximum and a 0.7 spatial overall can result in estimates of eddy currents that are made by:

1. Calculating the average value of field change and assuming this average value at all points in the winding.
2. Calculating the loss at the maximum field point multiplying by 0.7 to arrive at the spatial average and applying it to the whole winding volume.
3. Assume a uniform distribution of field from 0.2 T to 0.5 T within the winding.

These methods give estimates of eddy currents on conductor ac losses that compare as 1.225, 1.75, 1.0 for each of the three methods above.

The results were computed using method 1, however, if the distribution of the windings is linear with the pulsed field then a 18.4% reduction in eddy currents and ac losses could result.

The theory of ac loss calculation for superconductor is well developed, however, values of substrate resistivity and contact resistances are not that well known, and are likely to vary with load. Comparisons with theory to better than a factor of two is excellent agreement. It is therefore necessary to measure the ac losses for the particular conductor to be used in order to accurately predict the ac losses for a coil.

Because of the high sensitivity of the design to the magnitude of the pulsed field, the uncertainties are much reduced at the 0.1 T values because the eddy currents, ac losses no longer completely overshadow other sources of heat load.

6.2 Conductor Uncertainties

The uncertainties having to do with the conductor fall into the category of determining the limits of stable operation of the conductor in the time-varying magnetic field, and of the ability of the conductor and insulation to withstand continuous repetitive pulsed loads.

The conductor choice for all the designs was dictated primarily by pulsed field considerations in order to minimize ac conductor losses and as such the general uncertainties associated with the magnitude of the pulsed field apply to the conductor.

The stability computations used in sizing the conductor made use of surface heat flux values of 0.35 W/cm^2 , a conservative value in view of the fact that effective values as high 1 W/cm^2 have been measured. This points to the fact that performance measurements on specific conductors and simulated coil section must be made in order to accurately pin down--- not the effective heat flux, which is after all only a convenience for comparison with theory---but the maximum value of current in a specific configuration that will allow the conductor to revert back to being superconducting after a transition into a normally resistive state. It is this

value of current that represents the limit of stable operation for a particular conductor and cooling passage configuration.

The repetitive lateral pulsed loads exerted on the conductor by means of the poloidal coils represents an unknown area from a mechanical point of view. Mechanical properties of cabled conductors---and fatigue properties specifically are not known. Again, the severity of this uncertainty relates directly back to the magnitude of the pulsed field.

The 12 T conductor design is based on single strand data on NbTi at low temperature with stability estimates based on limited coil tests at temperatures below 4.2 K.

More data on the performance of specific conductors and cooling passage configurations are necessary to pin down more exact values of the limits of stability at temperatures below 4.2 K. There is ample current-carrying capacity in NbTi conductors at 2.5 K and 12 T, it is the stability requirements that present the major uncertainty.

6.3 Cryogenic Uncertainties

The primary function of the cryogenic system is to supply helium to cool and stabilize the superconducting windings.

The helium containers have two legs---one going up at the straight section of the D where the high field exists, the other rises in a curve from the bottom to the top of the coil.

While the average values of total heat generation over the whole pulse do not make excessive demands on the coolant supply, the fact that the pulse occurs in 1 second results in a large rapid input of heat to the helium. This heat pulse is a result of eddy currents, ac losses and neutron heating. The effect on the internal temperature distribution and the replacement of helium liquid in regions of high heat input need to be examined in more detail.

Since the amount of heat is proportional to the square of the magnitude of the pulsed field a significant fraction of the uncertainties in the cryogenic system design relate directly to the design of the poloidal field coils.

6.4 Structure

The structure as a whole is the largest cost contributing item, consequently its weight (and cost) should be properly minimized. The scope of the study was directed at identifying the main loads and designing suitable structures compatible with requirements of manufacturing and assembly into the system.

The loads considered in the design of the system included the normal operating loads due to the toroidal field and the cyclic loads resulting from the poloidal coils. Also considered in the design were loads that result from operation of a single coil at currents different from the rest of the system. These occur if the coil is self-energized during testing, or separately discharged in the system as in the case of an electrical fault or vacuum failure.

The assumption that the coil will be operated separately results in the following structural interactions---unbalanced loads occur on the central support cylinder, large lateral loads occur in adjacent coils, in-plane loads differ from the pure tension mode requiring the design of structure for bending. This assumption was made because of safety and protection considerations---nevertheless it does have a significant impact on the structural component weights.

To the extent that more detailed analysis is capable of generating improved schemes and more effective structural designs there is

an uncertainty associated with necessity of including all of the unbalanced loads referred to above.

6.5 Others

There are other uncertainties associated with the designs, however, these can generally be classed as those having to do with designing and building a coil for the first time. Designs evolve during the detailed design stage and can be backed up by supporting tests that prove out or require modification of design details. These uncertainties are common to the first of a kind of any large system and are not peculiar to CPR coils in particular.

In general, a high degree of confidence has been generated in the 8 T designs and the uncertainties associated with the designs are of a type that yield readily to analysis and direct measurement.

The 12 T designs contain more uncertainties primarily because of structural considerations. This is in part due to the emphasis placed on the 8 T designs during the study thus limiting the effort on the more severe structural requirements of the 12 T.

7. RECOMMENDED TEST COIL SIZE AND TEST CONDITIONS

7.1 Introduction

In selecting the minimum size of coil to build, to test the design concept and to verify the design details, it is necessary to set down the goals or purpose that building the coil seeks to achieve.

Each of the EPR coils as conceived in this report consists of eight separate winding modules, each sealed in its own helium vessel, each of which contains approximately $1/8$ of the ampere turns of a coil or $1/128$ of the total ampere turns of the whole system. As such the system is designed in a way that tests on smaller coils with ampere turns considerably lower than any of the EPR coils can be very meaningful.

From a conductor and winding point of view, the primary variables are the magnetic field level, the magnitude and rate of change of pulsed fields to which it is subjected and the details of the helium cooling system.

The stability of the conductor is a local condition, and as such can be simulated to some extent in smaller coil sections exposed to external magnetic fields. This can simulate the conductor performance and to some extent the stability of the conductor in the particular configuration of windings, however it is difficult to simulate the internal state of stress, within the winding. As has been shown in Section 3 of this report, the initiation of normal regions depends very strongly on the internal state of stress within the winding. Except for this factor, adequate simulation can be obtained in small coils or simulated coil sections.

The load per unit length on a conductor is determined by the current and local magnetic field, consequently if it carries the same current and is exposed to the same magnetic field, the same

loading conditions will exist in the test coil as in the full size system. Further, as much as possible the conductor should be subjected to the same magnitude and same rate of change of poloidal field as is expected in the full size system. This simulates the heating per unit volume as well as the magnitude of pulsed load per unit length of conductor.

There are two areas where bearing stress on the conductor is important -- the high field region where the conductor is essentially straight and the coil is supported by the central cylinder, and the top and bottom of the coil where the poloidal field results in lateral cyclic loads on the conductor bundle. In the straight section all the conductor forces are transmitted to the central cylinder and large internal bearing stresses can result if an internal structure is not used. The total bearing pressure that would act if the conductors were unsupported is proportional to the square of the magnetic field and as such is independent of size. Consequently, this loading is adequately modeled at any size provided the same magnetic field is generated.

As far as the cyclic lateral bearing force, the total force is proportional to the coil ampere turns and the pulsed poloidal field. Since the large coils are broken up into eight coil modules, adequate simulation of this load -- as far as the conductor and windings are concerned -- occurs if the number of ampere turns corresponds to about one coil module -- or 1/128 of the total ampere turns of the large coil system.

There is another force that must be considered in scaling and that is the force resulting from the discharge of a faulted coil while other coils are energized. This is a lateral force which is highest

in the high field region. The magnitude of this force depends on the ampere turns of each coil and the distance to the neighboring coils.

The need to simulate the correct thermal environment both locally as well as over the whole coil must be considered. While the scaling of a purely D.C. magnet must consider natural convection aspects in the long vertical legs, the addition of different amounts of pulsed heat to each of the legs puts more emphasis on the choice of the vertical height of the coil. (Since scaling will be necessary in any case, this particular aspect can be tested separately by constructing long vertical helium containers with simulated winding passages and pulsed heat generation.)

If all of the above are considered in the selection of the coil size then from a superconductor operation point of view the main operating conditions in the model coil will be the same as in the full scale coil.

For a fixed conductor and cooling passage configuration and fixed magnetic field, the total thickness of conductor and cooling passage is independent of magnet size. The structure thickness of a tension structure increases as the size increases. The deflections increase proportionately with increasing size, consequently the full size system will be relatively softer structurally than the model coil -- when referred to the windings. This relative softening of the structure is difficult to simulate in smaller-sized models.

The last major consideration is the question of coil winding and manufacturing. While it is relatively easy to obtain a full size conductor which is characteristic of the manufacturing methods that

would be essentially identical to those used to produce conductor for the EPR system, the coil winding and coil manufacturing techniques of the EPR coils cannot be simulated by choosing a coil size so small that winding and manufacturing are not typical of the large sizes required.

Small scale coils or coil sections of various shapes should be built to develop the necessary understanding of conductor behavior, and its variation under the various conditions to which it will be subjected. However in order to adequately characterize the coil performance of the EPR coils it is necessary to build a coil that contains the results of manufacturing methods generally suitable for large size. It is this consideration which requires that the size of coils to test the design concepts be of the same order of magnitude as the EPR coils.

7.2 Characteristics of the Recommended Size

The recommended scaledown of the system size is $1/3$ in linear dimension. Coils of $1/3$ size should be built to generate 8 T in a $1/3$ size system using the same conductor and cooling passage configuration. Table 7.1-1 shows a comparison of a 6 coil $1/3$ size system, an 8 coil $1/3$ size system with the 16 coil EPR system.

Table 7.1-2 compares the reduction in the amount of conductor and structure required as well as the comparison of the ampere turns for the 6 coil and 8 coil scaledowns from the 8 T EPR design. The similitude of the 6 coil system is better than that of the 8 and is therefore preferred.

7.3 Test Conditions

Because coils are wound and manufactured individually, the recommended testing program naturally falls into the category

Table 7.1-1

System Comparison

	1/3 Size 8 T	Full Size 8 T
Major Radius (m)	2.52	7.57
Horizontal Bore (m)	2.5	7.5
Vertical Bore (m)	3.75	11.25
Magnetic Energy (GJ)	1.40	25.9
Total Ampere-Turns (MA)	47.4	141

Individual Coil Comparison

Number of Coils	6	8	16
Hoop Tension (MN)	33.0	24.7	83.4
Amp. Turns (MA)	7.90	5.93	8.81
Lateral Force due to Poloidal Field (0.4 T)			
1/2 Coil (MN)	10.9	8.2	30.8
Per Unit Length (MN/m)	3.2	2.8	3.5
Torque due to Poloidal Field (MN-m)	40	30	302
Radial Force/Unit Length (MN/m)	41	31	38
Length of Straight Section of D (m)	2.37	2.37	7.12
Lateral Force under Fault (MN)	77	60	264
Lateral Force under Fault/Unit Length			
Nose Area (MN/m)	9.1	7.2	12.6
Low Field Area (MN/m)	2.9	2.30	3.3

Table 7.1-2

Comparison of Main Variables
of Scale Model to EPR

	<u>6 Coils</u>	<u>8 Coils</u>
<u>System:</u> Conductor Weight	.12	.12
Tension Struct. Weight	.054	.054
 <u>Coil:</u> Conductor Weight	 0.33	 0.25
Tension Struct. Weight	0.14	0.11
Ampere Turns	0.90	0.67
Straight Length	0.33	0.33

of single coil tests and multiple coil tests.

