CTR Program
ARGONNE NATIONAL LABORATORY
9700 South Cass Avenue
Argonne, Illinois 60439

TOKAMAK EXPERIMENTAL POWER REACTOR CONCEPTUAL DESIGN
VOLUME I

Weston M. Stacey, Jr.—Project Manager
Mohamed A. Abdou
Peter J. Bertoncini
Christian C. Bolta
Jeffrey N. Brooks
Kenneth Evans, Jr.
John A. Fasolo
Jung C. Jung
Robert L. Kustom
Victor A. Maroni
Richard F. Mattas
Frederick E. Mills

Balabhadra Misra
John S. Moenich
Alfred Moretti
James H. Norem
John S. Patten
Walter F. Praeg
Peter Smelser
Dale L. Smith
Herbert C. Stevens
Larry Turner
Sou-Tien Wang
Carl K. Youngdahl

August, 1976

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.

ABSTRACT

A conceptual design has been developed for a tokamak Experimental Power Reactor to operate at net electrical power conditions with a plant capacity factor of 50% for 10 years. The EPR operates in a pulsed mode at a frequency of $\sim 1/\text{min.}$, with a $\sim 75\%$ duty cycle, is capable of producing ~ 72 MWe and requires 42 MWe. The annual tritium consumption is 16 kg.

The EPR vacuum chamber is 6.25 m in major radius and 2.4 m in minor radius, is constructed of 2-cm thick stainless steel, and has 2-cm thick detachable, beryllium-coated coolant panels mounted on the interior. An 0.28 m stainless steel blanket and a shield ranging from 0.6 to 1.0 m surround the vacuum vessel. The coolant is H_2O .

Sixteen niobium-titanium superconducting toroidal-field coils provide a field of 10 T at the coil and 4.47 T at the plasma. Superconducting ohmic-heating and equilibrium-field coils provide 135 V-s to drive the plasma current. Plasma heating is accomplished by 12 neutral beam injectors, which provide 60 MW. The energy transfer and storage system consists of a central superconducting storage ring, a homopolar energy storage unit, and a variety of inductor-converters.

FOREWORD

The experimental power reactor (EPR) will have a major role in the U.S. tokamak reactor development program. The current ERDA/DMFE program plan (WASH-1290) projects a tokamak fusion test reactor (TFTR) to begin operation in the early-1980's, a tokamak EPR to begin operation in the mid-1980's, possibly a second EPR to begin operation in the early 1990's, and a demonstration reactor to begin operation in the late 1990's.

In August, 1974, an interdisciplinary project was organized at Argon National Laboratory to perform a scoping and project definition study (ANL/CTR-75-2) for the first EPR. This study was extended during the pas year to develop a conceptual design for the EPR. These studies were intended to define the characteristics of and requirements for an EPR withi the context of the general objectives and schedule of the current ERDA/DM program plan.

Partly as a result of the focus provided by these studies on the tec nological and plasma physics extrapolations required for an EPR, the up-dated ERDA/DMFE reference plan (currently in draft) projects a deferral of the EPR initial operation until the late-1980's and the introduction of a reactor intermediate to the TFTR and the EPR. Subsequent studies wi address modifications of the EPR design that are required by this new rol in the program.

ACKNOWLEDGEMENTS

An undertaking of this magnitude owes its success to the assistance and support of many people in addition to the authors. Although it is impractical to site all of the contributors, certain substantive contributions are specifically acknowledged.

At Argonne, a number of people contributed directly to the design. T. K. Khoe assisted in the design of the beam line for the neutral beam injection system. S. H. Kim assisted in the design of the magnet system. R. E. Fuja assisted in the design of the inductor-converter system for the equilibrium field coil energy transfer system. H. J. Moe performed the potential off-site tritium exposure calculations. T. C. Chawla developed much of the information on hazards analysis and safeguards, as well as initiating the adaption of an analytical formalism for fusion reactor safety analysis. Numerous other people at the laboratory advised upon various aspects of the design.

Several people at other institutions made important direct contributions. J. N. Davidson (Georgia Tech) developed the field ripple criterion for the toroidal field. D. W. DeMichele (Texas A & M) provided valuable support for the toroidal field coil computations and assisted in some of these analyses. T. W. Petrie (Univ. of Illinois) performed the calculations for alpha particle slowing down and escape. T. K. Sung (Univ. of Wisconsin) assisted with the radioactive inventory calculations. C. A. Trachsel and B. A. Cramer (McDonald-Douglas Astronautics) performed the analysis of the structural support system for the magnets and provided valuable suggestions on the engineering design of the blanket and shield. T. Yang and R. W. Conn (Univ. of Wisconsin) performed the preliminary design calculations for a non-circular plasma option.

The preparation of the report for publication was handled by C. S. Bury, who was assisted by C. A. Poore, C. M. Hytry and J. S. Lind. T. J. Lucitt, E. L. Singleton and many others were responsible for the drafting. The extra effort of these people made it possible to meet a demanding publication schedule.

TABLE OF CONTENTS

VOLUME I

	<u>Page</u>
LIST OF FIGURES	xvii
LIST OF TABLES	xviii
CHAPTER I. INTRODUCTION	I-1
CHAPTER II. SUMMARY	II-1
A. Reference Design	II-1
B. Plasma Physics and Performance Analysis	II-8
1. MHD Equilibria and Steady-State Plasma Performance	II-8
2. Transient Performance	II-10
3. Plasma Initiation	II-19
C. Magnet System	II-19
1. Toroidal-Field (TF) Coils	II-22
2. Poloidal-Field Coils	II-28
3. Structural Support	II-29
D. Plasma Heating	II-32
1. Neutral Beam Injection	II-32
2. Radio-Frequency Heating	II-34
E. Energy Storage and Transfer System	II-36
1. Ohmic Heating System	II-36
2. Equilibrium-Field Coil System	II-40
3. Neutral Beam Injection System	II-42
4. RF Heating System	II-42
5. Central Energy Storage Inductor	II-43
F. Vacuum Systems	II-43
G. First Wall	II-45
H. Blanket/Shield System	II-50
I. Access and Maintenance	II-62

	<u>Page</u>
J. Tritium	II-63
K. Facility Description	II-66
L. Costs and Schedule	II-69
CHAPTER III. PLASMA PHYSICS & PERFORMANCE ANALYSIS	III-1
A. MHD Equilibria and Steady-State Plasma Performance	III-1
1. MHD Equilibria	III-1
2. Steady-State Performance	III-11
3. Subignition Plasma Performance Analysis	III-19
4. Particle and Radiation Fluxes on the First Wall	III-23
5. Suprathermal Alpha Particle Effects	III-28
6. Toroidal-Field Ripple Effects	III-31
B. Transient Performance Analysis	III-33
1. MHD Equilibria During the Burn Cycle	III-33
2. Plasma Driving and Heating System	III-36
3. Startup Requirements	III-40
4. Reference Case Burn Cycle Simulation	III-50
5. Power Performance	III-70
6. Plasma Heating	III-81
7. Plasma Refueling and Recycling	III-87
C. Plasma Initiation	III-92
1. Normal Initiation Mode	III-92
2. Alternatives	III-96
3. Requirements for Initiation of Discharge	III-100
D. Research Requirements	III-103
1. Confinement	III-103
2. MHD Equilibria and Stability	III-104
3. Impurity Control	III-104
4. Heating	III-105
5. Refueling	III-106
6. Plasma Initiation	III-106

	<u>Page</u>
CHAPTER IV. MAGNET SYSTEMS	IV-1
A. Toroidal-Field Coil System	IV-3
1. Maximum Practical Field	IV-3
2. Field Ripple, Access and Coil Size	IV-5
3. Pure-Tension Toroidal-Field Coil Characteristics and In-Plane Hoop Stress	IV-7
4. Nuclear Heating and Radiation Damage	IV-14
5. Superimposing Fields and Out-of-Plane Load	IV-15
6. Superimposing Field and AC Losses	IV-19
7. Normal Metal Field Shielding Design	IV-26
8. Shield Option - Superconducting Field Shield	IV-37
9. Cryostatic Stability	IV-39
10. Conductor Design, Coil Structure and Coil Winding	IV-46
11. The Effects of Fabrication Tolerances	IV-54
12. Coil Protection and Magnet Safety Analysis	IV-57
13. Summation of Dissipation Loss and the Refriger- ation Requirements	IV-63
14. Refrigeration System Evaluation	IV-64
15. Cryostat Design	IV-72
16. Toroidal-Field Coil Support	IV-77
B. Poloidal Coil System	IV-78
1. Superconducting Ohmic-Heating Coil Design	IV-79
2. Superconducting Equilibrium-Field Coil Design	IV-83
3. Plasma Initiation-Trimming (IT) Coil	IV-87
4. Axial Forces, Hoop Stresses and Coil Inter- actions	IV-89
5. Coil Stability and Magnet Protection	IV-96
6. Conductor Design and Coil Structure	IV-98
7. AC Loss Estimation and Refrigeration Require- ments	IV-103
8. Poloidal Coil Cryostat Design	IV-105
9. Equilibrium Field Flux Penetration on the Blanket and Shield	IV-107

	<u>Page</u>
C. Research and Development Requirements	IV-11
1. Toroidal-Field Coil System	IV-11
2. Ohmic-Heating Coils and Equilibrium-Field Coils . .	IV-51
3. Structural Material Development	IV-11
CHAPTER V. PLASMA HEATING SYSTEMS	V-1
A. Neutral Beam Injector Design	V-1
1. Ion Sources	V-4
2. Atomic Ion and Neutral Beam Transport for Reference Designs 1 and 2	V-5
3. Molecular Beam Transport	V-14
4. EPR Neutral Injector Power Flow Model for Reference Designs 1 and 2	V-14
5. Injector Gas Flow Diagram	V-17
6. Final Reference Design 1	V-24
7. Reference Design 2	V-24
8. Overall Injector Efficiency	V-27
9. Reference Design 3	V-27
10. Overall Injector Efficiency as a Function of Beam Energy	V-32
11. Injector Mechanical Design Description	V-35
B. Radio-Frequency Heating of EPR	V-38
1. Lower Hybrid Resonance Heating	V-40
2. LHRH RF Power Source	V-44
3. Ion Cyclotron Resonance Heating (ICRH)	V-47
C. Research and Development Requirements	V-51
1. Neutral Beam Injectors	V-51
2. RF Heating Systems	V-51
CHAPTER VI. ENERGY STORAGE AND TRANSFER	VI-1
A. Toroidal-Field Coil Power Supply	VI-2
B. Ohmic-Heating Coil System	VI-2
C. Equilibrium-Field Coil System	VI-14

D.	Neutral Beam Power Supplies	WI-22
E.	RF Heating System Power Supply	WI-28
F.	Energy Storage Inductor	WI-29
G.	Research and Development Requirements	WI-33
CHAPTER VII. VACUUM SYSTEMS		II-IV
A.	Toroidal Vacuum System	II-IV
B.	Neutral Beam Vacuum System	II-12
C.	Waveguide Vacuum System	II-15
CHAPTER VIII. THE FIRST-WALL SYSTEM		II-III
A.	Design Description of the EPK First-Wall System	II-III
1.	Vacuum Vessel	II-151
2.	Coolant Panel	II-152
3.	Current Breaker	II-153
4.	Limiter	II-155
B.	First-Wall Materials	II-155
1.	Structural Materials	II-157
2.	Liner Materials	II-157
3.	Insulator Materials	II-157
4.	Coolant	II-159
C.	Thermal Analysis	II-160
D.	Stress Analysis	II-162
1.	Vacuum Vessel	II-162
2.	Coolant Panels	II-163
E.	Research and Development Requirements	II-165
1.	Materials	II-165
2.	Coolant Technology	II-165
3.	Structural Mechanics	II-165

	<u>Page</u>
CHAPTER IX. BLANKET/SHIELD SYSTEM	IX-1
A. Nuclear Design of the Blanket/Shield System	IX-5
1. Primary Design Criteria and Constraints	IX-5
2. Description of the Nuclear Systems for the EPR: Reference Design and Calculational Models	IX-9
3. Toroidal Geometry Effects in the Reference Design	IX-15
4. Neutronics Effects in the First Wall and Blanket	IX-28
5. Neutronics Analysis of the Bulk Shield	IX-33
6. Neutronics Effects in the Toroidal-Field (TF) Coils	IX-39
7. Nuclear Design of Penetrations Shield	IX-44
B. Blanket and Shield Materials	IX-59
C. Pulsed Magnetic Field Requirements and Torque Effects on Blanket/Shield Design	IX-63
D. Mechanical Design Description of the Blanket/Shield System	IX-66
1. Description of the Reference Blanket Design	IX-68
2. Description of the Slab-type Blanket Design	IX-71
3. Description of the Reference Design Shield System	IX-71
4. Penetration Shield System	IX-73
5. Maintenance and Repair Scenario	IX-73
E. Thermal Analyses	IX-74
1. Blanket	IX-74
2. Magnet Shield Thermal Analysis	IX-76
F. Research and Development Requirements	IX-77
1. Neutronics	IX-77
2. Coolant Technology	IX-78
3. Mechanics	IX-78
4. Shield Technology	IX-79
5. Electromagnetics	IX-79

	<u>Page</u>
CHAPTER X. TRITIUM HANDLING FACILITY	X-1
A. Introduction	X-1
B. Tritium Inventory and Reactor Fueling (Logistics) . . .	X-4
C. Fuel Circulation and Processing	X-5
1. Debris Separation and Handling	X-5
2. Nonmetallic Impurity Removal	X-6
3. Isotopic Enrichment	X-7
4. Fuel Cycle Design and Hardware	X-7
D. Design Description of the Cryogenic Distillation Cascade	X-10
1. Summary Design Description	X-12
2. Analysis of the Distillation Cascade	X-12
3. Column Dimensions and Inventories	X-15
4. Comments on the Level A Cascade	X-16
E. Atmospheric Detritiation Systems	X-18
1. Estimates of Gas Handling Requirements	X-18
2. Catalyst Requirements	X-21
3. Atmospheric Clean-up Scenario and Associated Costs	X-22
F. Pressurized Water Handling Practices	X-24
G. Comments on Blanket Processing Test Modules for Stage II	X-28
H. Research and Development Requirements	X-29
1. Fueling Technology	X-30
2. Tritium Storage and Transportation	X-30
3. Fuel Conditioning and Purification	X-30
4. Tritium Circulation Technology	X-31
5. Tritium Control Technology	X-31
6. Blanket Processing Technology	X-31
7. Instrumentation and Control Technology	X-32

	<u>Page</u>
CHAPTER XI. INSTRUMENTATION AND CONTROL	XI-1
A. Tokamak EPR Plasma Diagnostics	XI-1
B. First Wall, Blanket and Shield Systems	XI-4
C. Tritium Facility	XI-6
D. Plasma Support Systems	XI-7
E. Magnet System	XI-8
CHAPTER XII. REACTOR ENGINEERING SYSTEMS	XII-1
A. Structural Support Systems	XII-1
1. Blanket/Shield System Support	XII-1
2. Magnet Coil Structural Support	XII-3
B. Reactor Assembly	XII-9
1. Prerequisites - Pre-assembly Status	XII-9
2. Assembly	XII-10
3. Schedule Sequence	XII-13
C. Remote Maintenance	XII-14
1. General Approach	XII-14
2. Specific Repairs	XII-18
3. Blanket and Shield Block Removal and Replace- ment Procedure	XII-19
CHAPTER XIII. REACTOR AND FACILITIES	XIII-1
A. Reactor Building	XIII-1
B. Mockup Maintenance Building	XIII-5
C. Tritium Facility	XIII-6
D. Power Supply Facilities	XIII-6
E. Cryogenic Facilities	XIII-7
F. Heat Transport and Cooling Tower Facilities	XIII-7
G. Control Center	XIII-8

	<u>Page</u>
H. Turbine Generator Facility	XIII-8
I. Power Substation	XIII-8
CHAPTER XIV. ENVIRONMENTAL AND ENGINEERING SAFEGUARDS	XIV-1
A. Preliminary Hazard Analysis	XIV-1
B. Potential Off-site Tritium Exposures	XIV-4
C. Radioactivity, Afterheat, and Biological Dose	XIV-7
CHAPTER XV. RESOURCE REQUIREMENTS	XV-1
A. Materials Inventory	XV-1
B. Cost Estimate	XV-1
C. Manpower and Funding Profiles	XV-1
D. Schedule	XV-4

VOLUME II

LIST OF FIGURES	<i>xv</i>
LIST OF TABLES	<i>xix</i>
APPENDIX A. SUMMARY OF EPR DESIGN PARAMETERS	A-1
APPENDIX B. IMPURITY CONTROL	B-1
1. Computational Model	B-1
2. Analysis	B-18
3. Technological Feasibility of First-Wall Surface Modifications	B-25
a. Low-Z Coating	B-28
b. Low-Z Separated Monolithic Liner	B-31
c. Carbon Curtain Liner	B-33
d. Low-Sputtering Coating	B-34
APPENDIX C. PLASMA CALCULATIONAL MODELS	C-1
1. Global Model	C-1
a. Balance Equations	C-1

	<u>Page</u>
b. Transport Model	C-4
c. Trapped Ion Mode (Particles, Electron Energy, and Ion Energy)	C-6
2. MHD Equilibrium Model	C-7
3. Global Model Parameters	C-12
4. Plasma Control Algorithm	C-15
5. Core-Corona Model	C-17
6. Plasma Initiation Model	C-22
a. Electron, Ion, and Neutral Density	C-22
b. Electron and Ion Energy Balance	C-23
c. Plasma Current	C-24
d. Runaway Electrons	C-24
APPENDIX D. STRUCTURAL SUPPORT SYSTEM	D-1
1. Summary	D-1
2. EPR Definition	D-4
3. Material Selection	D-7
4. Structural Analysis Considerations	D-9
5. Magnet Support Considerations	D-10
6. Design Development	D-12
a. The Torque Shell	D-13
b. Torque Frame Concept	D-19
7. Concept Comparison	D-22
a. TF Coil Support	D-22
b. Overhead Support Frames	D-23
c. Access/Maintenance	D-23
d. Weight Comparisons	D-24
e. Cost Comparisons	D-24
8. Recommendations for Further Study	D-26

	<u>Page</u>
APPENDIX E. MATERIALS CONSIDERATIONS FOR THE PRIMARY ENERGY CONVERSION SYSTEM	E-1
1. Structural Materials	E-2
a. Austenitic Stainless Steel	E-3
b. Alternate Structural Materials	E-14
2. Insulators	E-15
a. Electrical Properties	E-16
b. Radiation Swelling	E-17
c. Mechanical Properties	E-17
d. Physical Properties	E-18
3. Low-Z Liner Materials	E-18
4. Blanket and Shield Materials	E-20
a. Boron Carbide	E-20
b. Graphite	E-21
c. Lead Mortar and Aluminum	E-21
APPENDIX F. MAGNETICS	F-1
1. Normal Metal Flux Shield for Toroidal Field Coils	F-1
2. Eddy Current Effects in the Subdivided Blanket and Shield	F-3
a. Unsegmented Blanket and Shield	F-3
b. Blanket and Shield Segmented Toroidally	F-4
c. Subdivided Blanket and Shield Represented by a Brick Model	F-5
d. Limitations to the Model Due to Assumptions	F-6
e. Phase Delay and Time Delay	F-7
APPENDIX G. NEUTRONICS PENETRATION ANALYSIS	G-1
1. General Considerations	G-1
2. Calculational Model	G-3
3. Analysis of Unshielded Penetrations	G-6
4. Shielding of Major Penetrations	G-10

	Page
APPENDIX H. FIRST WALL STRESS ANALYSIS	H-1
1. Effect of Constraints on First Wall Thermal Stresses . .	H-5
2. Stress in Uniform-Thickness Wall	H-8
a. Solid Wall	H-8
b. Sandwich Wall	H-1
3. Stress in Ring-Stiffened Shell	H-1
a. Effect of Ring Weight, Shell Weight, and Pressure Differential on Ring Dimensions	H-1
b. Effect of Support Location on Ring Dimensions . . .	H-1
c. Tapered Rings	H-1
d. Effect of Coolant Panel Weight on Ring Di- mensions	H-2
e. Stress Concentrations	H-2
4. Buckling	H-2
5. Strain Cycling of Coolant Panels	H-2
a. Fixed Supports	H-3
b. Sliding Supports	H-3
APPENDIX I. ENRICHMENT OF ISOTOPES OF HYDROGEN BY CRYOGENIC DISTILLATION	I-1
1. Development of the Computer Program	I-1
a. General Considerations	I-2
b. Mathematical Formulation	I-4
c. Convergence Criteria	I-5
d. Description of the Computer Code	I-6
e. General Features of the Analysis Results	I-7
2. Cryogenic Enrichment Scenario for the EPR	I-2
a. Analysis of the EPR Cascade	I-2
b. Equilibration	I-2
3. Cryogenic Enrichment Scenario for More Stringent Enrich- ment Requirements	I-3
4. Conclusions	I-4
APPENDIX J. NONCIRCULAR PLASMA CONSIDERATIONS	J-1

LIST OF FIGURES

		Page
II-1	Perspective View	II-2
II-2	EPR Vertical Section Dimensions	II-3
II-3	Detailed Vertical Section of EPR	II-5
II-4	EPR Plan View	II-6
II-5	MHD Equilibria at $B_{\text{max}}^{\text{TFC}} = 10 \text{ T}$ -- Total Beta	II-9
II-6	Burn Cycle Scenario	II-12
II-7	Effect of OH Current Reversal Time on Plasma Driving and Heating System Requirements	II-14
II-8	Power Components During Reference Burn Cycle	II-16
II-9	Net Energy Flow in Reference Burn Cycle	II-18
II-10	Power Performance as a Function of Operating Mode	II-20
II-11	Plasma Breakdown -- Normal Start-up Cycle	II-21
II-12	EPR Magnet Schematics	II-24
II-13	Reference Sheet Conductor (60 KA) for TF Coil and its Coil Cross Section (Jelly Roll Winding)	II-27
II-14	40 KA Cable for Poloidal Coils	II-31
II-15	Beam Line Design	II-35
II-16	Circuit Diagram	II-39
II-17	Cross Section of the Region of Insulating Cylinders and Conducting Drums for a Typical CCG	I-41
II-18	Vacuum Vessel Structure	I-46
II-19	Coolant Panel	I-47
II-20	Transient temperature histories (0.5 MW/m^2) locations on the coolant exit plane at the surface of the beryllium coating (upper curve), at the surface of the stainless steel in contact with the coating (middle curve), and at the stainless steel surface in contact with the coolant (lower curve).	I-51
II-21	EPR Blanket/Shield Arrangement	I-52

	<u>Page</u>
II-22 Blanket Block	II-53
II-23 Neutron heating in the water coolant as a function of depth in the blanket/shield for three locations with respect to the neutral beam duct: a - at the wall of the beam duct, b - at 10 cm from the wall of the beam duct, and c - at 30 cm from the wall of the beam duct	II-58
II-24 Atomic displacement in stainless steel as a function of depth in the blanket/shield for three locations with respect to the neutral beam duct: a - at the wall of the beam duct, b - at 10 cm from the wall of the beam duct, and c - at 30 cm from the wall of the beam duct	II-59
II-25 Radioactivity and Biological Hazard Potential as a Function of Time After Shutdown Following Two Years of Operation	II-61
II-26 Fuel Cycle Scenario for the ANL/EPR	II-65
II-27 Reactor Containment Building	II-67
III-1 Radial Profiles	III-3
III-2 MHD Equilibria at $B_{\max}^{\text{TFC}} = 10 \text{ T}$ -- Total Beta	III-5
III-3 MHD Equilibria at $B_{\max}^{\text{TFC}} = 10 \text{ T}$ -- Poloidal Beta	III-6
III-4 MHD Equilibria at $B_{\max}^{\text{TFC}} = 10 \text{ T}$ -- Safety Factor	III-7
III-5 MHD Equilibria at $B_{\max}^{\text{TFC}} = 10 \text{ T}$ -- Flux and Current Contours	III-9
III-6 MHD Equilibria at $B_{\max}^{\text{TFC}} = 10 \text{ T}$ -- Profiles	III-10
III-7 Confinement Requirement for Subignition, Beam-Driven Operation	III-21
III-8 Power Amplification for Subignition, Beam-Driven Operation	III-22
III-9 Power Output for Subignition, Beam-Driven Operation	III-24
III-10 Neutron Wall Load for Subignition, Beam-Driven Operation	III-25
III-11 Fast Alpha Escape Fraction	III-29
III-12 Alpha Power Deposition Profile	III-30

	<u>Page</u>
III-13 Toroidal Field Ripple	III-34
III-14 Plasma Driving System Circuit Diagram	III-35
III-15 Burn Cycle Scenario	III-41
III-16 Effect of OH Current Reversal Time on Plasma Driving and Heating System Requirements	III-44
III-17 Effect of Beam Initiation Time on Plasma Driving and Heating System Requirements	III-45
III-18 Effect of Beam Power on Plasma Driving and Heating System Requirements	III-46
III-19 Effect of Plasma Resistivity on Plasma Driving and Heating System Requirements	III-47
III-20 Effect of Oxygen Impurity on Plasma Driving and Heating System Requirements	III-48
III-21 Plasma, OH and EF Currents During Reference Burn Cycle	III-54
III-22 Poloidal Beta During Reference Burn Cycle	III-55
III-23 OH Coil System Power and Energy Transfer During Reference Burn Cycle	III-56
III-24 EF Coil System Power and Energy Transfer Driving Reference Burn Cycle	III-57
III-25 Power and Energy Transfer Out of the Energy Storage Unit During Reference Burn Cycle	III-58
III-26 Particle Densities and Refueling Current During Reference Burn Cycle	III-60
III-27 Ion and Electron Temperatures During Reference Burn Cycle	III-62
III-28 Power Component, During Reference Burn Cycle	III-65
III-29 Power Components During Startup	III-66
III-30 Cumulative Energy Components During Reference Burn Cycle	III-69
III-31a Net Energy Flow in Reference Burn Cycle	III-71
III-31b Net Energy Flow in Reference Burn Cycle	III-74

	<u>Page</u>
III-32 Power Performance as a Function of Operating Mode	III-75
III-33 Power Performance as a Function of OH Current Re- versal and Beam Turn On Times	III-77
III-34 Power Performance as a Function of Injected Beam Power	III-79
III-35 Power Performance as a Function of Energy Transfer Efficiencies	III-80
III-36 Power Performance as a Function Plasma Edge Temperature	III-82
III-37 Power Deposition Profile, Per Unit Volume	III-84
III-38 Power Deposition Profile, Per Plasma Ion	III-85
III-39 Plasma Breakdown -- Normal Start-up Cycle	III-93
III-40 Plasma Breakdown -- Sensitivity	III-95
III-41 Voltages During Startup	III-97
III-42 Plasma Breakdown -- Alternate Start-up Cycle	III-99
III-43 Effect of Oxygen Impurity on Volt-Second Requirements . .	III-102
IV-1 J-H Curve for NbTi versus Temperature	IV-4
IV-2 Field Ripple Versus Extension of Outer Coil Leg	IV-6
IV-3 EPR Magnet Schematics	IV-8
IV-4 Hoop Stresses Distribution of Argonne Pure Tension Coil	IV-11
IV-5 Pure Tension Coil Characteristics	IV-12
IV-6 Pure Tension Coil Profile Versus Number of Coils	IV-13
IV-7 Reference Cycles for OH Coil, Plasma and EF Coil	IV-17
IV-8 Time Evaluation of Out of Plane Load (no shield)	IV-18
IV-9a $B_{\parallel}$ to TF Coil Due to EF, PL, and OH	IV-20
IV-9b $B_{\perp}$ to TF Coil Due to EF, PL and OH	IV-21
IV-10 Cross Section of Cryostat and Coil Form	IV-22
IV-11 Magnetoresistance of Copper	IV-28