Whether all the coils to be built for the system are identical or different, it is desirable to test the coils as they are made to verify that indeed they can be cooled down and energized. Because the loading mode of a single energized coil is very different from the loading mode when energized in a system, the structural requirements for self-energizing are different. Two alternates exist. (1) Partially energize the coil to a current limited by the structure or (2) design the structure (or a removeable test fixture) to support the load. The ability to test each coil individually to its full current (and slightly beyond) is a proof test of the major components. While successful operation of a single coil does not insure successful system operation, the assembly of all coils into a system without prior testing will preclude system operation if one coil is faulty.

A single coil test program should include the following tests:

1. Conductor stability tests using heaters to simulate continuous as well as repetitive pulsed heating.
2. Variation of helium temperature and flow rate to determine limits of proper coil operation.
3. Structural verification tests to determine that loads are being supported as designed.
4. The charge and discharge tests at various rates within the limits allowed by the coil insulation.
5. The coil should be quenched at progressively higher currents by means of heaters with measurement of voltages, currents and internal helium pressure to test the ability of the coil

to quench without incurring excessive voltages, temperatures, or internal helium pressures.

Once a single coil has been tested it can be placed into a system with a high degree of confidence. Multiple coil tests should include most of the single coil tests -- modified for the fact that the system contains larger energies, and as such it is generally not desirable to operate under conditions where one or more coils quench inadvertently.

The first tests on the system should be those of structural verification and the operation of coils in the modes the system has been designed for but at lower currents. Once the structural adequacy has been verified up to design currents, the multiple coil interactions, such as individual coil shut down, should be tested. Finally, quench tests should be run (initially at low current levels) to test that the system indeed operates as designed.

8. RECOMMENDATIONS FOR ADDITIONAL RESEARCH AND DEVELOPMENT

The designs generated in this report are based as much as possible on existing conductor and magnet technology as such minimum research should be required before full commitment. Essentially all of the recommended tasks are of an engineering optimization and developmental nature, as opposed to research, and are intended in a majority of cases to characterize or more accurately pin down specific design details or variables.

Conductors

The conductor to be used in the EPR magnet system must be fully characterized as far as its stability, its AC losses, and its expected performance in a winding of a large coil where it is subjected to pulsed fields and high loads. The conductor must be characterized for the range of operating temperatures and for the range of magnetic fields encountered. Further, the mechanical characteristics both along the conductor as well as perpendicular to it must be measured under static as well as with superimposed cyclic loads to simulate the effect of the poloidal field coils.

To undertake the above characterization lengths of full-size conductors should be manufactured so that the measurements are performed on production grade material. This approach will answer the questions of conductor quality and quality control, as well as provide conductor for testing.

The testing should include short sample characteristics measurement of AC losses while conductor is compressed, stability tests using heaters to measure maximum stable current as a function of magnet field, temperature, and cooling passage geometry. Fully instrumented simple test coils (both solenoids as well as racetracks) should be constructed and tested using the conductor so that all of the coil effects are adequately simulated. In addition to de-

veloping the necessary conductor handling and coil winding techniques these coils serve as small scale models which can be subjected to local pulses both magnetic and mechanical.

The development of the 12 T conductor as opposed to the 8 T conductor is primarily one of optimizing and characterizing Nb-Ti conductors for 12 T operation at reduced temperature. Initially the Nb-Ti can be characterized and optimized for high field by short sample tests on small wires. The next step is the same as the 8 T conductors--testing and characterization of full size conductors in short samples in simulated coil sections and in coils with pulsed magnetic fields and forces.

This characterization and use of conductors will provide confidence and remove the major uncertainties associated with the EPR design. Because the limitations on design depend on the allowable longitudinal elongation as well as the allowable lateral bearing stress these values must be measured and the conductor design evolved to maximize these without damage to the conductor.

Insulation

Aside from providing the mechanical separation between turns and layers the interturn and interlayer insulation forms part of the internal structure of the winding. Because it is used to transmit internal bearing stresses the mechanical and electrical characteristics of the insulation must be known. To this effect an insulator test program is necessary to verify that the bearing stresses as well as the interturn and interlayer voltages during rapid system transients are within the design limits.

Structure

The structure is the heaviest and most costly item in the designs considered. Further structural design optimization, keeping in mind the

problem of eddy currents as well as the need to adequately provide for non-series operation of the coils, may result in more effective structural configurations.

Comparison of D shapes with racetrack shapes or other easy to fabricate structures needs to be done in order to arrive at an "optimum" shape that takes into account cost and fabrication complexity.

The need to provide for large in plane bending during non-series operation of coils may make shapes that are easier to fabricate than the D more attractive when everything is considered.

The structural materials chosen for this design are those on which data was available. The choice of materials was therefore limited to these. This leaves out many potentially useful materials on which data are not available. In general there is a lack of a large enough data base to adequately support a large coil system of the EPR size. More data on structural properties is required in general to allow use of lower cost and, hopefully, just as effective materials as structures.

The use of wound structures using glass-epoxy or more advanced materials may solve some of the problems of structural eddy currents. A full evaluation of the potential advantages and disadvantages of this approach should be performed.

Cryogenics

The cryogenic development program should be aimed at demonstrating the helium distribution and flow in a long vertical passage with pulsed heat load. Temperature fluctuations and temperature gradients should be measured and a design developed to adequately supply helium to the windings.

Operation of helium systems at 4.2 K as well as 2.5 K need to be included in the effort.

Radiation Damage

The main requirement in the area of radiation damage is to generate a sufficient and reliable data base on the various components in the system. The program needs to include the variation of properties of the following:

- a. Superconductor
- b. Matrix material - copper
- c. High purity aluminum--potentially useful
as an AC shield
- d. Insulation material
- e. Structural materials

AC Shield

The use of an AC shield can potentially reduce the AC losses in the windings by shielding the superconductor from the pulsed field.

This study showed that with the available values of resistivity (after nuclear radiation) of high purity aluminum the use of a high purity aluminum shield is not practical. Since the potential benefits of a shield are large, this conclusion needs to be reviewed and the use of superconducting shields instead of aluminum shields examined.

Manufacturing of Subsize Coil

In order to successfully build large EPR size coils the manufacturing methods and processes need to be developed. To this effect the practice winding of large coils, development of laser welding techniques, are generally useful to the manufacture of large coils. However, since it is necessary to translate the designs into successfully operating hardware the manufacture and testing of a subsize coil is necessary to develop all of the necessary expertise for manufacturing EPR size magnets.

As such, the best way to develop design and manufacturing techniques is to design and build a subsize system. Based on considerations of further scale-up, as well as variation of the main parameters with size and cost, the recommended size for the coil is a 1/3 scale version of an EPR coil. The basis for this and additional discussion of this choice is given in Section 7 of this report.

Appendix I. Analysis of Tension and Bending in Multicoil Systems

AI.1 Introduction

In general, the different magnetic loading conditions whether under normal or fault conditions on a single coil can be broken up into a component in the plane of the coil itself and an out-of-plane component. While the coil needs to be designed to withstand both the load components, it is easiest to think of these as being separate loads simultaneously applied.

We shall consider each coil as being represented by a line current so that loads can be thought of as loads per unit length along the coil.

For a coil system consisting of m coils (including not only the toroidal coils but also the poloidal coils), the loading per unit length w on coil m as a function of angular position θ is given in the following form:

$$\bar{w}(\theta) = \bar{I}_n \times \bar{B}$$

where I_n is the current in the n th coil, and $\bar{B}$ is the magnetic field at that point.

In general, the magnetic field $\bar{B}$ at a point for a set of coils each having a different current is given by an expression of the following form:

$$\bar{B} = \sum_{s=1}^m \bar{\alpha}_s i_s$$

where the coefficients $\bar{\alpha}_s$ are dependent on the geometry of the coils themselves and the position of the point at which the field is computed.

If we focus our attention on a single coil with windings in a single plane, we can define the magnetic field components as along the current, in the plane of the coil, and perpendicular to the plane of the coil:

$$\bar{\alpha}_s = \alpha_{si} \bar{u}_i + \alpha_{spi} \bar{u}_{ip} + \alpha_{sop} \bar{u}_{op}$$

where $\bar{u}_i$, $\bar{u}_{ip}$, and $\bar{u}_{op}$ are unit vectors along the current direction, perpendicular to the conductor in the coil plane, and out of the coil plane, respectively.

The current vector $\bar{I}_n$ can be defined as:

$$\bar{I}_n = i_n \bar{u}_i$$

where i_n is the magnitude of the current. Making use of the above, loading per unit length can be written as:

$$\bar{w}(\theta) = (i_n \bar{u}_i) \times \sum_{s=1}^m (\alpha_{is} \bar{u}_i + \alpha_{ip} \bar{u}_{ip} + \alpha_{op} \bar{u}_{op}) i_s$$

or

$$\bar{w}(\theta) = \sum_{s=1}^m \alpha_{ip} i_n i_s (\bar{u}_i \times \bar{u}_{ip}) + \sum_{s=1}^m \alpha_{op} i_n i_s (\bar{u}_i \times \bar{u}_{op})$$

or assuming a right-hand relationship between u_i , u_{op} , and u_{ip} so that $\bar{u}_i \times \bar{u}_{ip} = -\bar{u}_{op}$ and $\bar{u}_i \times \bar{u}_{op} = \bar{u}_{ip}$:

$$\bar{w}(\theta) = - \sum_{s=1}^m \alpha_{sip} i_n i_s \bar{u}_{op} + \sum_{s=1}^m \alpha_{sop} i_n i_s \bar{u}_{ip}$$

The first term on the right is the out-of-plane loading; the second is the in-plane loading. The total loading is a result of the currents in all the coils; and in general, the load is a result of computing the contributions from each coil. However, we can simplify the equation for specific cases.

AI.2 Single Coil Energized

During testing, it is very desirable to be able to energize a coil by itself (the n th coil for example). Under these conditions $i_s = 0$ for $s \neq n$, and we are left with:

$$\bar{w}(\theta) = - \alpha_{nip} i_n^2 \bar{u}_{op} + \alpha_{nop} i_n^2 \bar{u}_{ip}$$

The out-of-plane load for a coil with windings in one plane is zero, so the loading is simply:

$$\bar{w}(\theta) = \alpha_{nop} i_n^2 \bar{u}_{ip}$$

AI.3 All Toroidal Field Coils Energized in Series

For this case we have $i_s = i_n$:

$$\bar{w}(\theta) = \left(-\sum_{\substack{s=1 \\ m \neq n}}^m \alpha_{sip} \bar{u}_{op} + \sum_{\substack{s=1 \\ m \neq n}}^m \alpha_{sop} \bar{u}_{ip} \right) i_n^2 + (-\alpha_{nip} \bar{u}_{op} + \alpha_{nop} \bar{u}_{ip}) i_n^2$$

The loading has been written this way to bring out the physical significance of the fact that the total loading $\bar{w}$ can be thought of as having a loading mode which corresponds to that which the other coils apply ($m \neq n$) and the self loading mode ($m = n$).