	<u>Page</u>
IV-12	Resistivity of Aluminum as Function of Purity, Temperature and Magnetic Field IV-29
IV-13	AC Losses Distribution Along TF Coil IV-33
IV-14	Out of Plane Load on Al Shield IV-36
IV-15	Locations of Superconducting Shield Coils IV-40
IV-16	Field Screening by Superconducting Shield Coils IV-41
IV-17	Distributions of Heat Sources and Heat Sinks IV-43
IV-18	Heat Transfer Characteristics of Boiling Helium IV-44
IV-19	Reference Sheet Conductor (60 KA) for TF Coil and its Coil Cross Section (Jelly Roll Winding) IV-49
IV-20	10 KA Cable Conductor and its Cryostat IV-52
IV-21	A Conceptual Winding Line for TF Coil IV-53
IV-22	Five Possible Fabrication Tolerances IV-55
IV-23	Tension Distortion of Coil Displacement and Coil Shape Deflection IV-56
IV-24	Out of Plane Load Due to Errors of Rotation, Twist and Tilt IV-58
IV-25	Dumping Circuit IV-59
IV-26	Optimum Operating Temperature of Aluminum Shield . . . IV-67
IV-27	3°K Refrigeration Schematics IV-68
IV-28	Relative Compressor Power for 3°K Refrigerator IV-69
IV-29	Relative First Stage Displacement for 3°K Refrigerator IV-70
IV-30	Relative Mass Flow Rate for 3°K Refrigerators IV-71
IV-31	Schematics of Central Refrigeration Facility IV-73
IV-32a	TF Magnet Structural Support IV-75
IV-32b	TF Magnet Structural Support (Cross-section) IV-76
IV-33	Circular Plasma Boundary IV-85
IV-34	Flux Contour IV-86

	<u>Page</u>
IV-35	ITC Magnet Characteristics IV-88
IV-36	Vertical Force per Unit Length During Burn Phase IV-91
IV-37	Axial Force per Unit Coil Length (10^3 kg/m) IV-92
IV-38a	Axial Pressure, P (kg/cm ²) and Vertical Force, F (tons). . IV-93
IV-38b	Axial Pressure, P (kg/cm ²) and Vertical Force, F (tons). . IV-94
IV-39	40 KA Cable for Poloidal Coils IV-100
IV-40	Poloidal Coil Structure and its Cryostat Con- figuration IV-102
IV-41	Fiberglass Braiding Machine for Nonmetallic Poloidal Coil Cryostat IV-106
V-1	Preliminary Beam Line Design. R_b is the Bending Radius, θ is the Bending Angle in M-1. For 180 keV D^+ , the Gap Field $B_0 = 575$ Gauss in M-1 V-6
V-2	Schematic of Ion Source and Accelerator for a Diverging I Ion Beam. For the initial version of Reference Design 1, $\omega_0 = 0.020$ Rad., $\Omega_0 \text{ max} = 0.0735$ Rad., $R_n = 2.225$ m, $n^0 = 4$ or 5 V-8
V-3	Division of the neutralizer into segments for numerical integration. The second term in the expression for r' is an allowance for beam scattering V-13
V-4	EPR injector power flow diagram V-15
V-5	Gas flow diagram for EPR. V-20
V-6	Neutralizer -- Plasma Portions of the Beam Lines for Reference Designs 1 and 2 and Preliminary Layouts of the Direct Converters and Magnetic Separators in Chamber II V-23
V-7	Power Flow Diagram for Reference Design 3 V-31
V-8	Preliminary layout of D^- beam line for Reference Design 3-a V-33
V-9	Maximum Power Efficiencies for the Neutralization Processes $D^- \rightarrow D^0$ and $D^+ \rightarrow D^0$, $j = 1, 2, 3$ as Functions of Energy per Deuteron j V-34
V-10	ANL Experimental Power Reactor Plan View V-36
V-11	ANL Neutral Beam Injector V-37

	<u>Page</u>
V-12	The "Grill" Structure for LHRH Heating V-41
V-13	Details of Right Angle H-Plane Angle H-Plane Bend and "Grill". V-42
V-14	High Power rf LHRH System for one Port V-43
V-15	A Block Diagram of the High Power Source and Low Power Drive Train for a "Grill". V-46
V-16	Details of the Loops and Power Feed System of ICRH . . . V-48
VI-1	Cross Section of the Region of Insulating Cylinders and Conducting Drums for a Typical CCG VI-4
VI-2	Sketch of CCG Geometry VI-6
VI-3	Magnetic Field Plot for CCG VI-7
VI-4	Goodman Endurance Chart for CCG 17-4 Stainless Steel Cylinders VI-10
VI-5	Circuit Representation for Ohmic Heating Coil System . VI-13
VI-6	Inductor-Converter Bridge Circuit Diagram VI-15
VI-7	Inductor-Converter Voltage Wave forms when $I_s \gg I_E$. VI-17
VI-8	Inductor-Converter Voltage Wave Forms when $I_s = I_E$. VI-18
VI-9	Inductor-Converter Voltage Wave forms when $I_s \ll I_E$. VI-20
VI-10	Relative Timing Sequence for Switching SCR's to Produce Symmetrical Voltage Waves with Inductor-Converter Network for $I_s \gg I_E$, $I_s = I_E$, and $I_s \ll I_E$ VI-21
VI-11	Conventional NBI Power Supply System VI-24
VI-12	HV Power Supply for NBI Block Diagram VI-24
VI-13	10 kHz 3-Phase Switching Circuit for Neutral Beam Injector Power Control VI-27
VI-14	Power and Energy Balance in Pulsed System VI-31
VI-15	Energy Storage and Transfer System VI-33
VI-16	40 kA Energy Storage Conductor VI-36

	<u>Page</u>
VII-1 Vacuum Port Configuration	VII-7
VII-2 Adsorption of hydrogen on molecular sieve 5A at low temperatures and pressures	VII-8
VII-3 Adsorption of helium on molecular sieve 5A at low temperatures and pressures	VII-8
VIII-1 Vacuum Vessel Torus	VIII-6
VIII-2 Coolant Manifold Arrangements	VIII-7
VIII-3 Crossover Manifold Section with Current Breaker	VIII-8
VIII-4 Coolant Channel Arrangement for Coolant Panels and First Wall	VIII-10
VIII-5 Photograph of Panel Coil Section Manufactured by Dean Products, Inc., Brooklyn, N. Y.	VIII-11
VIII-6 Vacuum Wall Segment	VIII-14
VIII-7 Typical Manifold Section Piping	VIII-16
VIII-8 Elevated Temperature Fatigue of Austenitic Stainless Steel	VIII-20
VIII-9 Energy Dependent Physical Sputtering Yields of Iron and Beryllium Irradiated with D ⁺ Ions that have Mono- energetic and Maxwellian Energy Distributions	VIII-25
VIII-10 Temperature Distribution through the Beryllium-Coated Stainless Steel Panel after the First Burn Pulse (45 s) for Locations on the Coolant Inlet Plane (lower curve) and Exit Plane (upper curve)	VIII-35
VIII-11 Temperature Distribution through the Beryllium-Coated Stainless Steel Panel after the Sixth Burn Pulse (345 s) for Locations on the Coolant Inlet Plane (lower curve) and Exit Plane (upper curve)	VIII-36
VIII-12 Transient Temperature Histories (0.5 MW/m ²) Locations on the Coolant Inlet Plane at the Surface of the Beryllium Coating (upper curve), at the Surface of the Stainless Steel in Contact with the Coating (middle curve), and at the Stainless Steel Surface in Contact with the Coolant (lower curve)	VIII-37

VIII-13	Transient Temperature Histories (0.5 MW/m^2) Locations on the Coolant Exit Plane at the Surface of the Beryllium Coating (upper curve), at the Surface of the Stainless Steel in Contact with the Coating (middle curve), and at the Stainless Steel Surface in Contact with the Coolant (lower curve)	VIII-37
VIII-14	Transient Temperature Histories (1.0 MW/m^2) Locations on the Coolant Inlet Plane at the Surface of the Beryllium Coating (upper curve), at the Surface of the Stainless Steel in Contact with the Coating (middle curve), and at the Stainless Steel Surface in Contact with the Coolant (lower curve)	VIII-38
VIII-15	Transient Temperature Histories (1.0 MW/m^2) Locations on the Coolant Exit Plane at the Surface of the Beryllium Coating (upper curve), at the Surface of the Stainless Steel in Contact with the Coating (middle curve), and at the Stainless Steel Surface in Contact with the Coolant (lower curve)	VIII-39
VIII-16	Maximum Stress in Reinforcing Ring Supported at Various Locations	VIII-42
IX-1	EPR Circular Blanket/Shield Layout	IX-10
IX-2	A Schematic Reference of Toroidal Geometry	IX-16
IX-3	Source Strength, $S(R,Z)$, of the D-T Neutrons in the Plasma Region as a Function of Major Radius at Several Values of Z	IX-18
IX-4	Isometric View of the DT Neutron Source Strength in the EPR Plasma Region	IX-19
IX-5	Variation of the Neutron Wall Loading with the Poloidal Angle χ for Three Cases: a = neutron source distribution as derived from the MHD equilibria in the reference plasma calculations, b - uniform neutron source distribution, c - the nominal case of a uniform source distribution in an infinite cylinder approximation	IX-20
IX-6	Variation of the Helium Production Rate within the First 1-cm of the First Wall with the Poloidal Angle χ	IX-22
IX-7	Variation of Atomic Displacements within the First 1-cm of the First Wall with the Poloidal Angle χ	IX-23
IX-8	Atomic Displacement as a Function of Distance from First Wall in the Reference EPR Design (Reference Source) at $\chi = 4^\circ$ and $\chi = 176^\circ$	IX-26

	<u>Page</u>
IX-9 Helium and Hydrogen Production as Functions of Distance from First Wall in the Reference EPR Design (Reference Source) at $\chi = 4^\circ$ and $\chi = 176^\circ$	IX-27
IX-10 Spatial Distribution of Nuclear Heating in the EPR First Wall and Blanket for a Neutron Wall Loading of 0.5 MW/m^2	IX-30
IX-11 Spatial Distribution of Atomic Displacement in the EPR First Wall and Blanket	IX-31
IX-12 Spatial Distribution of Helium and Hydrogen Production Rates in the EPR First Wall and Blanket. Also shown is the Spatial Dependence of He-to-dpa Ratio	IX-32
IX-13 Spatial Distribution of Heating Rates in the EPR Outer Shield for a Neutron Wall Loading of 0.5 MW/m^2	IX-34
IX-14 Spatial Distribution of Heating Rates in the EPR Inner Shield for a Neutron Wall Loading of 0.5 MW/m^2	IX-35
IX-15 Spatial Distribution of Helium and Hydrogen Production Rates in the Inner Shield (in the midplane) of the EPR Design	IX-38
IX-16 Spatial Distribution of Nuclear Heating in the Inner Segment of the EPR Toroidal Field Coil (in the mid-plane) for a Neutron Wall Loading of 0.5 MW/m^2	IX-40
IX-17 Atomic Displacements and Radiation-induced Resistivity (ρ_r) in Copper (stabilizer) as a Function of Depth within the Inner Segment of the TF Coils after Reactor Operation for an Integral Wall Loading of 1 MW-yr/m^2	IX-42
IX-18 Dose Absorbed in Mylar and EPOXY (with aromatic type curing agent) Insulators as Function of Depth within the Inner Segment of the Toroidal Field Coil	IX-43
IX-19 Schematic of Geometry Representation for Analysis of Neutral Beam Penetrations and their Shield	IX-46
IX-20 Total Neutron Flux (normalized to a neutron wall loading of 1 MW/m^2) as a function of Depth within the Blanket and Bulk Shield at Two Locations; a - a radial line far removed from penetration effects, and b - a surface parallel to the walls of the neutral beam duct and is 5 cm away	IX-54

	<u>Page</u>
IX-21 Neutron Heating in the Water Coolant as a Function of Depth in the Blanket/Shield for Three Locations with Respect to the Neutral Beam Duct: a - at the wall of the beam duct, b - at 10 cm from the wall of the beam duct, and c - at 30 cm from the wall of the beam duct	IX-56
IX-22 Atomic Displacement in Stainless Steel as a Function of Depth in the Blanket/Shield for Three Locations with Respect to the Neutral Beam Duct: a - at the wall of the beam duct, b - at 10 cm from the wall of the beam duct, and c - at 30 cm from the wall of the beam duct	IX-58
IX-23 A Vertical Cross Section for the Blanket/Shield Showing Schematically the Movable Shield Plug for the Evacuation Ducts	IX-60
IX-24 EPR Blanket/Shield Arrangement Circular Option	IX-65
IX-25 EPR Alternate Slab Type Blanket/Shield Arrangement . . .	IX-67
IX-26 Typical Blanket Block	IX-70
X-1 Fuel Cycle Scenario for ANL/EPR	X-8
X-2 Fuel Cycle Module Connection Housing	X-11
X-3 Cryogenic Enrichment Scenario for EPR-1	X-13
X-4 Block Diagram of the Detritiation System	X-25
X-5 Tritium Accumulation in the EPR First Wall Cooling Water Circuit	X-27
XII-1 Shield Support Framework	XII-2
XII-2 Torque Shell Structural Support	XII-4
XII-3 EPR Torque Frame Structural Support	XII-6
XII-4 Reactor Assembly Sequence	XII-11
XII-5 Reactor Assembly Sequence	XII-12
XII-6 Basic Conceptual Design of Internal Remote Repair Machine	XII-16
XIII-1 Overall Site View	XIII-2
XIII-2 Reactor Facility Elevation	XIII-3

		<u>Page</u>
XIII-3	Reactor Facility Plan View	XIII-4
XIV-1	Radioactivity	XIV-8
XIV-2	Afterheat as a Function of Time after Shutdown Following Two Years of Operation	XIV-12
XV-1	EPR Design and Construction Schedule	XV-7

LIST OF TABLES

	<u>Page</u>
I-1 EPR Objectives	I-1
I-2 Tokamak Plasma Physics Experiments -- Estimated Parameters	I-4
II-1 EPR Geometrical Parameters	II-4
II-2 Steady-State Plasma Parameters -- Reference Design	II-11
II-3 Plasma Driving and Heating System Maximum Require- ments	II-15
II-4 Energy Balance -- Reference Case Burn Cycle	II-17
II-5 Toroidal Field Coil System	II-23
II-6 TF Coil Heat Dissipation Summary	II-26
II-7 OH/EF Coil Magnet Characteristics	II-30
II-8 Neutral Beam Injection System Characteristics	II-33
II-9 RF Heating Parameters	II-37
II-10 Energy Transfer and Storage Systems -- Maximum Ratings	II-38
II-11 Vacuum System Parameters	II-44
II-12 First-Wall Design Parameters	II-48
II-13 First-Wall Operating Parameters and Design Limits	II-49
II-14 Summary of Blanket Design Parameters	II-56
II-15 Summary of Shield Design Parameters	II-57
II-16 Summary of Tritium-Handling Facility Parameters	II-64
II-17 Atmospheric Cleanup System	II-66
II-18 Plant Capital Investment Direct Cost Estimate	II-69
III-1 Plasma Performance Reference Design Summary - Steady- State Conditions	III-12
III-2 Sensitivity of the Plasma Performance to the Diagon- netic/Paramagnetic Parameter, δ	III-14

	<u>Page</u>
III-3 Plasma Performance of the Optimum Equilibria as a Function of Profile	III-15
III-4 Plasma Performance as a Function of Temperature and Density Profiles	III-17
III-5 Plasma Performance as a Function of Temperature . . .	III-18
III-6 Subignition Plasma Performance as a Function of Neutral Beam Power	III-20
III-7 First-Wall Particles and Radiation Currents for the EPR Reference Design	III-26
III-8 Neutron Flux and Currents at the Inner Surface of the EPR First Wall	III-27
III-9 MHD Equilibria for the Peak Plasma Current, $I_P^{\max} = 7.58 \text{ MA}$	III-37
III-10 Capacitance Requirements for OH Homopolar Generator .	III-42
III-11 Plasma Driving and Heating System Requirements . . .	III-43
III-12 Plasma Heating and Driving System Reference Case Parameters	III-51
III-13 Energy Balance -- Reference Case Burn Cycle	III-72
III-14 Refueling and Replenishment Rates-Reference Case . .	III-88
IV-1 ANL-EPR Toroidal Field Coil System	IV-9
IV-2 Typical Specifications of 60 kA TF Coil Conductor . .	IV-24
IV-3 AC Loss in Aluminum Shield	IV-34
IV-4 TF Coil Heat Dissipation Summary	IV-65
IV-5 Pressure Range of Dense Gas Refrigerator	IV-72
IV-6 Poloidal Coil Winding Configurations	IV-81
IV-7 Poloidal Coil Magnet Characteristics	IV-82
IV-8 Hoop Stress and Radial Pressure of Poloidal Coils During Burn Phase	IV-97
IV-9 Induced Field in the Plasma Region due to Eddy Currents in the Segmented or Subdivided Blanket and Shield	IV-108

V-1	Ion Source Design Characteristics	V-3
V-2	Results of Calculations for D^+ and D^0 Beam Transport and Neutralizer Gas Loads (Case I)	V-9
V-3	Results of Calculations for D^+ and D^0 Beam Transport and Neutralizer Gas Loads (Case II)	V-10
V-4	Results of Calculations for D^+ and D^0 Beam Transport and Neutralizer Gas Loads (Case III)	V-12
V-5	Results of D_2^+ and D_3^+ Beam Transport Calculations for the Beam Line of Figure V-1	V-14
V-6	Cross Sections for Charge Changing Collisions and Dissociation for 90 keV Hydrogen and 180 keV Deuterium Particles	V-16
V-7	Gas Loads to Pump Chamber I, II and Torus	V-18
V-8	Neutral Injector Gas Loads (per Injector) for the Design of Table V-4 with $P_B = 60$ MW and $N_S = 24$ Ion Sources (in Twelve Injectors)	V-19
V-9	Chamber I Pumping Speeds and Pressures and Fractional Losses of D^+ , D_2 and D_3 for the Design of Table V-4 with $P_B = 60$ MW and $N_S = 24$ Ion Sources	V-21
V-10	Chamber II Pumping Speeds and Pressures and Fractional Losses of D^0 and D^+ for the Design of Table V-4 with $P_B = 60$ MW and $N_S = 24$ Ion Sources	V-24
V-11	Neutral Beam Reference Design Summaries	V-25
V-12	Values of the Components of Fractional Recovered Power in the Power Flow Diagram of Figure V-4 for Reference Designs 1 and 2 with $V_+ = 0.18$ MV	V-28
V-13	Calculated Values of Ion Beam Power, P_+ , Ion Beam Power Recovery Efficiency, P_R/P_+ , and Overall Neutral Injector Efficiency, ϵ_{NI} , for Reference Designs 1 and 2 with $P_B = 60$ MW, $\eta_1 = \eta_2 = 0.85$, $\eta_3 = 0.3$ and $k = 1.1$	V-29
V-14	Values of P_+ , $k P_+$, $P_{R_{1,2}}$, P_{R_3} , P_{IPS} and ϵ_{IPS} for Reference Designs 1 and 2	V-29
V-15	RF Heating Parameters	V-50

	<u>Page</u>
VI-1 Parameters for a Single CCG Module	VI-8
VI-2 SCR Design Parameters for EF Coil Inverter-Converter Network Design	VI-22
VI-3 Energy Transfer and Storage Systems -- Maximum Ratings	VI-34
VII-1 EPR Gas Load After Burn	VII-2
VII-2 EPR Toroidal Vacuum System Parameters	VII-4
VII-3 Contribution to EPR Gas Load from First Wall Particle Currents During Burn	VII-4
VII-4 Cryosorption Pumping of Helium as a Function of Temperature	VII-9
VII-5 Cryogenic Requirements for Thirty-Two 25,000 l/s (Hydrogen) Cryosorption Pumps	VII-9
VII-6 Neutral Beam Vacuum System Parameters	VII-14
VII-7 Cryogenic Requirements for 12 Neutral Beam Injectors	VII-14
VIII-1 First Wall Design and Operating Parameters	VIII-2
VIII-2 First-Wall Materials Selection	VIII-17
VIII-3 Assessment of Water and Helium as Primary Coolants for an EPR	VIII-30
VIII-4 Summary of Coolant Panel Heat-Transfer Analysis for the Eight Panel	VIII-33
IX-1 Summary of Blanket/Shield Design Parameters for the ANL-EPR	IX-2
IX-2 Dimensions and Material Compositions of the EPR Inner Blanket/Shield at Midplane	IX-12
IX-3 Dimensions and Material Compositions of the EPR Outer Blanket-Bulk Shield at Midplane	IX-13
IX-4 Composition and Atom Densities of Lead Mortar	IX-14
IX-5 Rates of Helium and Hydrogen Production in Several Low-Z Coating Materials	IX-28
IX-6 Helium and Tritium Production Rates in Boron Carbide in the ANL-EPR Inner Shield	IX-36

	<u>Page</u>
IX-7	Total Neutron Fluxes in the Toroidal-Field Coils and Beam Ducts (normalized to a neutron wall loading of 1 MW/m^2) for three cases: (1) no penetrations, (2) unshielded neutral beam ducts, and (3) partial shield of neutral beam duct IX-48
IX-8	Torques on Typical Blanket and Shield Blocks IX-64
IX-9	Thermal Fluid Analysis for a Pressurized Water Cooled EPR Blanket Module IX-75
IX-10	Thermal Fluid Analysis for a Water Cooled EPR Shield Block IX-76
X-1	Summary of Tritium Handling Facility Parameters X-2
X-2	Summary of Operating Parameters for the EPR Cryogenic Distillation Cascade X-14
X-3	Column Diameters and Liquid Holdup for Selected Superficial Vapor Velocities X-17
X-4	Analysis of Recycle Flow Scenario X-20
X-5	Analysis of Cyclic Flushing Scenario X-20
X-6	Scrubber Plant Catalyst Cost Versus Clean-Up Requirements Catalyst Operating Temperature and Flow Rate . . . X-23
XI-1	Plasma Diagnostic Requirements XI-3
XI-2	Instrument Requirements for the Magnet Systems XI-10
XI-3	Toroidal Magnet Alarm and Interlock Actions XI-12
XII-1	Weights of Major Reactor Components XII-14
XIV-1	Potential Hazard Sources XIV-2
XIV-2	Specific Radioactivity in First Wall XIV-10
XIV-3	Biological Dose, in mrem/hr, After Shutdown Following Two Years of Operation XIV-13
XV-1	Materials Requirement XV-2
XV-2	Plant Capital Investment Direct Cost Estimate XV-3
XV-3	Project Manpower and Cost Profiles XV-5

CHAPTER I

INTRODUCTION

The objectives which guided this EPR design evolved from technological and programmatic considerations. The EPR should demonstrate the technological feasibility of tokamak power reactors. This requirement must be reconciled with the current status and projected program in plasma physics, the state-of-the-art and development requirements for the various technologies, with the programmatic requirement for initial operation in the mid-to-late 1980's or early 1990's, and with the resources which may reasonably be expected to be available for the task. On the basis of these considerations, the objectives shown in Table I-1 were identified.

Table I-1. EPR Objectives

- Operate at or near net electrical power conditions.
 - Operate with a plant capacity factor of 25-50% for a design lifetime of 5-10 years.
 - Demonstrate plasma confinement, stability, fueling, heating, control, etc. in the reactor regime.
 - Demonstrate most fusion reactor technologies in a power reactor environment.
 - Provide for materials irradiation testing.
 - Demonstrate tritium breeding, advanced coolants and materials, and advanced blanket concepts in test modules.
-

Operation at or near net electrical-power conditions imposes four important secondary requirements upon the EPR. First, a D-T plasma must be confined at or near ignition conditions. Second, the reactor thermal power output must be several hundred megawatts. Third, the reactor must operate with a significant duty cycle, probably $\sim 50\%$. Fourth, sensible heat must be removed from the blanket in a form suitable for conversion to electricity, using conventional power cycle technology, with a reasonable efficiency, probably 20-30%. These secondary requirements can be traded off against each other, to some extent.

Operation with a plant capacity factor (capacity factor $\equiv$ duty cycle $\times$ plant availability) of 25-50% for 5-10 years imposes a number of important secondary requirements upon component lifetime, reliability and maintenance. These requirements are generally over-and-above, although interrelated with, those imposed by net electrical power operation.

These first two objectives, together with the present understanding of stable plasma confinement and a judgement as to feasible extensions of several technologies within the time permitted by the schedule, determine the major characteristics of the EPR design. The minimum blanket plus shield thickness (Δ_{BS}) is determined by the maximum allowable accumulated radiation damage to the superconducting toroidal field coil over the design lifetime of the EPR, which depends upon the power output, capacity factor and design lifetime. The thermonuclear power density in the plasma scales as $P_{th} \propto \beta_p^2 B_t^4 / q^4 A^{3/2}$, and the toroidal field (B_t) in the plasma is reduced relative to the maximum field at the toroidal coil as $B_t = B_{max} \times (1 - (r_w + \Delta_{BS})/R)$, where R is the major radius and r_w is the vacuum chamber radius. An upper limit on B_{max} with NbTi superconducting technology is about 10 Tesla. $A = R/a$ is the aspect ratio, where a is the plasma radius. Allowable values of β_p and q are determined from plasma MHD equilibrium considerations. Plasma energy confinement scales as $n\tau_E \propto \beta_p^2 B_t^6 / q^4 A^{3/2}$ in trapped-ion-mode theory, and the design is based upon the judgement that the trapped-ion-mode theory underpredicts $n\tau_E$ by a factor of $\sim 1-5$. The design is based upon a circular plasma cross section, despite the greater potential power density of noncircular cross sections, because of the extremely limited experimental basis for the latter. Extensive parametric surveys were performed during the scoping studies^{1,2} and refined during the conceptual design study to determine the size of the EPR that will be required to satisfy the first two objectives.

The third and fourth objectives in Table I-1 are concomitant to the first objective, but represent distinct and significant accomplishment in themselves, even if the first two objectives are not accomplished. The EPR could operate somewhat below electrical power breakeven conditions and still demonstrate the feasibility of both plasma physics and reactor technology for tokamak power reactors.

An EPR which accomplish the first two objectives will represent a unique test facility. Its utilization as such was considered a design objective, albeit with the constraint that the primary design objectives were not compromised.

The EPR represents a considerable extrapolation in plasma conditions beyond those achieved to date and those which are anticipated for the "current generation" of tokamak experiments -- those which will be coming on line in the next few years -- which must provide its plasma physics design basis (see Table I-2). The reference design point is defined by a reasonable set of assumptions about the plasma parameters, but provision is made for operation with less favorable parameters. For example, supplemental beam heating is provided for operation in the beam-driven mode in the event that the energy confinement turns out to be subignition.