Because of symmetry for all the toroidal coils energized in series, no out-of-plane loads result so we have:

$$\bar{w}(\theta) = \left[\alpha_{nop} + \sum_{\substack{s=1 \\ m \neq n}}^m \alpha_{sop} \right] \bar{u}_{ip} i_n^2$$

AI.4 Load on Single Coil with Current i_n ; All Other Currents Equal i

For this case we have the following loading ($i_s = i$)

$$\bar{w}(\theta) = (\alpha_{nop} i_n^2 + \sum_{\substack{s=1 \\ m \neq n}}^m \alpha_{sop} i_n i) \bar{u}_{ip}$$

This can be written as follows:

$$\bar{w}(\theta) = \left[\alpha_{nop} (i_n^2 - i_n i) + (\alpha_{nop} + \sum_{\substack{s=1 \\ m \neq n}}^m \alpha_{sop}) i_n i \right] \bar{u}_{ip}$$

The importance of the above equation is that it allows calculation of the combined loading from the two loading modes. It shows that in a system where i_n is different from the rest of the coil currents the loading is made up of a self loading mode with the square of the self current i_n^2 replaced by $i_n^2 - i_n i$ and a toroidal mode with the square of the toroidal current i^2 replaced by $i_n i$.

AI.5 Tension and Bending

We are now in a position to discuss the consequences of various in-plane loadings. Generally, the design of the coil structure begins by computation of the loading (in-plane) that results from energizing the toroidal field coil in series with a current i_n . As has been shown, this is of the form:

$$\bar{w}(\theta) = \left[\alpha_{nop} + \sum_{\substack{s=1 \\ m \neq n}}^m \alpha_{sop} \right] \bar{u}_{ip} i_n^2$$

Usually a tension or minimum bending configuration is used to take care of this load so that we would expect a small or negligible bending moment to exist in the structure with the above loading mode. This means that the result of the above loading will be pure tension T and no bending M :

$$T = \beta i_n^2$$

$$M = 0$$

where β is a proportionality constant.

A self energized coil with current i_n results in a loading

$$\bar{w}(\theta) = \alpha_{nop} i_n^2 \bar{u}_{ip}$$

This loading mode is not identical to the mode which resulted in little or no bending. Both bending and local tension result from self energizing of a coil:

$$T = \beta_n i_n^2$$

$$M = \mu_n i_n^2$$

where β_n and μ_n represent the functions relating the current in the n th coil i_n to the tension and bending in the n th coil when it is self energized.

If the current in the n th coil is i_n with i in all the other coils, the loading is:

$$\bar{w}(\theta) = \left[\alpha_{nop}(i_n^2 - i_n i) + (\alpha_{nop} + \sum_{\substack{s=1 \\ m \neq n}}^m \alpha_{sop}) i_n i \right] \bar{u}_{ip}$$

The loading is divided up into self loading mode and into a pure tension mode. As such, the tension can be written down as:

$$T = \beta_n i_n^2 + (\beta - \beta_n) i_n i$$

and the bending moment:

$$M = \mu_n (i_n^2 - i_n i)$$

As the coil current decreases during a quench, the local in-plane tension decreases as the following ratio:

$$\frac{T}{T_0} = \frac{\beta_n \left(\frac{i_n}{i}\right)^2}{\beta} + \left(1 - \frac{\beta_n}{\beta}\right) \frac{i_n}{i}$$

If the local tension resulting from energizing a single coil full current $\beta_n i^2$ is less than the local tension resulting from energizing all the coils in series to full current βi^2 , then all of the terms in the above equation are positive and the decay of tension is monotonic. This is the case for all toroidal coil geometries considered. The local bending moment is zero at $i_n = i$ as well as at $i_n = 0$. The maximum bending occurs at $i_n = i/2$:

$$M = -\mu_n i^2/4$$

We can relate this to the maximum bending that exists for fully energizing a single coil to full current:

$$M_{self} = \mu_n i^2, \quad i_n = i$$

The ratio of the bending moments:

$$\frac{M_q(i_n=i/2)}{M_{self}(i_n=i)} = -\frac{1}{4}$$

Alternately, we can compare the quench bending moment to the self energizing bending moment at half current:

$$\frac{M_q(i_n=i/2)}{M_{\text{self}}(i_n=i/2)} = -1$$

We conclude the following: The bending moment that results during the quenching of one coil in a toroidal field array with a structure designed for zero bending moment when all coils are energized to the same current is equal in magnitude but of opposite sign to the bending moment that would result if one coil were energized by itself to one-half current. As a corollary, the bending moment as a result of energizing a single coil to its full current is equal to -4 times the bending moment that results during a quench. It is therefore the self energizing of a single coil that determines the structural design as far as in-plane bending moments are concerned.

If the original shape of the coil is such that bending is not completely eliminated when energized in the toroidal mode with all the currents equal, the bending with all coils energized to a current i will be:

$$M = \mu_e i^2$$

To combine the bending due to this mode with the bending due to the self-energized mode requires replacement of the self current i_n^2 by $i_n^2 - i_n i$ in computing the self mode bending and replacement of the toroid current i^2 in the toroidal mode by $i_n i$ as has been discussed above. This results in the following expression for the bending moment when the coil current i_n differs from i :

$$M = \mu_n(i_n^2 - i_n i) + \mu_e i_n i$$

Using the bending moment for a single fully energized coil as a reference:

$$\frac{M(i_n, \text{all others } i)}{M(i, \text{all others } 0)} = \left(\frac{i_n}{i}\right)^2 - \frac{i_n}{i} \left(1 + \frac{\mu_e}{\mu_n}\right)$$

This has a maximum value at:

$$\frac{i_n}{i} = \frac{1 + (\mu_e/\mu_n)}{2}$$

which results in the maximum value of the ratio:

$$\frac{M_{qmax}}{M_{self} \text{ (Full Current)}} = - \frac{(1 + \mu_e/\mu_n)^2}{4}$$

The bending moment resulting from self energizing a coil is $-4/(1 + \mu_e/\mu_n)^2$ times the maximum bending moment during a quench. Again, the most severe bending moment is the one resulting from self energizing a single coil and not that which occurs during a quench. If the shape is well chosen, μ_e/μ_n (which represents the ratio of bending moments due to departure from the pure tension shape to that due to self energizing a single coil) will be small.

Appendix II. Fabrication

AII.1 Introduction

This appendix covers aspects of the fabrication and assembly of the individual components of the magnet system. The following areas are covered:

- conductor fabrication including cable forming
- coil winding into modules
- cold coil structure
- fabrication of warm central cylinder designs
- assembly of warm central cylinder designs
- cold central cylinder designs

These areas are covered for designs using individual dewars with warm central cylinder (Designs I, Ia, and III) as well as designs using one large room temperature vacuum vessel with a cold central cylinder (Designs II and IV). Because of the similarity of the 8 T and 12 T designs for a given configuration, the plan was written to be applicable both to 8 T and 12 T. Even though each of the designs requires different types of conductor, the basic manufacturing operations of conductor fabrication and cabling are described once and are applicable to all the designs.

The individual sections of this appendix were written as if the system were actually going to be built in the near future, and detailed descriptions of manufacturing details and procedures have been included.

A summary of the overall fabrication plan is included in the main body of the report in Section 3.6. This appendix assembles the details of the plan components. It is not intended that these be read as a continuous presentation but rather an assembly of the components of the plan, each of which can be read or referred to as a separate item.

In formulating a fabrication plan for the magnet system previously described, consideration has been given to the assumptions and design criteria employed for each component of the magnet system as well as to costs, handling procedures, and manufacturing tolerances.

This section of the report discusses the fabrication plan for each major component of the warm central (bucking) cylinder and the cold central (bucking) cylinder designs for the 8 T Toroidal Field magnets. It includes equipment requirements, inspection and testing requirements, and coil assembly procedures. The warm bucking cylinder design concept is presented in detail. Most of the information presented for the warm bucking cylinder design also applies to the cold bucking cylinder design; consequently, using this as a basis, the additions, deletions and/or differences of the cold bucking cylinder design are discussed only.

The conductor design, which was covered in Section 3.1, has been based on the assumption that the cold coil structure supports the loads of electromagnetic (em) origin. This means the conductor must have a low stiffness characteristic along its longitudinal axis, and consequently, it must be fabricated with a low Young's Modulus in that direction. In addition to satisfying this requirement, the stranded Rutherford cable design proposed for use in this program also is flexible enough to be wound relatively easily.

The coil windings have been broken down into small sections or modules. This module concept considerably eases the handling problems associated with manufacturing and allows for easy access to each conductor turn, which

should result in lower labor costs for inspections and tests. In addition, the costs associated with a failure in the coil windings can be minimized because an individual section rather than an entire coil winding assembly can be replaced.

The cold coil structure is to be operated at a temperature of 4.2 K. As a consequence, the materials and the fabrication techniques were selected to minimize the effects of those failure mechanisms that are accentuated at cryogenic temperatures in systems that are subjected to cyclical loading. This means that tolerances associated with any mechanical defect must be defined based on the fracture mechanics for the materials at cryogenic temperatures. The ramifications of welding must also be evaluated to determine their effects on the material properties.

In addition to being fabricated easily, the structure must also be designed to provide remote maintenance features. It must further be designed to minimize the problems associated with shipping, handling, and assembly.

The discussion of the miscellaneous components of this section covers the fabrication of:

- The outer dewar wall structure, which is at room temperature and therefore can be fabricated from suitable material using more orthodox manufacturing techniques and inspection and testing procedures.
- The radiation shield which will be maintained at liquid nitrogen temperatures.
- The column support system which must be designed to act as a low heat leak column to support the large gravitational loads.

- The electrical and plumbing connections, which must be designed with remote disassembly considerations in mind.
- The bucking cylinder is at room temperature for the warm bucking cylinder design and at 4.2 K for the cold bucking cylinder design.

The assembly procedure was based on the assumption that each of the 16 coils would be completely fabricated and tested off-site and shipped to the site. This means the proposed site would preferably be near a major waterway so that the coils could be transported by barge (which is the only mode of transportation which is readily available and economically feasible for use in shipping structures of the size and weight of the coils required for use on this program) from the factory to the final site. In addition, the design and the fabrication plan incorporate features which minimize direct labor costs at the site (which historically are much greater than those incurred at the factory) by doing as much preassembling, match marking, and drilling of mating components as is practical and feasible.

AII.2 Conductor Fabrication

Figure AII-1 shows an isometric view of the conductor which has been tentatively chosen for use in the toroidal field magnet designs. The conductor consists of a stranded Rutherford type NbTi superconducting cable which has a stranded Rutherford type high purity copper cable on either side of it which extends slightly above and below the

Figure A11-1 Schematic Illustrating Method of Interlayer and Interturn Insulation

superconducting cable. An epoxy resin material fills the gap above and below the superconductor in between the copper cables resulting in a compact rectangular-shaped conductor.

The proposed conductor fabrication plan is based upon utilizing today's technology. The following is a list of the major steps that will be utilized in the fabrication of the conductor.

- Produce the tin coated NbTi superconducting wire strands
- Strand and shape these strands into Rutherford type cable
- Solder fill the cable by passing it through a solder bath and sizing die
- Spool the finished superconducting cable on individual reels
- Produce the high purity copper wire strands
- Tin coat each wire strand
- Strand this wire into Rutherford type cables
- Solder fill the cable by passing it through a solder bath and sizing die
- Spool the finished high purity copper cable on reels
- Mold straight length of the epoxy resin filler
- Pass the three cables through a sizing fixture and apply heat to join together
- Insert straight lengths of epoxy resin filler
- Locally heat cable to soften and form it into shape on the winding mandrel

Preparation and Extrusion of Billets

The initial step in the production of the superconducting strands is the preparation and extrusion of the wire billets. The following is a brief description of these operations. Table AII-1 is a work flow sequence for the production of the superconducting wire and cable.