The EPR also represents a considerable extrapolation in many areas of fusion reactor technology. The design philosophy in this regard was to minimize technology extrapolations, consistent with accomplishing the EPR objectives, while insuring, insofar as possible, that the necessary technology developments would be relevant to subsequent tokamak reactors. Identification of R&D required for the EPR was a specific goal of the design study, and a separate report³ has been issued on this subject.

The general design philosophy was to develop an integrated design in sufficient detail that the major design problems would be uncovered and realistic solutions found. A balanced solution, in which the performance requirements for the various systems represent roughly comparable demands upon the respective technologies, was stressed for those design problems that involved two or more interacting systems. Special emphasis was placed upon developing realistic and credible solutions to design problems, subject to the constraints imposed by the schedule on technology development.

-
1. W. M. Stacey, Jr., et al., "Tokamak Experimental Power Reactor Studies," ANL/CTR-75-2, Argonne National Laboratory (June 1975).
 2. W. M. Stacey, Jr., P. Bertoncini, J. Brooks, and K. Evans, "Effect of Plasma Confinement and Impurity Level Upon the Performance of a D-T Burning Tokamak Experimental Power Reactor," ANL/CTR/TM-53, Argonne National Laboratory (August 1975).
 3. F. E. Mills, et al., "Research and Development Required for the Tokamak Experimental Power Reactor," ANL/CTR/TM-65, Argonne National Laboratory (May 1976).

Table I-2. Tokamak Plasma Physics Experiments — Estimated Parameters

Experiment	Major Radius R (m)	Plasma Radius a (m)	Plasma Current I (MA)	Ion Temp. T_i (keV)	Density n (m^{-3})	Energy Confinement τ_E (s)	Flattop Time t_f (s)	Experimental Data Available	Comments
<u>Current Generation</u>									
PLT	1.3	0.45	1.6	3	1×10^{20}	0.3		1976-77	
T-10	1.5	0.40	1.1	2		0.1	1	1976-77	
D-111	1.4	0.45 (a = 3)	5.0	5	2×10^{20}	0.5	1	1978-79	Configurational stability — doublet.
PDX	1.4	0.60	0.9	2-4	$4-8 \times 10^{19}$	0.3	1	1978-79	Noncircular configurational stability. Divertor, impurity studies.
<u>Next Generation</u>									
JET	2.96	1.25 (a = 1.7)	4.8 ^a	5	5×10^{19}	1	15	1981-82	$1-5 \times 10^3$ D-T pulses.
TFTR	2.48	0.85	1.0 2.5	6 6	8×10^{19} 4×10^{19}	0.2	1 1	1981-82	4×10^3 D-T pulses.
TFTR Upgrade	4-5	3-4	4-5				5		Conjectural - no plans.
JT-60	3	1	3.3	5-10	$2-10 \times 10^{19}$	0.2-1	10	1982-83	
T-20	5	2	6	7-10	$1-5 \times 10^{19}$	2	5-20	1983-84	10^5 D-T pulses
<u>Experimental Power Reactor</u>									
EPR	6.25	2.1	7.6	10	1×10^{20}	5-10	30-60		

^a Ultimate.

CHAPTER II

SUMMARY

An integrated conceptual design has been developed for a tokamak Experimental Power Reactor (EPR) that would accomplish the objectives that were described in the previous chapter. Whenever there was a choice of design options, the conservative approach, with minimum technological extrapolation, was chosen in order to develop a realistic design. This chapter summarizes the conceptual design, including the performance evaluation and cost estimate.

A. Reference Design

The EPR is a tokamak with a major radius of 6.25 m and a circular plasma of 2.1 m radius, which is designed to achieve ignition conditions with energy confinement $\eta\tau_E = 2.4 \times 10^{20} \text{ s/m}^3$. Operating in a repetitively pulsed mode, with ~ 1 pulse/min. and $\sim 75\%$ duty cycle, the EPR is capable of producing 72 MWe gross power and 30 MWe net power.

The design-basis performance objective of the EPR is to operate for 10 years with a plant capacity factor of 50%. The plant capacity factor is defined as the product of the duty cycle (75%) and the plant availability (67%).

A perspective view of the EPR is shown in Figure II-1. The principal geometric parameters are given in Table II-1 and in the vertical section view shown in Figure II-2. A detailed vertical section view is shown in Figure II-3, and a plan view is shown in Figure II-4.

Surrounding the plasma is a stainless steel vacuum vessel with detachable, beryllium-coated cooling panels mounted on the interior (plasma) side. An 0.28 m thick blanket, assembled from 272 stainless steel blocks, surrounds the vacuum vessel. Shield blocks, varying from 0.6 m to 1 m in thickness, surround the blanket. The inner shield is constructed of alternating layers of stainless steel and B_4C to achieve high radiation attenuation in a limited space. The outer shield is constructed primarily of lead mortar and graphite. Special local shielding around penetrations is constructed of stainless steel and B_4C . The first wall, blanket and shield are cooled by pressurized H_2O and are supported from beneath by 16 frames.

A toroidal field at the center of the plasma of 4.47 T is provided by 16 cryostatically-stable, NbTi superconducting toroidal field (TF) coils operating

Figure II-1. Perspective View

Figure II-2. EPR Vertical Section Dimensions

Table II-1. EPR Geometrical Parameters

Radius (m)	
Major, R_0	6.25
Plasma, a	2.1
First wall, r_w	2.4
Aspect Ratio, $A = R/a$	2.98
Plasma Volume (m^3)	544
Toroidal Vacuum Chamber Volume (m^3)	711
First Wall Area (m^2)	592
Blanket Thickness (m)	0.28
Shield Thickness (m)	
Inside	0.58
Outside	0.97
Toroidal Field Coils	
No.	16
Horizontal bore, R_{bore} (m)	7.78
Vertical bore, Z_{eff} (m)	12.6

Figure II-3. Detailed Vertical Section of EPR

at 3°K with a maximum field in the coil of 10 T. The TF coils are supported by an inner cryogenically-cooled support cylinder and by lower support legs and are restrained against out-of-plane loads by a torque shell.

Cryostatically-stable, NbTi superconducting ohmic-heating (OH) and equilibrium-field (EF) coils are located external to the TF coils. The OH coil system consists of 10 ring coils and a central solenoid and is designed to provide 85 V-s to the plasma. The EF coil system consists of 16 ring coils and is designed to provide 50 V-s to the plasma. The OH coils are designed to operate at a peak field of 5 T, a peak field rise of 6.7 T/s, a peak current of 80 kA and a peak voltage of 48 kV. Operating conditions for the EF coils are less demanding. The OH solenoid and the inner OH and EF coils are supported by the central pedestal assembly, and the outer OH and EF coils are supported by the external support assembly frame.

Water-cooled copper initiation-trimming (IT) coils are located in the blanket close to the first wall. The IT coils provide 4 V-s in 10 ms to initiate the plasma breakdown and are also used for fine control of the plasma position.

Supplemental plasma heating is provided by 12 neutral beam injector units, each consisting of two ion sources and two beam lines, which inject 60 MW of 180 keV D⁰ beams into the plasma for ~ 5 s. The beams are aimed tangentially into the plasma in a symmetrical pattern of clockwise and counter-clockwise injection. The injection system has a power efficiency of 34% or better. Provision is also made for launching 25 MW of rf power into the plasma through four ports.

The toroidal vacuum chamber is evacuated to 10⁻⁵ Torr in < 15 s after a burn pulse through 32 0.95 m diameter ports, each of which is pumped by a 25,000 l/s cryosorption pump. The neutral beam injector vacuum conditions are maintained by 100 m² of cryosorption panels per injector unit. Two sets of pumps and panels must be provided to allow regeneration without shutdown.

Gigajoules of energy must be efficiently stored and transferred in the plasma driving (OH/EF) and heating systems. A superconducting ring dipole inductor is used as a central energy storage inductor (ESI) to store 3.2 GJ of energy. Energy is transferred between the ESI and the EF coil by a three-phase inductor-convertor (I-C) SCR bridge, and between the ESI and the neutral beam

injection system by a transfer network in which an I-C drives a high-frequency polyphase rectifier. A separate inertial energy storage unit capable of storing 1200 MJ, consisting of radially-stacked, drum-type homopolar generators, is used for the OH system, with energy losses being made up from the central ESI. An average power of 21 MW from the power grid to the ESI is required to make up for energy losses in the plasma driving and heating systems.

The EPR will consume 16 kg of tritium annually and will have a plant inventory of 1.5 kg. The fuel cycle system removes debris and nonmetallic elements from the vacuum chamber exhaust, isotopically enriches the tritium by cryogenic distillation, then stores and delivers the processed tritium to the vacuum chamber.

The reactor complex consists of 11 major facilities covering 65,000 m². The reactor containment building is 73.2 m in diameter and 50 m high, constructed of 1.5 m thick reinforced concrete with a 1 cm thick steel liner forming a barrier against tritium escape.

The total direct cost of the EPR is estimated at \$579 M. Adding 25% each for engineering and contingency raises the total cost to \$868 M.

B. Plasma Physics and Performance Analysis

1. MHD Equilibria and Steady-State Plasma Performance

Steady-state plasma performance parameters were obtained from a consistent solution of the MHD equilibrium equations and the plasma particle and power balance equations. MHD equilibria were obtained for different pressure profiles and degrees of diamagnetism/paramagnetism. These equilibria determine allowable values of plasma current, the safety factor, q , and the plasma-to-magnetic pressure ratios, β_p and β_t . Characteristics of MHD equilibria corresponding to a peak field at the TF coils of 10 T and that satisfy the constraint $q \geq 1$ are shown in Figure II-5 for different values of the pressure profile exponent, α^* . The solid curves are loci of equilibria varying (from left to right) from highly diamagnetic to highly paramagnetic plasmas. Current reversal occurs for solutions to the left of the dashed line. The maximum value of β_t , hence the maximum power density, occurs for equilibria

* $p(r) \sim p_0 [1 - (r/a)^2]^\alpha$.

Figure II-5. MHD Equilibria at $B_{\text{max}}^{\text{TFC}} = 10$ T -- Total Beta

slightly less diamagnetic than those for which current reversal occurs. Thus, the dashed line represents a locus of "optimal" solutions -- the value of β_p along this locus is confined to a rather narrow range of $1.8 \geq \beta_p \geq 1.6$. The safety factor evaluated at the plasma surface, $q(a)$, increases with the degree of peaking in the pressure profile (i.e. with increasing α). The reference design point was chosen by selecting the pressure profile whose optimum solution has $q(a) = 3.0$, a value below which confinement has experimentally been found to deteriorate.

Steady-state plasma parameters corresponding to the reference design MHD solution of Figure II-5, and a similar solution for $B_{\max}^{\text{TFC}} = 8 \text{ T}$, are given in Table II-2. A wide range of performance parameters is possible, corresponding to the range of MHD equilibria depicted in Figure II-5, and to the range of operating temperatures and values of energy confinement that may be obtained. Extensive analyses were performed to insure that the parameters shown in Table II-2 are representative of the range of plasma conditions that may be obtained in EPR. With supplemental beam heating, the power output is not jeopardized by the possibility of subignition energy confinement.

2. Transient Performance

Requirements on the plasma driving and heating systems and the power performance characteristics are determined from burn cycle dynamics simulations of the plasma, the ohmic-heating (OH) and equilibrium-field (EF) coil systems, and the neutral-beam injection system. The basic burn cycle is depicted schematically in Figure II-6, where the times correspond to the reference burn cycle. The plasma conditions of the reference case at $B_{\max}^{\text{TFC}} = 10 \text{ T}$ (see Table II-2) are approximated during the burn (flat-top) phase, and the dynamics calculations are constrained by the limit $\beta_p \leq \beta_p^{\max} = 1.71$.

A variety of startup procedures were simulated in order to determine a compromise among several conflicting economical and technological limitations. The critical parameters that, to some extent, can be traded-off against each other are: (1) energy transfer from the homopolar Oh supply (U_{OH}); (2) peak power required from the EF supply (P_{EF}); (3) total energy drawn from the energy storage unit for beam heating (U_{BE}); (4) maximum rate of change of the field in the OH coil ($\dot{B}_{\text{OH}}$); and (5) maximum field in the OH coil (B_{OH}). Initiation of beam heating midway through the OH current reversal, thereby

Table II-2. Steady-State Plasma Parameters — Reference Design

Poloidal beta, β_p	1.7	
Total beta, β_t	0.048	
Safety factor		
Magnetic axis, $q(0)$	1.00	
Plasma surface, $q(a)$	3.05	
Plasma radius, a (m)	2.1	
Aspect ratio, A	2.98	
Average temperature $\bar{T}$ (keV)	10	
Effective ion charge, Z_{eff}	1.3	
Confinement for ignition, $n\tau_E$ (s/m ³)	2.4×10^{20}	
Peak field at TF coils, $B_{\text{max}}^{\text{TFC}}$ (T)	10.0	8.0
Field at centerline, B_{t0} (T)	4.47	3.58
Plasma current, I_p (MA)	7.58	6.06
Average D-T ion density, $\bar{n}_{\text{DT}}$ (m ⁻³)	9.4×10^{19}	6.0×10^{19}
Power output, P_T (MW)	638	261
Neutron wall load, P_w (MW/m ²)	0.86	0.35
Ratio of $n\tau_E$ required for ignition to TIM value of $n\tau_E$, α_{TIM}	1.0	4.0

Figure II-6. Burn Cycle Scenario

reducing resistive losses during startup, was found to be beneficial. The time of the OH current reversal, Δt_{OH} , is an important factor in determining the requirements of the plasma driving and heating systems, as indicated in Figure II-7. On the basis of these results, $\Delta t_{OH} = 2$ s was chosen for the reference case. The effects of varying beam power and of uncertainties in plasma resistivity and oxygen impurity concentration were analyzed in a similar manner. As a result of these extensive studies, the maximum requirements shown in Table II-3 were identified.