Table AII-1

Work Flow Sequence for Superconducting Wire and Cable

<u>Operation</u>	<u>Steps</u>
Incoming Material Inspection*	Visual and dimensional inspection of all materials to verify conformity with specifications.
Component Preparation*	Chemical cleaning, two water rinses, chemical rinse and forced air drying of all components followed by visual and dimensional inspection to assure cleanliness and precision fit. Store copper components in inert atmosphere to prevent oxidation.
Billet Assembly	Stack array of matrix tubes and solid fillers. Insert superconducting alloy rods into matrix tubes. Press nose into extrusion cylinder. Install array in extrusion cylinder. Insert solid fillers to pack cylinder. Photograph for Q.C. record. Press lid into extrusion cylinder. Electron beam weld cylinder seams.
Billet Extrusion and Initial Drawing	Extrude to approx. 2½" dia. Crop extrusion ends. Take Q.C. sample (part rod at mid-length). Cold work reduction on draw bench to .818" dia.
Wire Fabrication and Cabling	Cold work reduction. Major heat treatment. Twisting. Cold work final size reduction. Tinning. Short sample testing. Cable about core. Short sample testing. Shipment to composite fabrication.

-
- * Operations are common to:
- 1) superconducting alloy rod
 - 2) copper matrix tubes
 - 3) copper filler rods
 - 4) extrusion cylinder, nose and lid

High conductivity copper ingots weighing approximately 1,150 kg (2,500 lbs) each and measuring 25.4 cm (10.0 in.) diameter by 2.54 m (100.0 in.) long are procured and used as a raw material for the extrusion cans, tubular hexagonal elements, and copper filler elements which go into the extrusion billet assembly for the superconducting wire strands and the high purity copper wire strands. Detailed studies of cleaning and billet stacking processes have led to the establishment of practices which minimize void space and provide contamination-free surfaces which promote formation of sound metallurgical bonds and maintain high resistance ratios in copper. All superconducting NbTi rods and copper tubes are checked for straightness and surface. Following this inspection, the rods and tubes are cleaned in an acid bath prior to their assembly into a billet. The acid concentration and temperature as well as rinsing conditions are controlled to provide clear, stain-free components for the extrusion array assembly. Following the acid cleaning the components are stored in a dry nitrogen atmosphere to assure that no deleterious oxidation can take place before assembly.

The individual NbTi rods are then inserted into copper tubes which are in turn stacked in the 25.4 cm diameter extrusion can. The desired copper to superconductor ratio is attained by assembling solid, high purity copper rods with the copper encased NbTi rods. Once the extrusion can has been filled, it is sealed by means of an electron beam welded lid. The completed billet is then extruded down to a 6.35 cm diameter rod.

Wire Drawing and Cable Forming

After the billets have been extruded to 6.35 cm (2.5 in.) diameter, they are drawn on the required reduction schedule to a diameter slightly greater than the final desired diameter. At that diameter, the round wire is twisted to reduce ac losses. After twisting, the conductor is drawn

through the final die to the final wire strand diameter. Each individual strand is then tinned. The individual strands are then cabled. The cable is, in turn, solder filled and drawn through a sizing die to its final dimension.

The procedure for producing the stabilizing high purity copper cable is similar except the twisting of each wire strand can be eliminated since it is not required.

Once completed, individual lengths of superconducting cable and stabilizing copper cables will be boxed and shipped on individual spools to the module winding site.

During processing, several heat treatments will be performed to optimize the current carrying capacity of the superconducting wire and to maximize copper resistivity ratios. These heat treatments also serve to stress relieve the wire.

Insulation

The insulation proposed consists of a two-component, room temperature cure epoxy resin filled material and an epoxy-glass cloth material such as G10. The epoxy resin material will be molded in finite lengths which are easily manageable (to be determined during a future detailed design phase program) and then inserted between the stabilizing copper cables above and below the superconducting cable. During the winding phase of the assembly, the epoxy resin will be locally heated to soften it and then formed into shape. The choice of epoxy resin will be made during a future detailed design program.

The G10 material will be procured in available standard stock sheets and manually inserted between the conductor during the winding phase. Figure AII-1 shows how the G10 material is located between conductors.

Inspection and Testing

The conductor inspection and testing program for use in this program begins with inspection of incoming component materials. Both NbTi and copper constituents will be accompanied by certifications which closely follow specifications. Documentation of certification and consignment of the material to billet identity will be maintained.

The program is best described by subdividing into the following distinct processing categories:

1. Cleaning of components and assembly of extrusion billets
 2. Extrusion
 3. Rod processing on draw bench
 4. Wire processing
 5. Handling, storage and shipping
 6. Support areas
1. Cleaning of components and assembly of extrusion billets

All superconducting NbTi rods and copper tubes will be checked for straightness and surface imperfections prior to acid cleaning. Acid concentration and temperature as well as rinsing conditions will be controlled to provide clean, stain free components for extrusion array assembly. Storage of cleaned components in dry nitrogen atmosphere prior to array assembly will assure that no deleterious oxidation can take place before assembly. Actual assembly will occur in a clean room separated from the production area with tools and work area cleaned immediately prior to use. After assembly, photographs of each billet array will be taken prior to fitting the lid on for electron beam welding.

2. Extrusion

Extrusions will be quenched, cropped and examined under cognizance of quality control personnel present. These pieces will be stamped to identify

nose and tail sections, and a sample will be taken from each end for metallographic examination and documentation. Extrusion parameters and cropping dimensions will be documented in a billet file.

3. Rod breakdown on draw bench

Rod breakdown will be performed on large draw benches under the cognizance of trained Quality Control personnel who are responsible for billet identity maintenance and assurance of proper die schedule. Records will be maintained and become part of the permanent billet file.

4. Wire processing

Incoming inspection will include cropping, visual inspection of wire surface, and recording of coil weight. Prior to processing, a job card will be assigned to each length of coil. This job card will be attached to the coil and accompanies it throughout production. If the production length is processed as several shorter sections, a duplicate in the form of a tag or copy of the original job card will be assigned to each additional piece.

In addition to the job card which describes the complete processing history, records of each drawing pass will be maintained for single die schedules.

On multipass machines, quality control will be assured by standard checks for die wear prior to threading and measurement of wire diameter after each die to assure proper tension distribution in the extended wire between each die and the subsequent capstan.

During processing, several heat treatments will be performed to optimize current carrying capacity and copper resistivity ratio or to stress relieve the wire. Permanent records will be maintained by identification on strip chart recorder. Following all heat treatments, wire will be respooled to assure smooth payoff for the next operation.

5. Handling, storage and shipping

During processing, wire will be kept on metal spools which are stored under plastic covers when not in actual use. This standard practice, plus filtering of lubricants, will prevent inadvertent introduction of foreign matter which can mar the wire surface. Storage after insulation is completed will be on nonmetal spools with paper covering the outer layer. Individual lengths will be shipped on individual spools. Outer layers will be protected by cardboard. Spools will be boxed for shipment.

6. Support areas

There will be two key areas which will be used in support of wire production: a complete metallographic capability and short wire testing facility.

Short wire testing will be performed at 8 T and 4.2° K. This capability will be used in the inspection process in a dual role. Samples of production billets will be taken prior to heat treatment and processed for qualification tests to assure the correctness of the process for the production billets. The capability will also be used to test individual production lengths before and after bending to assure no degradation in current carrying ability.

The second support area, metallographic capability, will be used to determine filament size of final wire, to determine copper and superconductor microhardness as well as pattern configuration in extruded samples, and for fractographic analysis of wire breaks.

These supporting tests yield permanent records which will be maintained at the factory with the exception of fractographic analysis which will be performed visually with the aid of a microscope.

AII.3 Windings

The coil winding method selected for the coil designs presented herein is based on the concept that each coil will be made up of eight individual winding modules. Each module will be assembled into its own liquid helium tight container. These units will then be assembled and shimmed as required during the final assembly of each coil.

The major considerations that will go into the winding of these modules are: conductor cleaning, conductor splicing, tensioning, inspection, and testing. The following paragraphs are brief descriptions of these items.

Conductor Cleaning

After the superconducting and stabilizing copper cable spools and other materials which are required in the coil winding process have been received at the final assembly site and inspected, they can be set up in preparation of the module winding operation, which is shown schematically in Figure AII-2. As shown in Figure AII-2, the finished conductor will pass through an ultrasonic cleaning device just prior to being wound into position to remove any contaminants, oxides or small particles which might be present. Other methods of cleaning conductor that could be employed include such things as manual wiping with a cleaning solution, passing the conductor through an alcohol bath or some other suitable solution, or a combination of the two.

Ultrasonic cleaning has been selected because it has several advantages over these other methods:

1. It can be carefully controlled.
2. It is automated.
3. It can remove minute particles resulting from the soldering operation which could, during normal operation, cause a perturbation leading to a possible shutdown situation.
4. It is economical.

Figure AII-2

Schematic Diagram of Coil Module Winding Process

The coil winding should be done in a "clean room" atmosphere to minimize the possibility of contaminants and/or minute particles gathering on the conductor during the winding operation.

Conductor Splicing

Joints between superconductors have been developed and used in magnets for many years. The most commonly used geometries for monolithic conductors are illustrated in Figure AII-3 and consist of the lap, scarf, and butt joints.

Joining methods which have been used fall into the following categories:

1. soft solder
2. silver solder
3. compression
4. ultrasonic welding
5. cold welding
6. explosive bonding

The use of soft solder in lap or scarf joints is probably the most common technique for conductors of all sizes and has been selected at this stage as the joining method. Conductor ends are usually cleaned, tinned, clamped in a fixture, soldered, machined (if a scarf joint), cleaned, and then insulated. For very large conductors, control and application of sufficient heat may be a problem area. Furthermore, soft solder is not a structural material, and joint support must be considered in the design and coil fabrication.

Joints are usually not superconducting. With proper design and technique, however, resistances in the 10^{-8} to 10^{-9} Ω range are common. Consideration of the heat generated in these areas is accounted for in the magnet design.

Lap Joint

--- Material Removed by Machining

Scarf Joint

Butt Joint

Figure AII-3

Illustration of Typical Joint Geometry

Ultrasonic welding offers a great deal of promise as an alternate method and is currently being used in joints for the U-25 magnet which is being fabricated at the Argonne National Laboratory. Structural and electrical resistance checks on joined samples have been excellent; however, considerable effort is required in fixturing and tuning the apparatus. Utilization of the techniques on the large conductors expected to be used in this program will require some development. However, the technique is attractive because of its ability to be controlled exactly once debugged.

The conductor proposed for use on this program consists of Rutherford type cables and therefore the joint geometries that have been used for monolithic conductors cannot be utilized directly in this case. Figure AII-4 shows an exploded view of the joint geometry which has been tentatively selected for use on this program. It shows that the mating ends of each cable interlock in such a manner that the overall rectangular dimensions of the conductor remain the same. If the cross section of each joint is greater than that of the conductor, problems in fitting the conductor and insulation inside the cross section of the coil structure that is provided could arise and would be difficult to resolve since joints occur arbitrarily throughout the windings.

Without further development, the most practical technique for joint formation to use for the conductor selected for this program would be the

Figure AII-4 Exploded View of Conductor Joint

soft solder method. With further development, the most promising joint formation techniques appear to be ultrasonic welding and explosive bonding. In any case, development of adequate quality control procedures will be essential.

A specially designed fixture will be built to form the joints in such a manner that would ensure mechanical and electrical integrity from one joint to the next.

Conductor Tensioning

Conductor tensioning can be required for several reasons: (1) ease of coil winding fabrication, (2) differential contraction, and (3) minimize relative movement between conductors.

During the winding operation of the coil module, the conductor is fed onto the mandrel which is turning via a motor and gear drive mechanism. Generally speaking, the greater the winding tension of the conductor the straighter and smoother the conductor lies against the winding mandrel. This means less time is required to conform the conductor to the required shape and implies less direct labor and consequently lower fabrication costs.

The integrated differential contraction rate of stainless steel to copper at LHe temperature and that of aluminum to copper at the same temperature are quite different; i.e., for a stainless steel bobbin, the copper conductor tends to tighten about the bobbin because the contraction rate of stainless steel is lower than that for copper, whereas for an aluminum bobbin, the copper tends to separate from it because the contraction rate of aluminum is greater than that of copper. Consequently, for a solenoid magnet design, for example, one would want to minimize the pretension in the conductor wound on a stainless steel bobbin because it would be additive to

the internal forces induced by differential thermal contraction, whereas one would maximize the pretension in the conductor wound on an aluminum mandrel to ensure it stayed in contact with the bobbin. Although the shape of the coil under study on this program is not a solenoid shape, it does have, in effect, a stainless steel bobbin; consequently, the pretension should be minimized.

Pretensioning the conductor results in an increase of the normal load acting between turns, which means the frictional force acting to prevent relative movement between conductor turns is greater and, as a result, perturbations due to frictional heat during relative movement are minimized. Since the TF coil assemblies on this program are of a stabilized design (see Section 3.1 for a discussion on this topic), any local perturbation or normal region due to relative conductor movement will be damped and consequently will not propagate.

As pointed out in Section 3.2, the assumption has been made that the windings are not subjected to a tension load due to loads of em origin and consequently can move to bear against the coil structure (which has been designed to support these loads). Consequently, in order to approach this design assumption in practice, it will be necessary to minimize the pretension in the conductor. Therefore, the only pretension in the conductor would be governed by that which is required to efficiently and economically wind the coil modules. This pretension value would be determined during a future detailed design program based on tests and backed by analytical calculations. The pretension can be accomplished readily with a mechanical device which exerts a suitable pressure on the conductor that allows the conductor to slip or move when tensioned.

Conductor Winding

As shown in Figure 3.2-1b, each coil is divided into eight sections, with each section containing a coil module. There are four different size coil modules. Each module consists of the conductor, LHe housing structure, insulation, and shims as required as shown in a typical cross section of the coil module in Figure AII-5. Each coil module will be fabricated in the horizontal position; i.e., flat. After successful completion of inspection and testing requirements, it will be assembled into the coil structure.

Fabrication of the coil windings in modular form has several advantages:

- (1) The risk of incurring expenses due to faulty coil winding fabrication is greatly minimized because a small section of faulty coil winding can be replaced instead of the entire coil winding of one coil;
- (2) The risk of fabrication schedule overruns are substantially reduced because a faulty module can be replaced rather quickly with another module;
- (3) There is more accessibility to conductor turns because of the smaller bundle sizes which implies lower direct labor cost for inspections and tests;
- (4) During operation, a faulted coil assembly could be repaired simply by removal of the defective module and insertion of a good module;
- (5) Coil modules are relatively small and light and therefore can be handled easily.

It is expected that the coil modules will be fabricated in the same building as that in which the final coil assembly occurs. The coil modules are large and have a small cross section; consequently, they will be fairly flexible and for this reason should remain on the winding mandrel until

Figure AII-5

Cross Section of Typical Coil Module

installed into the coil structure. Consequently, it would be desirable to locate the coil module and final assembly area in the same building. After completion of each coil module, an overhead crane would transport it to the coil structure. Figure AII-6 shows the coil module fabrication plan in schematic form. The plan is based on a "station" concept where a "station" is a place at which one or more of the fabrication steps are performed. As shown, winding of the conductor is performed at Station No. 1.

The first step in the fabrication of the coil modules is to fabricate the housing structure. The housing structure, which will be tentatively fabricated from .64 cm (.25 in.) thick 304L stainless steel plate, serves a two-fold purpose: (1) to contain the conductor during the winding operation and (2) to provide a vacuum-tight vessel in which to hold the LHe coolant. To facilitate winding of the conductor, the housing structure will be fabricated in two parts, with each part having an L shape. The L shape offers visual inspection opportunities and unobstructed accessibility to each conductor which is vital to the fabrication of a quality coil module. The lower L shaped section of the housing will be fabricated and shimmed to the winding mandrel as required to form an accurate surface for the conductor to lie against during winding. The winding mandrel will be shaped accurately to the desired D shape of the coil and inspected for compliance prior to winding. After the L shaped half of the module housing structure is in position and secured, it will be steam cleaned (or equivalent) just prior to commencement of the winding operation to remove any contaminants, oxides or particles from its surface.

After the stabilizing copper cable, superconducting cable and epoxy resin filler have been formed into one cable and the housing structure has been positioned and cleaned, the winding of the conductor can begin. The

Figure AII-6. Coil Module Fabrication Sequence

winding mandrel will sit on a turntable which will be driven by a motor and gear train arrangement. This is a standard method used today in the winding of large copper conductors for large room temperature magnets. Figure AII-7 illustrates a possible clamping arrangement for the coil windings to the mandrel during fabrication. The conductor is wound in vertical layers (reference Figure AII-6 with each layer in series with the adjacent layer. As each layer of a specific conductor thickness is completed or as each length of conductor ends, it is joined to the next size conductor or next length of conductor using joining techniques as outlined earlier. As the ultrasonically cleaned conductor approaches the winding mandrel, it is locally heated to soften it and then formed into shape manually or with a specially designed automatic forming device. Insulation is positioned manually as the winding proceeds.

After completion of the winding operation, the conductors are temporarily constrained within the winding envelope with a clamping arrangement. The entire assembly, including the winding, is then moved to Station No. 2 via an overhead crane. In the meantime, preparations can be made for winding another module. It is expected that more than one winding station would be needed to accelerate the schedule and optimize the costs. The exact number of stations would be determined during a future detailed design and fabrication program. Obviously, it will be necessary to establish strict "cleanliness" policies, as mentioned earlier, to prevent any foreign contaminants, etc. from getting into the windings.

At Station No. 2, the placement, shimming (as required), and seal welding of the other L-shaped part of the housing is conducted in addition to the dressing of the leads of the coil module. Two joining methods have been considered for use in joining the L-shaped housing halves on in this

Figure AII-7. Coil Module Winding Machine (Conceptual Drawing)

program; i.e., welding and bonding. Bonding is attractive from the viewpoint of zero heat input into the coils during the joining processes but has been rejected because of the following reasons: (1) the gap between the mating parts of the housing must be tightly controlled, which means machining and hence higher fabrication costs, (2) differential contraction between bonding agent and the stainless steel can cause cracks which would allow the LHe to leak, consequently leading to a fault condition in the coil, and (3) bonded joints have proved unreliable from a vacuum sealing viewpoint. Welding, on the other hand, is strong, reliable, economical, and practical.

The detail design of the module will take into account the most suitable welding process consistent with reliable seal welds and low heat generation.

The leads which extend out of the coil modules for electrical connections with the adjacent coil modules would be cut to size, cleaned, insulated, etc. After completion of the welding operation, the winding mandrel and coil module would be transferred to the inspecting and testing Station No. 3 via the overhead crane at which point it undergoes extensive electrical, mechanical and leak tests.

At Station No. 4 the completed and inspected coil modules are installed into the cold coil structure. Using the overhead crane, the winding mandrel with the coil module in position is placed and aligned above the appropriate section of the cold coil structure. The coil mandrel is unclamped from the winding mandrel and lowered into position via jack screws inside the coil structure. It is shimmed for proper bearing along the entire length of the coil module. Leads are also aligned and positioned. After the coil modules have been positioned and secured in each section of one side of the cold coil structure, the side plates are bolted in place and the assembly is inspected. The coil structure is then turned over to its other side, and the

remainder of the coil modules are set in place and secured. After the side plates for this side of the coil are in place and the entire assembly inspected and tested, the coil assembly is complete.

Inspection and Testing

This section discusses the inspection and testing approach to evaluate conformance of materials, processes, parts and assemblies to the imposed specifications that are proposed for use on this program relative to the coil winding operation. Figure AII-8 shows the inspection and testing phase flow diagram.

General

All incoming material will be inspected and tested to ascertain it meets all specifications. To fulfill this task, records of purchase orders, material certifications, and other pertinent data will be maintained and systematically reviewed.

Inspection of production tools such as the winding mandrel and joint fixture will be conducted prior to production use to ensure they meet material and drawing specifications. In addition, throughout the duration of the production schedule all tools will be periodically inspected for wear and/or other defects which might have resulted after initial inspection.

All inspection tools will be calibrated and periodically checked for accuracy.

Station No. 1

At Station No. 1 after all incoming materials have been inspected and tested, the following items will be in process inspected and tested:

- The bond between the stabilizing copper cable and the superconducting cable will be visually and nondestructively inspected ultrasonically.

Figure AII-8

Inspection and Testing Plan Flow Diagram, Coil Assembly

In addition, material samples will be taken from lots for destructive evaluation.

- Soldered conductor cross sections will be continuously inspected for mechanical and electrical integrity.
- Heater and ultrasonic cleaning power will be monitored and controlled.
- The winding tension will be continuously monitored to assure its adherence to the predetermined schedule.
- The insulation and shim stock will be inspected to assure its conformance to specification.
- Windings, insulation, housing structure, and the immediate working area and personnel will be inspected for cleanliness.
- Completed coil windings will be dimensionally checked.
- Joints will be inspected visually and ultrasonically for bond coverage.
- The resistance of the joint will be measured and compared to the resistance of a continuous length of the conductor.
- Periodically, destructive testing of sample joints will be conducted to assure the continued qualification of the process.
- The electrical characteristics of the coil will be continuously monitored and recorded to assure their conformance to specifications.

Station No. 2

The following items will be in process inspected and tested at Station No. 2.

- Welding processes will be monitored to ensure their operation and function is within prescribed specifications.
- After welding, the housing structure will be leak tested for vacuum integrity. This will most probably be done either with a halogen gas leak detector or a mass spectrometer.

- Electrical tests will be performed to determine that no damage was done to the coil module as a result of the welding operation.
- Leads will be visually inspected and measured to verify dimensional accuracy.
- Silver soldered fittings will be inspected and tested for electrical and mechanical integrity.

Station No. 3

At Station No. 3, the final inspection and testing of the completed coil module will be conducted. The coil module will be tested electrically, mechanically, and dimensionally to ensure it meets prescribed specifications. Any coil module not successfully passing final inspection and testing will be earmarked and set aside for rework. All permanent final inspection and testing documentation will be kept at this station.

Station No. 4

At Station No. 4, the installation of the coil module into the cold coil structure will be inspected to verify the placement and mechanical fit. In addition, after the coil is completed a final electrical and mechanical inspection will be conducted and recorded.