The reference burn cycle was analyzed in detail. This cycle was initiated by the OH current reversal in the interval $0 \leq t \leq 2$ s. Neutral beam power, $P_B = 60$ MW, was injected in the interval $1 \leq t \leq 5.77$ s. The burn pulse terminated due to the accumulation of He and wall-sputtered Be impurities at $t = 55$ s. Various power components in the plasma are shown in Figure II-8; P_N is the neutron power, P_R is the radiation (line, recombination, bremsstrahlung and cyclotron) power, and P_{TR} is the power conducted or convected to the wall.

An energy balance on the entire system is given in Table II-4, and the net energy flow is shown schematically in Figure II-9. A total of 16.34 GJ of energy is incident on the first wall; of this, 15.96 GJ is produced by fusion and 0.38 GJ results from beam and ohmic heating of the plasma. An additional 0.45 GJ of thermal energy is recovered from the beam injection system, so that 16.79 GJ of thermal energy is available for conversion to electricity. With a conversion efficiency $\eta_T = 30\%$, this would result in 5.04 GJ of electrical energy. Deducting the 2.95 GJ of electrical energy required to run the plant -- 1.51 GJ to run the auxiliary systems and 1.44 GJ to make up deficits in the energy storage system caused by energy losses in the injection (0.98 GJ) and OH/EF (0.46 GJ) systems -- results in 2.09 GJ net electrical energy. Averaging this over a 70 s operating cycle (a 55 s burn plus a 15 s replenishment period) results in a net electrical power of 29.9 MW. The EPR may well operate without a thermal conversion system, in which case 2.95 GJ per cycle, or a continuous power of 42.1 MW, is required from the electric power grid.

Burn cycles shorter than 55 s can be achieved either by injection of a high-Z gas to radiatively cool the plasma or by termination of refueling.

Figure II-7. Effect of OH Current Reversal Time on Plasma Driving and Heating System Requirements

Table II-3. Plasma Driving and Heating System Maximum Requirements

Ohmic Heating Coil System	
Volt-seconds to plasma	85
Peak field, B_{OH} (T)	5.0
Maximum field rise, $\dot{B}_{OH}$ (T/s)	6.7
Maximum voltage, V_{OH} (kV)	51
Maximum current, I_{OH} (kA)	80
Maximum power required, P_{OH} (MW)	1900
Maximum energy transferred, U_{OH} (MJ)	1200
Minimum current reversal time, Δt_{OH} (s)	2
Equilibrium Field Coil System	
Volt-seconds to plasma	50
Maximum voltage, V_{EF} (kV)	21
Maximum current, I_{EF} (kA)	80
Maximum power required, P_{EF} (MW)	420
Maximum energy transferred, U_{EF} (MJ)	1500
Neutral Beam Injection System	
Deuteron energy (keV)	180
Power to plasma, P_B (MW)	60
Energy to plasma, U_B (MJ)	300
Energy from energy storage, U_{BE} (MJ)	1000

Figure II-8. Power Components During Reference Burn Cycle

Table II-4. Energy Balance — Reference Case Burn Cycle

Thermonuclear energy produced		15.96 GJ
Energy input to plasma		0.38 GJ
Beam injection	0.29 GJ	
Ohmic heating	0.09 GJ	
Energy to first wall		16.34 GJ
Neutrons	78%	
Transport	13%	
Radiation	9%	
Thermal energy from injectors		0.45 GJ
Electrical energy produced ($\eta_T = 30\%$)		5.04 GJ
Electrical energy required		2.95 GJ
Makeup to energy storage	1.44 GJ	
Auxiliaries	1.51 GJ	
Net electrical energy		2.09 GJ
Waste heat to environment		13.86 GJ
Auxiliaries	1.51 GJ	
OH/EF system	0.37 GJ	
NB direct conversion	0.23 GJ	
Thermal conversion	11.75 GJ	
Waste heat rejected to environment, average over operating cycle		198 MW
Electrical power required to operate EPR		42.1 MW
Gross electrical power		
Averaged over burn cycle, $\Delta t = 55$ s		91.6 MW
Averaged over operating cycle, $\Delta t = 70$ s		72.0 MW
Net electrical power		
Averaged over burn cycle, $\Delta t = 55$ s		38.0 MW
Averaged over operating cycle, $\Delta t = 70$ s		29.9 MW

Figure II-9. Net Energy Flow in Reference Burn Cycle (with thermal energy conversion).

Longer burn cycles can be achieved by using supplemental beam heating to maintain thermonuclear temperatures, the required beam power increasing with time to offset the accumulation of He and wall-sputtered Be. It is assumed that the plasma density can be maintained by a combination of recycling from the wall and refueling, but limited operation without refueling appears feasible. The power performance with only recycling, with recycling and refueling (reference case) and with supplemental beam heating, is shown in Figure II-10 as a function of the burn cycle length. The required supplemental beam power increases with time to a maximum of 35 MW for a 95 s burn pulse. An increase of as much as 30% in net electrical power, relative to the reference case, can be achieved by using supplemental beam heating to extend the burn.

3. Plasma Initiation

The time development of the plasma at the initiation of the discharge has been studied. A small plasma ($a = 0.35$ m) will be created at the center of the chamber ($R = 6.25$ m) by a toroidal electric field. Certain features of the initiation are illustrated in Figure II-11. The driving voltage, V_{LOOP} , produced by the changing flux in a special startup coil rises in about 2 ms to 500 V, and holds for ~ 6 ms. In the first 1.2 ms, the electron avalanche converts essentially all the neutral gas in the chamber to plasma. As soon as the electron density is sufficiently large, the plasma current starts to rise and ohmic heating occurs. When the plasma heats up, the resistance drops and it becomes possible to maintain the plasma current with a reduced voltage. A discharge with a plasma current of 115 kA and an electron temperature of ~ 130 eV can be maintained with a voltage of 28 V.

The voltage induced by the sinusoidal OH current reversal should reach 25 to 30 V at about 0.3 s; thus, the special startup coil should be pulsed about 0.3 s after the initiation of the OH current reversal, forming the plasma at this time. Creating the plasma at 0.3 s will waste only about 4 V-s of the OH inductive flux. After the plasma is created and the OH and EF coils are providing the driving voltage, additional gas can be added to the chamber and the plasma radius and current can be increased simultaneously.

C. Magnet System

The EPR magnet system consists of the toroidal-field (TF) coil system and the poloidal-field coil system. These coils interact with one another

Figure II-10. Power Performance as a Function of Operating Mode

Figure II-11. Plasma Breakdown — Normal Start-up Cycle

during operation and also interact with the other systems of the EPR. The magnet system design was developed in sufficient detail to evaluate problems in the construction and operation of EPR magnets.

1. Toroidal-Field (TF) Coils

The TF coils use NbTi as the superconductor because of its good ductility and proven performance in large magnets. The toroidal field strength should be as high as practical in order to enhance the plasma confinement and boost the power performance. A peak field of 10 T can be achieved with a large amount of superconductor at 4.2°K or with much less superconductor at 3°K. It is more economical to operate the 10 T TF coil at 3°K because the difference in refrigeration cost is much smaller than the difference in the cost of the superconductor. The problem of refrigeration at temperatures lower than 4.2°K for large systems was evaluated. The TF coils will achieve 10 T peak field at 3°K and 8 T peak field at 4.2°K with about 0.5°K temperature allowance for each case; 8 T is the minimum goal and 10 T is the maximum goal in the TF coil design.

A TF coil system, consisting of 16 pure-tension "D"-shape coils with a horizontal bore of 7.78 m, provides adequate space for the vacuum chamber, blanket and shield, provides adequate access for assembly and repair, and has a satisfactorily small maximum field ripple of 1.3%. The TF coil system is summarized in Table II-5, and depicted schematically in Figure II-12.

The superimposing field on the TF coils from the poloidal coils was found to be the most troublesome factor in the TF coil design. The out-of-plane load is large and creates serious problems for the structural design as well as a threat to the coil stability from bending moments and shear stresses in the coil structure. Moreover, unacceptably large ac losses will occur in the TF coil unless either the conductor is made of cable or the coil is shielded against changing fields. Both of these possibilities were evaluated.

The sponginess of cable conductor threatens coil structure integrity. In addition, cabling the conductor does not overcome all ac losses; changing the parallel field still couples filaments in a single strand. These ac losses, proportional to the conductor length, are still unacceptably large and the conductor stability is very poor. One possible solution is periodic twist-pitch reversal, but this is as yet only a hypothesis.

Table II-5. Toroidal Field Coil System

Superconductor/Stabilizer/ Insulator/Support	Nb-Ti/Cu/ fiberglass epoxy/SS
No. of coils	16
Coil shape	Pure tension $R_{10} = 2.45 \text{ m}$ $R_{20} = 11.1 \text{ m}$
Field ripple (%)	1.3
Maximum access (m)	~ 3
Peak field (T)	10 @ 3°K 8 @ 4.2°K
Bore (m)	
Vertical	12.6
Horizontal	7.78
Field in plasma, B_z (T)	
10 T peak field	4.32
8 T peak field	3.46
Stability	Cryostatic
Temperature allowance (°K)	0.5
Conductor design	Monolithic
Cooling	Pool boiling
Operational current (kA/turn)	-
Stored energy (CJ)	
Total	30
Coil	1.875
Inductance (H)	
Total	16.7
Coil	1.04
Ampere-turns (MAT)	
Total	134
Coil	8.37
Turns/coil	70 $\times$ 2
Mean turn length (m)	36
Total conductor length/coil (A-m)	151.2 $\times 10^3$
Coil weight/coil (Ton)	~ 208
Coil and bobbin cross section (m ²)	0.619
Bobbin	
Material	316 SS
Thickness (cm)	1.25
Winding cross section (m ²)	0.572
Average current density (A/cm ²)	
Over bobbin and coil	1352
Over coil winding	1463
Over copper	3660
Smallest radius of curvature at $\sim 10 \text{ T}$ field region (m)	1.98
Average hoop force/turn (lb)	133 $\times 10^3$
Average turn cross section (cm ²)	40.6
Cross section ratio, SS/Cu	~ 1.5
Overall average stress (psi)	
Stainless steel	26,000
Copper	14,500
Length of straight segment (m)	8.56
Compressive pressure (psi)	7660
Circumferential stress (psi)	< 74,000
Refrigeration power (MW)	14.3

Figure II-12. EPR Magnet Schematics

For the EPR, the option of a field shield was chosen. It was found that an aluminum shield operating at a mean temperature of 18°K is a reliable means of overcoming the varying field superimposed by the poloidal-field coils and plasma. Detail analyses of ac loss in field shields, the out-of-plane load and dc field soaking were completed. A summary of the heat dissipation in the TF coils is shown in Table II-6. Superconducting shields were also considered, but were not adopted because their use would require considerable development work.

With the coils shielded, a monolithic conductor can be used. A conductor 0.5 cm thick and 30 to 50 cm wide has been chosen as the reference conductor. It will carry a current of 60 kA. Each TF coil will consist of two jelly-rolled windings side by side with a central rib between them, as shown in Figure II-13. The combination of the central rib and the flat conductor with one turn per layer makes a strong structure with good transfer for hoop stress and centering force, good mechanical stability, good rigidity against out-of-plane load and assurance of cryogenic stability for the TF coils.

Coil protection is also facilitated by this conductor design with a high current and relatively few turns. During a rapid (8-1/2 minute) discharge, the voltage between neighboring turns will be low; and even the maximum voltage with respect to ground can be limited to 2,000 V.

The TF coils must be operated in series at all times; otherwise unequal currents in different coils could create large forces and torques between coils and large bending moments in each TF coil, even if the TF coil is in pure-tension shape. These would produce catastrophic damage to the TF coil system.

The TF coil geometry is of a pure-tension shape. Because the stresses with the pure-tension shape are so dramatically lower than those with other shapes such as a circle, an oval or the simple D, it was feared that a small deviation from the correct shape, as might occur during construction, might introduce large stresses, forces and torques. To study that possibility, the effects of five kinds of fabrication errors were calculated. Distortions of 5 cm from the ideal shape or displacement of 2 cm vertically or radially introduces nonuniform hoop tension. Of the angular deviations considered, a

Table II-6. TF Coil Heat Dissipation Summary

Heat Dissipation Items	With Aluminum Field Shield (Sheet Conductor)		Without Field Shield ^a (Cable Conductor with Twisting Reversal)	
	10 T (3°K) (kW)	8 T (4.2°K) (kW)	10 T (3°K) (kW)	8 T (4.2°K) (kW)
Nuclear heating	1.5	1.5	1.5	1.5
3°K/4.2°K aluminum shield ac losses	4.8	3.06	---	---
18°K aluminum shield ac losses	136	88	---	---
Thermal radiation load on 18°K shield	1.04	1.04	---	---
Thermal radiation load on helium vessel from 77°K shield	---	---	0.16	0.16
Cryostat heat conductor loss on helium vessel	0.16	0.16	0.16	0.16
Current lead loss	0.12	0.12	---	---
Parallel field conductor loss	---	---	4.74	3.5
Perpendicular field conductor loss	---	---	2.08	1.34
ac loss in coil form	---	---	10.1	6.5
Summation of loss at 3°K/4.2°K	6.5	4.8	18.7	13.3
Summation of loss at 18°K	137.0	89.0	---	---
Refrigeration power (compressor input power)	14.3 MW	8.9 MW	9.3 MW ^a	4.7 MW ^a

^a ac loss in conductor reinforcing structural material is not included.

Figure II-13. Reference Shett Conductor (60 KA) for TFC and its Coil Cross Section (Jelly Roll Winding)

rotation of 0.5° in the toroidal direction had the greatest effect -- an inter-coil force of about 4×10^5 kg/m is generated.