AII.4 Cold Coil Structure

General

The plan outlined below and which has been tentatively chosen for use on this program covers major manufacturing methods, processes, and equipment as well as inspections and tests that are needed to accomplish fabrication of the cold coil structure. Figure AII-9 shows the arrangement of the cold coil structure relative to the other major components of the magnet assembly.

The design concepts that are being considered for fabrication do not present any insurmountable manufacturing problems. All processes, equipment

Figure AII-9 Major Component Arrangement

and metallurgical requirements are well within present "state of the art" capabilities. The size, configuration and tolerance demands of the cold coil structure dictate the development and implementation of some special methods, procedures and tooling to accomplish the unusual requirements for manufacture of the components.

This plan is based upon the premise that manufacturing parameters that govern the fabrication of A.S.M.E. code approved vessels are adequate for the manufacture of this product. The A.S.M.E. codes provide the basis for control of material specification, material certification and control documents, manufacturing practices and controls, inspection methods and equipment, and quality assurance requirements.

Shop fabrication of the coil can best be described by a combination of sketches, description of major fabrication steps in sequences and detailed description of unusual or unique manufacturing processes.

A split symmetrical cross section of the cold coil structure design as illustrated in Figure AII-10 is considered to be the most practical of the studied options that have been considered for use on this program from a manufacturing standpoint. This design minimizes distortion problems caused by welding, reduces considerably the requirements for contour machining, and promotes easier fit up of the machined parts.

The manufacturing plan as presented is considered to be the most practical approach facility-wise for the fabrication of a limited number of units. Production runs of the components required would probably involve quantities that would permit consideration of installation of extremely large special welding and machining equipment that would allow welding the complete assemblies and then machining as a unit. Such an approach would reduce the manufacturing variables tremendously.

Figure AII-10

Cold Coil Structure

The welding operations outlined in the plan will require mock-up development to ascertain exact requirements for welding procedures.

The manufacturing plan assumes that an inspection of pressure boundary welds will be required. Manufacturing requirements can be considerably reduced by the elimination of this requirement.

All plates will be blast cleaned.

The following is a sequenced manufacturing plan for the center, hoop and side plates.

Center Plates (see Figure AII-11)

Incoming material and inspection release.

Clean plates.

Liquid penetrant test (PT) of plates.

Layout and identify plates.

Inspect layout.

Plasma burn plates.

Grind O.D. and I.D. edges of plates.

Face machine both sides of plates.

Layout for machining end weld preparations.

Machine end weld preparations.

Inspect machining.

Fit-up and end weld plates to form "D" as shown in Figure AII-12.

Control welding to maintain dimensional tolerances.

Machine and/or grind weld over-bead.

Inspect machining.

PT welds.

Store plate.

Hoop Plates (see Figure AII-13)

Incoming material inspection and release.

Clean plates.

PT plates.

Layout and identify plates.

Inspect layout.

Plasma burn plates.

Figure AII-11 Center Plate - Cold Coil Structure

Figure AII-12 Center Plate Fit-Up and Weld

Figure AII-13

Hoop Plate Cold Coil Structure
AII-38

Grind plate edges to remove burn slag.
Closed die hot form plates as shown in Figure AII-14.
Closed die cold size plates as shown in Figure AII-14.
Blast clean plates.
Inspect formed plates for contour.
Machine both edges of plate to finish dimensions.
Inspect edge machined plates.
Fit-up and tack weld plates as shown in Figure AII-15.
Inspect fit-up.
Remove from fit-up table and weld.
Machine and/or grind weld over-bead.
PT welds.
Inspect "D" shape for dimensional accuracy.
Store components for later assembly.

Side Plates (see Figure AII-16)

Incoming material inspection and release.
Clean plates.
PT plates.
Layout and identify plates.
Inspect layout.
Plasma burn plates.
Grind plate edges to remove burn slag.
Machine inside plate faces.
Contour machine inside and outside dimensions to "D" coil configuration.
Inspect machining.
Layout plate ends for weld preparation machining.
Machine plate end weld preparations.
Inspect machining.
Fit-up and end weld plates to form "D" as shown in Figure AII-17.
Control welding to maintain dimensional tolerances.
Machine and/or grind weld over-bead.
Inspect machining.
PT welds.
Store plate.

Figure AII-14

Closed Die Plate Forming, 14,000-Ton Press

Figure AII-15 Hoop Plate Fit-Up and Weld

Machining Stock

Figure AII-16

Side Plate, Cold Coil Structure

Typical Weld Preparation

Figure AII-17

Cold Coil Structure Fit-Up and Weld

Plate Assembly

Obtain pieces 1 and 1A.

Layout bolt holes in pieces 1 and 1A for attachment of pieces 2, 2A, 3, 3A, 4, 4A, 5, 5A, 6, 6A.

Inspect layout.

Drill and counterbore holes in plates 1 and 1A.

Inspect drilled holes.

Obtain pieces 2, 2A, 3, 3A, 4, 4A, 5, 5A, 6, 6A, and accurately fit these pieces to plates 1 and 1A.

Inspect fit-up.

Drill pieces 2, 2A, 3, 3A, 4, 4A, 5, 5A, 6, 6A, utilizing holes previously drilled in pieces 1 and 1A to guide drill during drilling.

Deburr drilled holes.

Inspect drilled holes.

Temporarily bolt pieces 1 and 1A to pieces 2, 2A, 3, 3A, 4, 4A, 5, 5A, 6, 6A.

Obtain pieces 7, 7A, 8, 8A, 9, 9A, 10, 10A and accurately fit these to pieces 2, 2A, 3, 3A, 4, 4A, 5, 5A, 6, 6A.

Inspect fit-up.

Layout bolt holes for pieces 7, 7A, 8, 8A, 9, 9A, 10, 10A.

Inspect layout.

Drill and counterbore holes in pieces 7, 7A, 8, 8A, 9, 9A, 10, 10A and then continue drilling holes into pieces 2, 2A, 3, 3A, 4, 4A, 5, 5A, 6, 6A.

Deburr drilled holes.

Inspect drilled holes.

Temporarily bolt pieces 7, 7A, 8, 8A, 9, 9A, 10, 10A to pieces 2, 2A, 3, 3A, 4, 4A, 5, 5A, 6, 6A.

Turn assembly over and remove pieces 1 and 1A.

Layout bolt holes used to join pieces 7 to 8, 8 to 9, 9 to 10, 7A to 8A, 8A to 9A, and 9A to 10A.

Inspect layout.

Simultaneously drill pilot holes in pieces 7, 7A, 8, 8A, 9, 9A, 10, 10A.

Disassemble components.

Complete drilling and tapping holes in pieces 7, 7A, 8, 8A, 9, 9A, 10, 10A.

Deburr drilled holes.

Inspect drilled holes.

Fit piece 1 to 1A maintaining coil cavity alignment relationship.

Layout bolt holes that are utilized to secure the two coil structure halves together.

Inspect layout.

Simultaneously drill pilot holes in pieces 1 and 1A.

Separate pieces 1 and 1A.

Complete drilling and counterbore holes in piece 1.

Deburr holes in piece 1.

Inspect holes in piece 1.

Complete drilling and tapping holes in piece 1A.

Deburr holes in piece 1A.

Inspect holes in 1A.

Hold all coil inside structure components for subsequent assembly.

NOTE: All components shall be match marked during fabrication.

AII.5 Other Component Fabrication

General

This section of the report outlines the fabrication plan that has been tentatively chosen for the following miscellaneous components:

Dewar Room Temperature Vessel

Intra-Coil Shear Panels

Bucking Cylinder

In addition, the fabrication techniques that would be employed in the fabrication of secondary components such as the radiation shield, low heat leak columns and G10 spacers are also briefly discussed.

The general manufacturing comments that were mentioned earlier in the discussion of the cold coil structure fabrication plan also apply to the miscellaneous components that are discussed herein.

Room Temperature Vessel

Figure AII-9 shows the arrangement of the room temperature vessel relative to the other major components of the magnet design. The fabrication plan that has been chosen for use on the program for the room temperature vessel is as follows. Figure AII-18 shows a cross section of the room temperature vessel.

Figure AII-18

Room Temperature Vessel

Incoming material inspection and release.
Clean plates.
PT plates.
Layout and identify plates.
Inspect layout.
Plasma burn plates.
Grind plate edges to remove slag.
Inspect burned plates for dimensional accuracy.
Hot closed die formed pieces 3, 4, 6, 8, 9 and 12 as shown in Figure AII-14.
Cold closed die size pieces 3, 4, 6, 8, 9 and 12 as shown in Figure AII-14.
Blast clean plates.
Inspect forming of plates 3, 4, 6, 8, 9, and 12.
Machine weld preparations - plates 3, 4, 6, 8, 9 and 12.
Hold pieces 3, 4, 6, 8, 9 and 12 for later assembly.
Machine bosses piece 5.
Inspect piece 5.
Hold piece 5 for later assembly.
Fit-up and tack weld piece 1 in a similar fashion as shown in Figure AII-12.
Inspect fit-up of piece 1.
Weld piece 1.
Machine and/or grind over-bead weld on piece 1.
PT welds piece 1.
Fit-up and tack weld pieces 2, 6 and 7 in a similar fashion as shown in Figure AII-12.
Inspect fit-up of pieces 2, 6 and 7.
Weld pieces 2, 6 and 7.
Machine and/or grind over-bead weld on pieces 2, 6 and 7.
PT welds pieces 2, 6 and 7.
Fit-up and tack weld pieces 3, 4, 8, 9 and 12 in a similar fashion as shown in Figure AII-13.
Inspect fit-up pieces 3, 4, 8, 9 and 12.
Weld pieces 3, 4, 8, 9 and 12.
Machine and/or grind over-bead weld on pieces 3, 4, 8, 9 and 12.

PT welds pieces 3, 4, 8, 9 and 12.

Fit-up and tack weld piece 1 to pieces 3 and 4.

Inspect fit-up pieces 1, 3 and 4.

Weld pieces 1, 3 and 4 - controlled welding.

Grind welds.

PT welds.

Inspect entire weldment for dimensional accuracy.

Fit-up and tack piece 2 to weldment.

Layout holes for bolts and pins in pieces 2, 8 and 12.

Inspect layout pieces 2, 8 and 12.

Drill and ream holes in pieces 2, 8 and 12.

Inspect drilled and reamed holes in pieces 2, 8 and 12.

Layout pieces 3 and 4 for bosses piece 5.

Inspect layout.

Fit-up and tack weld piece 5 to pieces 3 and 4.

Inspect fit-up.

Weld piece 5 to pieces 3 and 4.

Inspect welds.

Machine Reference Surface B as shown on assembly drawing in Figure 3.2-1a; i.e., flat to specified tolerances.

Helium leak test weldment.

Move completed room temperature vessel to coil assembly area.

Intercoil Shear Panels

The intercoil shear panel is also at room temperature for the warm bucking cylinder design and consequently deserves similar fabrication considerations as those given to the dewar room temperature vessel. As shown in Figure AII-19 the stiffening ribs are bolted to the shear panel to minimize distortion from welding. The fabrication plan that has been selected for use on this program is as follows.

Incoming material inspection and release.

Clean plates.

PT plates.

Layout plate for burning.

Plasma burn plates 1 and 2.

Figure AII-19

Intercoil Shear Panel Warm Central Cylinder Design

Closed die hot form plates as shown in Figure
Closed die cold size plates as shown in Figure
Inspect forming.
Layout formed plate 1 for burning to finish dimensions.
Plasma burn plate 1.
Grind edges of pieces 1 and 2 to remove burn slag.
Inspect burned plates 1 and 2 for dimensional accuracy.
Machine one edge of piece 2.
Layout hole patterns on piece 1.
Inspect layout.
Drill edge holes in piece 1 undersize for subsequent match drilling
with coil outer structures.
Inspect drilled holes in piece 1.
Fit piece 2 to piece 1 for drilling piece 2.
Inspect fit-up of piece 2 to piece 1.
Drill tap holes in piece 2 utilizing piece 1 as drill template.
Inspect drilling piece 1 to piece 2.
Clean and assemble piece 2 to piece 1.
Hold assembled pieces 1 and 2 for subsequent assembly.

Bucking Cylinder

The bucking cylinder is at room temperature during normal operation of the magnet assembly. It will be fabricated in split rings (as shown in Figure AII-20 for two reasons: (1) to facilitate manufacturing and (2) to allow insertion of insulating material between ring sections to prevent eddy currents from being generated. Aside from playing a key structural role within the magnet design, it also constitutes the center of the magnet assembly and consequently must be machined accurately for proper placement of the coils, which is absolutely necessary for successful magnet operation. Consequently, careful consideration must be given to the fabrication, machining and inspection of the bucking cylinder. The sequential fabrication plan for the split rings is as follows.

Figure AII-20

Split Ring Segment Bucking Cylinder Design

Incoming material inspection and release.
Clean plate.
PT plates.
Hot form plate.
Cold size plate.
Blast clean plates.
Inspect formed plates.
Layout for machining surfaces marked "A."
Inspect layout.
Machine surfaces marked "A."
Mount 2 half bore tube segments on vertical machining center.
Face surface marked "B."
Partially machine diameters marked "C" and "D."
Turn bore tube segments 180° and remount in vertical machining center.
Finish machining diameters marked "C" and "D."
Face-off surface marked "E."
Machine 16 flat surfaces marked "F" on vertical machining center.
Inspect surfaces machined on vertical machining center.
Layout tie rod holes.
Inspect hole layout.
Drill tie rod holes.
Inspect drilled holes.
Match mark segments.

Secondary Components

The radiation shield will be tentatively fabricated from .64 cm (.25 in.) aluminum plate in small sections that are easily manufactured and handled. Tracer tubes will be preformed and welded in place on each section. They will include fittings for connecting tracer tube sections.

The epoxy-resin spacers may be fabricated from available stock plate material to the prescribed specifications. An alternate method would be to mold the spacers which would eliminate a substantial portion of the machining and implies lower costs. The method chosen would be made after a detail

study of the economical and technical trade offs available during a future detailed design program.

The column support subassembly selected consists of nested concentric cylinders of different materials (i.e., epoxy-resin and aluminum) and associated support structure. This has been discussed earlier in Section 3.2.

Cylinders of epoxy-resin material of the large diameters and thicknesses that are of interest to this design will have to be fabricated as special filament wound, hand lay-up parts because it is not available commercially from stock in the required sizes. This, however, does not present any insurmountable manufacturing problems but implies higher fabrication costs. The aluminum cylinder will be fabricated from plate which is rolled, seam welded and machined to size.

The epoxy-resin cylinders that support compressive loads only will be epoxied into intermediate flanges made of 6061-T6 aluminum material. The aluminum cylinders which support tension loads will be welded to these flanges. The titanium flange at the top and bottom of the concentric cylinders will be machined from a piece of wrought material and bolted into position. The associated support structure will be fabricated from 310S stainless steel plate, welded and machined to specified dimensions and tolerances. The entire subassembly will then be bolted together, inspected and tested, and moved to the coil assembly area.

AII.6 Assembly of Warm Central Cylinder Design

This section discusses the toroidal field magnet assembly. It outlines the steps required for the factory assembly of the major component parts of the toroidal field magnet system that will be shipped separately to the reactor site. These major components consist of: a magnet assembly module comprising two completed coil assemblies together with intercoil structure,

the bucking cylinder, bucking cylinder support structure, and miscellaneous components.

The assembly procedure devised is based on the fact that two coils with associated assembled intercoil shear panels (i.e., coil assembly module) are preassembled off-site and shipped to the site via barge, which is a practical way to ship a massive assembly (about 600 tons) economically. This concept has several advantages:

- (1) The critical coil winding and assembly operations can be conducted at the factory where the expertise and applicable machinery and skills are available economically. This means the fabrication costs are reduced and the reliability and quality is increased.
- (2) On-site personnel can be minimized and on-site assembly time can be reduced, implying lower assembly costs.
- (3) Schedules can be controlled more easily.
- (4) Costs can be controlled more easily.

The assembly procedure discussed below and which has been tentatively selected for use on this program does not present any insurmountable assembly problems and can be implemented using existing equipment.

This plan requires the following:

- (1) The site of reactor be situated on or near a major waterway.
- (2) The assembly location of the coil assembly modules be situated near a major waterway which has crane and dock facilities to handle assemblies of the size and weights for these components.
- (3) The coil winding and coil assembly be performed at one location, preferably in the same building.

Coil Assembly

Figure AII-21 shows the coil fabrication plan sequence and lists each major step involved in the assembly of the coils, and Figure AII-22 defines the elements that will be referred to in the coil assembly process description. Each module will include two coils and associated intercoil shear panels that have been preassembled. Figure AII-23 shows a cross section of the coil as it is assembled.

In the first step, the room temperature vessel (minus a side plate) is placed on the assembly fixture, aligned, leveled, and secured in position.

The next step is to position, align, level, and secure with insulated fasteners the G10 spacers to the bottom inside surface of the room temperature vessel. As discussed in Section 3.2, the spring-loaded adjustment bolts for the G10 blocks for the upper half of the coil assembly are on one face of the room temperature vessel and those for the bottom half of the coil assembly are located on the opposite face of the room temperature vessel. Consequently, to position the G10 blocks at the top half of the coil, shims are required and for the lower half the spring-loaded adjustment bolts are utilized to properly position the G10 spaces. After this has been accomplished, sections of the radiation shield can be placed on top of the spacers and the tracer tube connections can be made between sections. Reference plane B as shown in Figure AII-23 is now established.

In a separate area the remaining G10 spacers and radiation shield sections can be positioned, aligned, leveled, and assembled to the cold coil structure subassembly which includes the coil modules and has been previously inspected and tested. After verification of the proper assembly of the G10 spacers and radiation shield sections to the cold coil structure subassembly,

Figure AII-21
Coil Fabrication Plan Outline

COIL MODULE
(Off Site)

COLD COIL STRUCTURE
(Off Site)

COIL ASSEMBLY
(Off Site)

MAGNET ASSEMBLY MODULE
(Off Site)

T. F. MAGNET ASSEMBLY
(On Site)

Figure AII-22

T. F. MAGNET ASSEMBLY ELEMENTS

Figure AII-23

Cross Section of Coil Assembly

the entire subassembly can be lifted via overhead cranes into position over the room temperature vessel and lowered to rest on reference surface B. At this point the cold coil structure subassembly can be centered within the room temperature vessel and shimmed.

The low heat leak column support system subassemblies which include the low heat leak columns and their supports and which have been preassembled, inspected and tested are the next major item to be assembled. After the column supports are bolted into position, the radiation shield is attached. The power lead assembly is assembled and all electrical, mechanical, and cryogenic connections internal to the room temperature vessel are made at this point prior to the assembly of the side plate of the room temperature vessel. At this time the assembly will be thoroughly checked mechanically and electrically to assure its conformance to specifications. Following this, the G10 spacers are placed on the radiation shield, aligned and leveled with shims if necessary so they are perfectly flat relative to each other, forming a level surface. The side plate of the room temperature vessel is then positioned, clamped, seal welded, and bolted. The nuts of the spring-loaded adjustment bolts (reference discussion on out-of-plane alignment blocks in Section 3.2) on the top half of the coil assembly are removed allowing the spring to be free. The same procedure is conducted for the spring-loaded adjusting bolts on the lower portion of the coil assembly. Sheet metal caps are then welded over each spring-loaded adjustment bolt to vacuum seal the clearance holes in the side of the room temperature vessel.

After a thorough mechanical, electrical and dimensional inspection and test, the coil assembly is completed and ready to be moved to the coil/intercoil shear panel alignment and assembly area.

Magnet Assembly Module

After a pair of coil assemblies have been completed and have successfully passed all prescribed inspections and tests, they can be rotated from the horizontal position to the vertical position via suitable overhead cranes and moved to alignment and assembly fixture for preassembly of the intercoil shear panels. As stated earlier, to minimize on-site direct labor costs, the coils would be shipped to the site in pairs that are connected together with intercoil shear panels. The remaining half of the intercoil shear panels will be installed on-site. Prior to shipment, the captive, spring-loaded bolts will have been installed in these panels. The remaining task for on-site assembly is the drilling and tapping of the mating room temperature vessel flanges using the master transfer drill templates.

Figures AII-24 and AII-25 show the top and side view, respectively, of the coil/intercoil shear panel alignment and assembly plan that has been chosen for use on this program. As shown, a fixture simulating very accurately the bucking cylinder would be fabricated, machined, leveled, aligned, and assembled to the floor. Machined vertical reference surface B would be aligned perfectly plumb via optical instruments such as a theodolite or transit. After this, horizontal reference surface A would be established by adjusting the several leveling plates until flat within a common plane utilizing optical methods. Each coil would be aligned to the bucking cylinder fixture and aligned radially by means of triangulation that utilizes three theodolites or transits to focus on an alignment target that has been machined into the room temperature vessel of each coil assembly. After the coils are aligned and have been verified to be at the correct major radius of the torus, the intercoil shear panels are positioned and bolted to the flanges of the room temperature vessel of the coils. Once the captive,

Figure AII-24

Top View of Coil/Intercoil Spacer Alignment and Assembly at Factory

Figure AII-25

Side View of Coil/Intercoil Shear Panel Alignment and Assembly at Factory

spring-loaded insulated shoulder bolts have been installed and torqued to the prescribed value, the entire magnet assembly module can be moved to a separate area for shipment preparations.

Central (Bucking) Cylinder Assembly

The bucking cylinder will be assembled at the factory and shipped to the reactor site as a completed unit. The split ring washer segments will be bolted and pinned together. Insulation will be inserted between sections to minimize the losses due to eddy currents. Figure AII-26 is a sketch of the completed bucking cylinder assembly.

On-Site Assembly of Components

The assembly concept that has been tentatively chosen for use on this program and which does not present any insurmountable assembly problems and is well within the present state of the art is stated below. Figure AII-27 illustrates the major components and interfaces that constitute the on-site assembly of the toroidal field magnet assembly.

After all component parts of the magnet assembly have been received at the site, inspected, and released for assembly, assembly of the toroidal field magnet assembly can begin. It is assumed the magnet assembly foundation, utilities and all peripheral facilities have been installed and/or are available.

The first major component to be installed is the bucking cylinder support and the bucking cylinder. The bucking cylinder support is positioned on anchor bolt leveling nuts which protrude from the foundation, is aligned, leveled, and then secured in position by tightening the anchor bolt nuts. The top surface of the bucking cylinder support is a machined reference and alignment surface.

Figure AII-26
Bucking Cylinder Assembly

Figure AII-27 Major Components of T.F. Magnet Assembly

After the bucking cylinder support has been installed, the bucking cylinder can be lifted into position, checked for alignment and adjusted as necessary by readjustment of the anchor bolt leveling nuts, and secured to the support with high strength bolts. Reference Surface B is an important leveling surface as shown on the assembly drawing (Figure 3.2-1a of Section 3.2). Surface B must be perfectly plumb around the entire circumference of the cylinder. This can be accomplished with optical instruments or a transit by sighting targets which have been accurately located and machined during the fabrication phase of the bucking cylinder.