The radiation shield is designed to protect the TF coils from nuclear heating and radiation damage. The nuclear heat load is 1.5 kW, which is negligible in comparison with other heat dissipation in the TF coils, as shown in Table II-6. The copper stabilizer is designed to tolerate a radiation-induced resistivity of 1.5×10^{-8} Ω -cm. The shield is designed so that this value occurs after 2.5 MW-yr/m² integrated, first-wall neutron load, which corresponds to 10 years of operation at the nominal wall load of 0.5 MW/m² and a 50% capacity factor. The aromatic-base epoxy insulator inside the conductor receives a dose of 3.5×10^8 rads at the design lifetime integrated wall load of 2.5 MW-yr/m² -- this dose is safely below the range $10^9 - 10^{10}$ rads at which the properties of the insulator are thought to degrade, although there are few data to support this conclusion.

2. Poloidal-Field Coils

The poloidal-field coils present problems different from those of the TF coils, but equally challenging. The poloidal-field coils consist of the initiation-trimming (IT) coils, the ohmic-heating (OH) coils and the equilibrium-field (EF) coils. The OH coils and the EF coils are superconducting and are located outside the TF coils as shown in Figure II-12. The smaller IT coil system is made of water-cooled copper and is located near the first wall. The IT coils initiate plasma discharge by delivering 4 V-s in 10 ms. The field from the IT coils can also be used to trim the plasma position.

Although the OH coils serve as the transformer primary for producing the plasma current and the EF coils provide equilibrium for the plasma, both coil systems have nearly identical problems, e.g. large stored energy, high operational current, rapid charging and discharging and ring coil configuration. For this reason, the conceptual design of the eight pairs of EF coils and six pairs of OH coils were carried out together.

The design requirements for the OH and EF coil systems were specified as a result of a detailed trade-off study and are summarized in Table II-3. Burn cycle dynamic simulations of the plasma, the coupled OH and EF systems and the plasma heating systems were performed. Free-boundary plasma MHD

equilibrium calculations were utilized in the design of an equilibrium field that would produce the circular plasma.

Characteristics of the OH and EF coils are given in Table II-7. The OH coils and EF coils both have a maximum operational current of 80 kA supplied by two parallel 40 kA cables with fully transposed strands. The OH coils have 837 turns in each parallel path; the EF coils have 464. The charging voltage for the OH coils is 48 kV and the turn-to-turn voltage is about 60 V. The charging voltage for the EF coils is 21 kV and the turn-to-turn voltage is about 50 V. In the helium gas environment, the minimum turn-to-turn separation must be about 0.3 mm.

Multilayer coils would require large gaps between layers and present an awkward problem for coil design, especially for the long OH solenoids. Therefore, each coil will be wound with a single-layer conductor 15 cm wide. The 40 kA cable design is illustrated in Figure II-14.

The EF coils and OH coils will be cooled by helium pool boiling at 4.2°K, 1 atm pressure. Pool boiling is simple, inexpensive, reliable and easy to control. Above all, a rather small heat transfer flux is adequate to remove the conductor ac losses if the helium bubbles can be properly vented to avoid bubble accumulation within the winding. Under this circumstance, the heat transfer flux ceases to be an important factor in determining the coil stability; instead the coil stability depends on the conductor current density, the amount of liquid helium surrounding the conductor and the extent of coil disturbances.

The equilibrium field must penetrate the blanket and shield to act on the plasma, but the blanket and shield are about a meter thick and consist mostly of metal such as stainless steel. Eddy currents in this material would distort the equilibrium field and delay its penetration if the blanket and shield were not sufficiently segmented. For a blanket and shield design of 16 segments, each made of 43 blocks, field distortion or time delay will be reduced to an insignificant level.

3. Structural Support

Two structural support concepts, a torque shell and a torque frame, were developed.* The torque shell design uses shear webs located between the TF

* This work was performed by McDonnell-Douglas Astronautics Company-East in collaboration with ANL.

Table II-7. OH/EF Coil Magnet Characteristics

	OH Coil	EF Coil
Superconductor/stabilizer	Nb-Ti/Cu	
Coil design	Single layer	
Conductor design	Fully transposed cable	
Stability	Cryostatic	
Cooling	Pool boiling	
Operating temperature ($^{\circ}\text{K}$)	4.2	
Average current density (A/cm^2)	2640	2946
Magnetic field (T)		
In flux core	~ 5	
At plasma center		~ 0.46
Ampere-turns (MAT)	67	± 18.6
Total conductor length (MA-meters)	847	996
Maximum dB/dt in conductor (T/s)	6.7	~ 1
Stored energy in OH/EF/plasma field (MJ)	2262	
Maximum operational current (kA)	80	80
No. of turns	837	464
Self-inductance (H)	0.48	0.52
Mutual coupling	$K_{\text{OHEF}} = 0.0158$	
Power supply voltage (kV)	48	21
Volt-seconds to plasma (V-s)	85	50
Coupling coefficient to plasma ring	$K_{\text{OHP}} = -0.2422$	$K_{\text{EFP}} = -0.2566$

Figure II-14. 40 KA Cable for Poloidal Coils

coil cryostats to provide continuous support for the coil and to cancel the induced torques. This design provides the lightest weight design but requires removal of the shear web panels to permit access to the blanket and shield. The torque frame concept uses a frame at the top and bottom of the reactor to transfer the TF coil loads to the reactor building wall and floor, respectively. Both concepts provide blanket and shield access through an approximately 3 by 8 m opening between TF coils. Very little access, if any, will be available from above and below the TF coil because of required structure. Openings for vacuum ducts and instrumentation, however, can be provided through this structure. The floor area around the reactor will not be restricted by structural members with either concept, thereby providing for ease of locating components such as neutral beam injectors and for freedom of movement for maintenance equipment. The outer and upper poloidal coils can be removed using their supporting structure as a lifting fixture, and the combined weight is compatible with planned crane capacity. The lower poloidal coils are captivated by the various support columns and require an in-place repair/replacement facility, which was conceptually included in the design. Use of 7075-T6 aluminum alloy joined by bolting, results in a substantially lighter and lower cost structural support design than can be achieved using welded stainless steel. The torque shell concept has been tentatively chosen as the reference option.

D. Plasma Heating

Supplemental heating, in addition to ohmic heating, is required to heat the EPR plasma to ignition temperatures. A power input to the plasma of 60 MW is needed for about 5 s during startup, and somewhat less power may be required for periods up to a minute to maintain the burn in the face of unfavorable plasma conditions. Current experience dictates that neutral beam heating be the reference option for this supplement. Radio-frequency heating is considered as the primary backup option.

1. Neutral Beam Injection

Three neutral beam injection systems have been designed. These are summarized in Table II-8.

Beam penetration calculations indicate that a beam energy of 180 keV is required for adequate penetration. The reference design is based on modest

Table II-8. Neutral Beam Injection System Characteristics

	(Reference) Design 1	Design 2	Design 3a	Design 3b
Atomic ion	D ⁺	D ⁺	D ⁻	D ⁻
Target for D ⁺ → D ⁰	D ₂ gas	D ₂ gas	D ₂ gas	Li plasma
Beam composition (D ⁺ , D ₂ ⁺ , D ₃ ⁺ /D ⁻)	(0.75,0.18,0.07/-)	(0.95,0.03,0.2/-)	(-/0.95)	(-/0.95)
Neutral beam power (MW)	60	60	60	60
Neutral beam energy (keV)	180	180	180	180
Neutral beam current (Equiv. A)	333	333	333	333
No. of injectors	12	12	6	6
No. of ion sources/injector	2	2	2	2
Type of grid	multiaperture	multiaperture	multiaperture	multiaperture
Ion beam current density (A/cm ²)	0.135	0.175	0.135	0.135
Ion beam power (MW)	441	338	113	81
Gas load/injector (Torr-ℓ)	110	57	41	11
Direct conversion efficiency	0.85	0.85	0.85	0.85
Thermal conversion efficiency	0.30	0.30	0.30	0.30
Electrical power efficiency	0.29	0.41	0.66	0.77
Overall power efficiency	0.34	0.45	0.66	0.77
Net power input (MW)	207	145	91	78

extrapolations beyond presently achieved results with D^+ sources. The second design is based upon improved D^+ sources. The third design, which would require considerable advances in source technology, is based upon direct-extraction D^- sources, with neutralization by a gas target (3a) or by a lithium plasma (3b). All designs employ energy recovery, and the first two designs inject only the $D^+ \rightarrow D^0$ component into the plasma.

In the reference design, twelve injectors, arranged as shown in Figure II-4 to tangentially inject into the plasma in a symmetrical clockwise and counter-clockwise pattern, provide 60 MW of 180 keV deuteron beams to the plasma. (An additional 20 MW could be provided by utilizing the four ports dedicated to radio-frequency heating experiments.) Each injector has two ion sources. The beam line for each ion source includes an accelerator to increase the energy of the D^+ ions, a magnet separator and energy grid to remove molecular ions and directly convert their energy into electricity, a neutralizer to form the neutrals, and a thermal energy recovery system. A schematic of the beam line is shown in Figure II-15. The pair of beams in each injector travels ~ 4 m along a beam duct and passes through an 0.75 m diameter port in the toroidal vacuum chamber wall. An electrical power efficiency (neglecting thermal energy recovery) of 0.29 and an overall power efficiency (including thermal energy recovery) of 0.34 is achieved with the reference design. These power efficiencies decrease rapidly with beam energy because of a decrease in the neutralization efficiency in D_2 gas.

Substantial improvements in power efficiency and corresponding reductions in power requirements and gas loads could be realized if D^+ ion sources are developed with a very high atomic ion component, as indicated by Design 2 in Table II-8. Even more dramatic improvements could be realized if direct-extraction D^- sources are developed.

2. Radio-Frequency Heating

Radio-frequency (rf) wave heating is an attractive alternative to neutral beam heating from the technological point of view, since efficient power sources exist for several heating modes and the neutron penetration problems intrinsic to the neutral beam injectors can be ameliorated. However, wave heating experiments have not encountered the same degree of success as neutral beam experiments in heating plasmas.

	$R_0(m)$	$\theta (deg)$
D^+	1.5	90.0
D_2^+	2.12	73.0
D_3^+	2.6	64.9

Figure II-15. Beam line design. (R_0 is the bending radius, θ is the Bending Angle in M-1. For 180 keV D^+ , the gap field $B_0 = 575$ gauss in M-1.)

Two rf heating designs were developed, one based upon heating in the lower hybrid resonance (LHRH) and the other in the ion cyclotron resonance (ICRH). The EPR reference design has four rf heating stations supplying 25 MW to the plasma, for added heating capability and experimentation. If rf heating becomes the primary option, the EPR design can accommodate 16 rf stations by replacing the neutral beam injectors with rf systems. The characteristics of ICRH and LHRH systems, which could provide 25 MW (4 stations) and 60 MW (10 stations) of heating power to the plasma, are shown in Table II-9.

E. Energy Storage and Transfer System

The energy storage and transfer (EST) system for the EPR consists of a central energy storage inductor (ESI), rectifiers to transfer energy between the ESI and the OH, EF and neutral beam systems and a rectifier to transfer energy from the substation into the ESI. A separate inertial energy storage unit, consisting of radially-stacked homopolar generators, is incorporated in the OH system, so that the inductive energy in the OH system is essentially transferred between the OH coil and the inertial storage inductor, with the central ESI providing makeup for losses. Inductive energy is transferred between the OH/EF coils and the plasma current, with some dissipative loss in the plasma. The neutral beam energy is deposited in the plasma or dissipated in the injection system. Electrical energy is recovered directly and recirculated in the neutral beam injection system. If rf plasma heating is used instead of neutral beam injection heating, the required energy is transferred from the ESI by a rectifier. The EST system is depicted schematically in Figure II-16 and summarized in Table II-10.

A constant power of 42 MW is required to operate the EPR, half of this going to the ESI to make up losses and the other half going to auxiliary systems. This power would come directly from the electrical power grid, unless a thermal conversion system is included in the EPR design. Overall energy flows for the EPR are described in Figure II-9.

1. Ohmic Heating System

The OH coil current is reversed at the start and end of the burn cycle. The OH coil energy storage unit is designed to transfer and store the bulk of the OH coil stored energy during the reversal periods by using radially-stacked

Table II-9. RF Heating Parameters

	ICRH	LHRH
Pump frequency (MHz)		
8 T	54.0	1120
10 T	68.6	1190
Output power (MW)		
4 ports	25	25
10 ports	60	60
Transmission efficiency from source to port (%)	64	48
Pulse duration, heating (s)		
25 MW	12.9	12.9
60 MW	5.4	5.4
Duty Cycle (%)		
25 MW	17.2	17.2
60 MW	7.2	7.2
Launcher	1/4 turn loops	"Grill" waveguide 8 across x 2 high
Transmission scheme	Coaxial cables	Waveguides
High power source	Tetrode amplifier	Klystron

Table II-10. Energy Transfer and Storage Systems — Maximum RatingsOhmic Heating Systems

Drum homopolar generators	
No. of generators in series	16
No. of drums/generator	6
Total energy transfer (MJ)	1200
Peak power (MW)	1900
Peak voltage (kV)	51
Peak current (kA)	68
Equivalent capacitance (F)	0.897
Rectifier system	
Type	Inductor-converter bridge
Energy transfer (MJ)	600
Peak power (MW)	66
Peak current (kA)	80
Peak voltage (kV)	0.8

Equilibrium Field System

Type	Inductor-converter bridge
Energy transfer (MJ)	1500
Peak power (MW)	416
Peak current (kA)	80
Peak voltage (kV)	21
Peak switching frequency (Hz)	1330

Neutral Beam System^{a, b} (60 MW)

Type	SCR, DC/AC/DC at 10 kHz
Energy transfer (GJ)	1
Voltage (kV)	180
Power (MW)	207

RF System (60 MW)^b

Type	5 phase inductor-converter bridge
Voltage (kV)	
ICR	18.0
LHR	64.5
Power (MW)	
ICR	94
LHR	125

Central Energy Storage Inductor

Type	Superconductive ring dipole inductor
Energy stored (GJ)	3.2
Energy transfer (GJ)	2.4
Peak current (kA)	80
Peak power (MW)	620
Average power from 60 Hz line (MW)	21

^a Assumes electrical energy recovery in power supply.^b Neutral beam and rf are alternative options.