With the bucking cylinder in place, the magnet assembly modules are positioned about the cylinder. Reference surface A, shown on the assembly drawing in Figure 3.2-12, consists of machined plates (i.e., four for each magnet assembly module) which have been leveled within a common plane. Once the leveling plates are set, the modules can be lifted into position with the overhead crane and slid into position against the bucking cylinder with winches or "come-alongs" and cables that are attached to special plates that have been provided on the room temperature vessel. The leveling plates are adjusted as necessary until the nose of each coil fully bears on the bucking cylinder. In order to facilitate movement of the coil modules, a low friction material will be provided on the leveling plates. This could be either a low friction material such as Rulon or a light coating of grease or oil. Machined key ways in the bucking cylinder accept machined keys on the nose of each room temperature vessel to ensure accurate alignment. Once all coil assembly modules have been finally positioned and secured, the remaining eight sets of intercoil shear panels are installed.

After final inspection and tests, two-component room temperature cure epoxy resin can be cast between coils at the nose in order to provide a load bearing material to withstand the high compressive forces arising during a

fault condition. The coil nose in this area has been pretreated with a release agent to facilitate coil replacement in case it is necessary. The choice of the two-component epoxy resin would be made during a future detailed design program. An alternative to the epoxy resin would be a nonshrinkable grout which is easy to handle and pour and is economical, the main disadvantage being the difficulty in removing it in case of coil replacement.

After the major component assembly of the magnet has been completed, interface connections would be made to the refrigeration, vacuum, and electrical power systems. These connections would be designed to be outside the radiation shield; consequently, the connections would not require remote maintenance and therefore less down time and lower operating costs.

The above discussion outlined the major component assembly of the magnet only without any mention of the other major assemblies which will have to interface with the magnet. This was done purposely for clarity and brevity sake to highlight the scope of the magnet assembly. Obviously, careful consideration must be given to the inner and outer plasma blanket/shield, refrigeration, vacuum, poloidal coil, and the radiation shield interfaces. This would be done during a future detailed design program.

Inspection and Testing

The following outlines the critical points that will require inspections and tests at the factory during the assembly of the coil and magnet assembly module. It also outlines the critical points for inspections and tests on-site. It is assumed that in addition to those listed below that the normal everyday inspections and tests will be performed. The following also assumes that all incoming materials, the cold coil structure subassembly, the room temperature vessel, the bucking cylinder, the radiation shield

sections, the column support subassemblies and the intercoil shear panels have also successfully passed inspection and prescribed tests.

Coil Assembly: Off-Site (Reference Figure AII-23)

- Verify that the coil assembly fixture (reference surface A) is level to a horizontal plane prior to placement of room temperature vessel on the fixture.
- Verify that reference surface B is level to a horizontal plane and that the alignment, spacing and dimensions of G10 spacers and radiation shield sections are in accordance with applicable drawings.
- Inspect cold coil structure assembly which includes radiation shield section and G10 spacers for alignment, flatness, and size per specified drawings.
- Inspect clearances between room temperature vessel and cold coil structure assembly and column support subassemblies.
- Test and inspect all mechanical connections for pressure integrity and structural integrity.
- Inspect interior of room temperature vessel for cleanliness.
- Inspect spring-loaded adjustment bolts prior to positioning of side plate of room temperature vessel.
- Verify nuts of spring-loaded adjustment bolts have been removed prior to the seal welding of bolt covers.
- After seal welding, inspect side plate fasteners for proper torque.
- After seal welding, test the room temperature vessel for vacuum integrity using a mass spectrometer or equivalent device.
- After final mechanical inspections, check the coil assembly for conformance to electrical specifications.
- Remove coil assembly to cryogenic test area for full performance tests at 4.2⁰ K.

- Return coil assembly to assembly area.
- Inspect the intercoil shear panel assembly to ensure it is in compliance with applicable specified drawings.
- Verify that the coil locations are in accordance with the alignment specifications (see Figure AII-25).
- Inspect the position of the transfer drilling templates prior to the machining of the threaded holes in the flanges of the room temperature vessels.
- Verify the fit of the shear panels.
- Inspect magnet assembly module prior to shipment to ensure that the shipping and packing specifications have been met.

Toroidal Field Magnet Assembly: On-Site

- Visually inspect all components for possible damage during shipment.
- Inspect and electrically test each coil of each magnet assembly module to verify it meets prescribed specifications and that no damage was incurred during shipment.
- Inspect and verify alignment of bucking cylinder support and bucking cylinder. Pay particular attention to reference surface B of bucking cylinder as shown on assembly drawing (Figure 3.2-1a) to ensure it is plumb to within specified tolerances.
- Inspect and verify that all magnet assembly modules are correctly positioned and that they properly mate with the bucking cylinder in accordance with specified tolerances and drawings.
- Inspect alignment of transfer drilling templates for intercoil shear panels prior to machining operations.
- Inspect toroidal field magnet assembly for mechanical fit and dimensional accuracy.

- Inspect mixing of two-part component epoxy to verify correct components and makeup and in addition inspect prior to and after casting to ensure it has cured properly.
- Inspect all mechanical, cryogenic, and electrical interconnections to verify they meet specified electrical, structural and dimensional requirements.
- Perform final electrical and mechanical tests.

AII.7 Cold Central (Bucking) Cylinder Design

This section discusses the fabrication plan for the major components as well as an outline of the assembly procedure of the cold bucking cylinder design that has been tentatively chosen for use on this program. To avoid repetition, this section describes only those major components that differ from those described for the warm central cylinder design.

The primary difference between the cold and the warm bucking cylinder designs lies with the dewar design and the assembly procedure. Unlike the warm bucking cylinder design in which a vacuum dewar is provided for each coil, the dewar for the cold bucking cylinder design is of the common vacuum type. As a consequence, the magnet assembly modules for this design are made up of two coil assemblies with associated intercoil shear panels but do not include column support subassemblies. This means that it is considerably larger and consequently requires a different fabrication plan and assembly procedure than that described for the warm design.

The intercoil shear panels for the cold bucking cylinder design are manufactured utilizing a similar fabrication plan as outlined earlier for the warm bucking cylinder design with one exception; i.e., each shear panel is made in two pieces and joined with high strength bolts and pins. Insulation is placed between the mating joints. This is shown in Figure AII-28.

Figure AII-28

Intercoil Shear Panel
Cold Central Cylinder Design

As discussed earlier, a two-piece, insulated, bolted design is necessary to minimize the effects of eddy currents at the reduced temperatures; i.e., 4.2 K.

Dewar Fabrication

The dewar for the cold bucking cylinder design is large in size, a fact which necessitates that the handling and shipping of the dewar (i.e., inner vessel, outer vessel, radiation shield and column support structure) be completed in sections. Based on maintenance, lifting capacity, standardization, and manufacturing considerations, it has been tentatively decided that the dewar be divided into eight sections; i.e., each major component of the dewar be divided into octants.

This section discusses the fabrication plan and the necessary inspections and tests that have been chosen for use on this program for the fabrication of the inner and outer dewar vessels. The radiation shield and the column support structure for the dewar and the bucking cylinder will be fabricated in a fashion similar to that outlined for the warm bucking cylinder design.

Figure AII-29 shows a cross section of the dewar design illustrating the general arrangement, component parts and mating joints of the outer vessel with respect to the other major components of the dewar. Each octant of the outer vessel is divided into four parts: 1) upper section, 2) lower section, 3) bucking cylinder inner wall, and 4) access port section. These are shown in Figures AII-30 through AII-33 respectively. Figure AII-34 shows the inner vessel octant which is discussed later. The top part of the upper section has the same shape as the lower section and consequently can be made with the same tools, which helps to lower tooling costs.

Figure AII-29 Dewar Cross Section

Figure AII-30 Upper Section, Outer Vessel

Figure AII-31 Lower Section, Outer Vessel

Figure AII-32
Bucking Cylinder Inner Wall

Figure AII-33
Access Port Section

Figure AII-34

Inner Vessel Octant

Fabrication Plan for Outer Vessel Upper Section (Fig. AII -29)

The following is a list of the sequential steps that will be taken in the fabrication of outer vessel upper section. The entire vessel will be made from plate stock rolled to shape, welded and machined.

- incoming material inspection and release
- clean plates
- PT plates
- layout and identify plates, flanges, and ribs
- inspect layout
- plasma burn plates, flanges, and ribs
- grind plate, flange, and rib edges to remove burn slag
- closed die hot form plates, flanges, and ribs as shown in Figure
- closed die cold size plates, flanges, and ribs as shown in Figure
- blast clean plates and flanges
- inspect formed plates, flanges, and ribs for contour
- machine both edges of plates, flanges, and ribs to final dimensions
- inspect edge machined plates, flanges, and ribs
- fit up and tack weld plates, flanges, and ribs
- inspect fit up
- remove from fit up table and weld
- machine and/or grind weld over-bead
- PT welds
- inspect for dimensional accuracy
- machine all flanges and interfaces
- machine tapped holes for G10 spacer blocks
- final inspection
- set aside for preassembly of dewar

Remaining Dewar Sections

Procedures similar to those described above will be used to fabricate the remaining dewar sections.

Dewar Trial Assembly

After each section has been fabricated and has passed prescribed inspections and tests, it is moved to a suitable area for preassembly. The dewar is completely assembled with each section fitted to the adjacent section, match marked and drilled for locating pins which will ensure proper reassembly on site. In addition, each section of the radiation shield will be located, fitted, and preassembled to its respective dewar section. At this time all critical aspects of the dewar will be thoroughly inspected and tested to minimize on-site direct labor costs.

After successful completion of the preassembly, inspections and tests, the dewar is disassembled and crated prior to shipment to the site. During a future detailed design effort it will be necessary to design and fabricate special handling fixtures to safely handle the sections of the dewar.

AII.3.2 Toroidal Field Magnet Assembly

Because the dewar was fabricated and shipped in sections, it must be assembled at the reactor site. This is accomplished by bolting sections together. Each joint is insulated to minimize the effects of eddy currents. All joints are then seal welded for vacuum integrity and leak tested. On-site costs will be minimized by preassembly at the factory.

The following is a list of the sequential steps that will be used in the assembly of the toroidal field magnet system.

- Place, align and secure each octant of the lower section of the dewar and weld.
- Place, align and fasten the column support structure subassemblies for the bucking cylinder and the coil assemblies.

- Place, align and fasten the bucking cylinder.
- Locate, align and secure the magnet assembly modules (each consisting of two coils and associated intercoil shear panels).
- Locate, align and fasten the remaining intercoil shear panels.
- Make all plumbing and electrical connections internal to the dewar.
- Place, align, bolt and seal weld the inner vessel of the bucking cylinder to the lower section of the dewar.
- Place, align and weld all upper sections of the dewar and fasten mechanical insulated joints.
- Make all external plumbing and electrical connections.

At this point the assembly of the toroidal field magnet is complete.

Inspection and Testing

In addition to the inspections and tests that are required off-site and on-site during the assembly phase of the program which were outlined earlier for the warm bucking cylinder design, the following additional inspections and tests are required for the cold bucking cylinder design:

- on-site weld inspection
- on-site leak test