Figure II-16. Circuit Diagram

drum-type homopolar generators. Additional energy required to provide 5 V-s for resistive plasma losses is transferred into and out of the OH coil during the burn cycle using an SCR-type inductor-converter bridge as the major transfer mechanism between the OH coil and a central superconducting energy storage inductor. The central ESI makes up the 0.46 GJ of energy that is dissipated in the OH plasma system each burn cycle.

The design of the homopolar generators is based on radially-stacked epoxy fiberglass insulating cylinders and 17-4 stainless steel cylinders. The

insulated cylinders are rigidly supported and aligned with respect to the central axis. The conducting cylinders rotate independently on a type of air bearing designed into the insulating cylinders. The innermost and outermost cylinders are made of insulating materials, so that high voltages can be achieved by electrically connecting many generators in series. A radially-directed, azimuthally-uniform magnetic field of about 5 T is produced by NbTi superconducting coils. Brushes are located along the edges of the cylinders and connected so that current flows back and forth in the axial direction. Adjacent cylinders counter rotate. The arrangement is called the counter cyclonic generator (CCG) and is illustrated in Figure II-17.

A series connection of 16 CCG modules, each of which contains six 0.55 m long stainless steel drums of varying radii between 0.55 and 0.85 m, are able to store 1256 MJ of energy and develop a peak voltage of 60,794 V. The CCG arrangement behaves like an electrical capacitor with a minimum value of 0.68 F. The OH coil is electrically shorted with a parallel connection of mechanical switches and vacuum breakers. The mechanical switch is opened, followed as rapidly as possible by the vacuum breakers, at the start of the energy transfer cycle. The shortest energy reversal time with the present CCG design is 1.8 s, neglecting lead and stray inductances. Longer reversal times are achieved by lowering the exciting field on the CCG.

The inductor-converter network used for OH coil control during the burn cycle is similar in operation to the EF coil energy transfer system described next. An average coil voltage of 625 V is developed by the OH coil inductor-converter. Approximately 500 MJ of energy is transferred between the OH coil and a central superconducting energy storage inductor.

2. Equilibrium-Field Coil System

The energy transfer system for the EF coils must be actively controlled because the power demand varies with plasma current and temperature. The design of the power supply is based on storing and transferring the energy between the EF coils and the superconducting energy storage inductor using an inductor-converter (I-C) capacitor bridge.

The design of the EF coil power supply uses typical SCR units currently available on the market. A three-phase bridge is used with 35 mF, 10 kV capacitors in each phase. The integrated average EF coil voltage varies from 900 V

Figure II-17. Cross section of the region of insulating cylinders and conducting drums for a typical CCG.

when switching the bridge at 1330 Hz to 21,000 V when switching the bridge at 120 Hz. The I-C network design has the additional capability to increase the integrated average voltage by about 50 to 60% on demand dictated by the presently anticipated fusion cycle at the most favorable ratio in current between the EF coil and the central energy storage inductor. A total energy of about 1.5 GJ is transferred to and from the central ESI.

Each arm of the I-C bridges consists of an array of 14 SCR units in series and 76 SCR units in parallel. Each SCR unit is designed to operate at a current of 75% of the continuous current handling limit and a reverse bias voltage of two-thirds of the peak inverse rating of presently available components.

3. Neutral Beam Injection System

The design of the neutral beam injector energy transfer system is composed of two major components; a saturated time-delay transformer (STDT), which uses the saturation effects of magnetic cores to act as a current surge limiter, and a high-frequency, polyphase-controlled rectifier using SCR switches to enable rapid de-energization of the beam in periods of less than 100 μ s.

The beam heating pulse is designed to last for 5 s. Energy is added to the central ESI throughout the fusion reactor cycle. The injector power supply extracts energy directly from the ESI during the beam heating phase, so that the power grid never sees a power bump. An I-C drives a high-frequency polyphase inverter. A 10 kHz voltage is developed in a summing transformer, filtered and subsequently rectified. The output lead is connected to the injector through an STDT. A coil on the STDT is connected to a controlled time delay circuit that will trigger a crowbar and interrupting switches in the event the primary neutral beam injector protection systems fail.

4. RF Heating System

The power supplies for the ion cyclotron region and the lower hybrid region rf heating systems are designed using the I-C concept used for the EF coil. Five-phase I-C bridge networks are designed to transfer energy from the ESI to the rf conversion units. Five-phase networks are used to avoid objectionably large voltage fluctuations on the frequency conversion tubes.

A lower hybrid region heating module generates 64.5 kV and 61 A. Switching steps on the bridge vary from 250 to 830 μ s. Each phase has a 77.5 μ F, 32,250 V capacitor. Eight power supply modules are operated from the ESI. Anode voltage variations are kept to within $\pm 1\%$. Each SCR switch consists of 10 SCR units in parallel and 33 units in series.

The ICR heating module generates 18,000 V and 271 A. Each I-C phase has a 555 μ F, 9000 V capacitor. Four power supply modules are operated from the ESI. Anode voltage variations are kept to within $\pm 1\%$. Each SCR switch consists of 19 SCR units in parallel and 9 SCR units in series.

5. Central Energy Storage Inductor

In order to operate the poloidal coil, neutral beam and rf systems, it will be necessary to store energy on site so that large power pulses required to initiate and terminate the tokamak discharge do not perturb the electrical power network. A superconductive energy storage inductor was designed to handle these energy pulses. The energy storage ring uses 21.4 MW at a constant input rate and provides a peak net withdrawal of 2.4 GJ at the end of beam heating. The maximum current is 80 kA at a maximum short time-average voltage of 15 kV. The ring has a major radius of 5.7 m and a minor radius of 0.8 m. The coil will use 0.58 m³ of NbTi and is constructed of pancakes separated by mica or fiberglass-epoxy boards. Alternate pancakes are wound clockwise and counter-clockwise to facilitate layer-to-layer connections. Conductor transposition is achieved by winding top and bottom halves with 40 kA cable and operating coil halves in parallel. There are a total of 12 layers of coil.

F. Vacuum Systems

The EPR toroidal vacuum system must: (1) reduce the residual gas pressure in the toroidal chamber from $\sim 2 \times 10^{-3}$ to $\sim 1 \times 10^{-5}$ torr in ~ 15 s after the burn pulse; and (2) achieve a base pressure of less than 10^{-8} torr at the beginning of an operational period. These criteria can be satisfied by thirty-two 25,000 l/s cryosorption pumps, each connected through a 1.1 m diameter duct to an 0.95-m diameter port in the vacuum chamber wall. To minimize the tritium inventory, the cryosorption pumps will be regenerated, using zirconium/aluminum getter pumps, every four hours to remove the tritium and deuterium that has accumulated on the liquid helium cooled panels. An

additional 32 cryosorption pumps are provided to allow for continued operation during the regeneration period.

Pumping requirements for the neutral beam injectors are very demanding. Twelve injectors will each have gas loads of 110 torr-ℓ/s during operation. The required pumping speed of $\sim 5 \times 10^6$ ℓ/s will be provided by 100 m² of cryosorption panel in each injector. To avoid shutdown during regeneration, an additional 100 m² of panel area must be provided and the design must allow for isolation of that portion of the cryosorption panel to be regenerated.

The principal parameters of the toroidal and neutral beam vacuum systems are shown in Table II-11.

The high impedance characteristics of the waveguide used for lower hybrid rf heating will necessitate a separate pumping system to insure adequate vacuum along the length of the waveguide. Each waveguide must have either a mercury diffusion or turbomolecular pump with an effective pumping speed of 10,000 ℓ/s to maintain the required 1×10^{-6} Torr vacuum at the waveguide window.

Table II-11. Vacuum System Parameters

	<u>Toroidal</u>	<u>Neutral Beam</u>
Volume	754 m ³	250 m ³ /injector
Surface area	771 m ²	254 m ² /injector
Gas load	2588 Torr-ℓ	110 Torr-ℓ/s per injector
Cryosorption pumping	32 - 25000 ℓ/s pumps	100 m ² panel/injector
Effective pumping speed	4.25×10^5 ℓ/s	5×10^6 ℓ/s per injector
Secondary pumps		
Al/Zr getter pumps	32 - 10000 ℓ/s	12 - 25000 ℓ/s
No. 1300 CFM blower stations	16	Use same pumps
No. 1400 ℓ/s turbomolecular pumps	16	Use same pumps

G. First Wall

The first wall system consists of a vacuum wall and detachable coolant panels. The free-standing vacuum vessel is constructed from 16 cylindrical segments of 2-cm thick stainless steel plate and is reinforced with an external ring and spar framework. Locations of the two circumferential support rings and ten longitudinal spars on each segment are shown in Figure II-18. The 16 segments are joined by formed rings that are welded to the ends of each segment. A chemically bonded Cr_2O_3 coating is applied to the joining surfaces in two of these rings to form a current breaker in the vacuum wall. Detachable stainless steel coolant panels are roll-bonded to the inside of the vacuum wall, as illustrated in Figure II-19.

The surface of the coolant panel facing the plasma is coated with 100-200 microns of beryllium to control impurity contamination of the plasma by stainless steel. The substantial porosity (10-15%) and fine microstructure obtainable with the plasma spray-coating process facilitates gas re-emission, particularly helium, and minimizes blistering erosion.

Water is supplied to the coolant panels by manifolds located in the connecting rings that join the first-wall segments. The toroidal vacuum wall is supported by a three-point per segment, roller/slide pad-type support from the blanket to the lower rings and spars. The three-point support minimizes the size of the reinforcing ring and the roller/slide support minimizes thermal stresses by allowing for expansion of the vessel. The first wall design is summarized in Table II-12.

Extensive thermal-hydraulic, mechanical, materials performance and radiation damage analyses have been performed to evaluate the first wall performance and to determine the design limits. Results are summarized in Table II-13. The stainless steel vacuum wall should maintain its structural integrity for the 10 year design life under the nominal operating conditions, viz., integrated wall loading of 2.5 MW-yr/m^2 , maximum annual neutron fluence of $6 \times 10^{25} \text{ n/m}^2$ (2.8 dpa/year, 54 appm/year helium and 133 appm/year hydrogen) and maximum wall temperature of $\leq 500^\circ\text{C}$. For these conditions the predicted radiation swelling of $< 4\%$ is tolerable. The limiting criterion is loss of ductility caused by displacement damage and helium generation. For

Figure II-18. Vacuum Vessel Structure

Figure II-19. Coolant Panel

Table II-12. First-Wall Design Parameters

Design Description

- Free-standing, stainless steel vacuum wall with rib and spar reinforcing.
- Detachable, water-cooled stainless steel panels to shield vacuum wall from plasma.
- Low-Z coating on plasma-exposed face of coolant panel for high-Z impurity control.

Design Parameters

Vacuum chamber	
Material	316 SS
Design stress (ksi)	10
Major radius (m)	6.25
Minor radius (m)	2.4
Volume (m ³)	711
Wall area (m ²)	592
Wall thickness (cm)	2
Ring and spar (cm)	
Width	5
Depth	11
Ports	
Vacuum (0.95 m diameter)	32
Heating (0.75 m diameter)	16
Experimental (1.5 m diameter)	4
Total port area (m ²)	31
Current breaker	
Material	Cr ₂ O ₃
Form	Coating
Preparation	Chemical bond
Coolant panel	
Material	316 SS
No.	352
Area per panel (m ²)	1-2
Length (m)	1-2
Width (m)	~1
Total panel thickness (cm)	~1
Thickness front wall (cm)	0.5
Low-Z coating	
Material	Beryllium
Thickness (μm)	100-200
Preparation	Plasma spray
Coolant	H ₂ O

Table II-13. First-Wall Operating Parameters and Design Limits*

<u>Nominal Operating Conditions</u>	
Capacity factor (%)	50
Operating cycle (s)	
Startup	5
Burn	35
Shutdown	5
Exhaust and replenishment	15
Average power loading during burn (MW/m ²)	
Neutron	0.5
Radiation, conduction, convection	0.13
<u>Operating Parameters</u>	
Stainless steel vacuum wall	
Maximum temperature (°C)	<500
Minimum yield stress at 500°C (ksi)	17
Maximum annual fluence (n/m ²)	6×10^{25}
Neutron Damage (dpa/yr)	2.8
Helium generation (appm/yr)	54
Hydrogen generation (appm/yr)	133
Stainless steel coolant panel	
Maximum temperature (°C)	380
Minimum yield stress at 500°C (ksi)	17
Maximum annual fluence (n/m ²)	6×10^{25}
Neutron Damage (dpa/yr)	2.8
Helium generation (appm/yr)	54
Hydrogen generation (appm/yr)	133
Maximum heat deposition (W/cm ²)	5.8
Maximum ΔT across panel surface (°C)	20
Maximum ΔT through panel face (°C)	
With Argon shutdown	100
Without Argon shutdown	75
Maximum ΔT during burn cycle (°C)	100
Maximum thermal strain range (%)	
Operating cycle	0.14
Burn cycle	0.09
Beryllium coating	
Maximum surface temperature (°C)	407
Helium generation (appm/yr)	780
Hydrogen generation (appm/yr)	13
Maximum erosion rate (um/yr)	30
Water coolant	
Maximum pressure (psi)	2000
Velocity (m/s)	1.6
Inlet temperature — first panel (°C)	40
Exit temperature — eighth panel (°C)	310
Pumping power (MW)	<1
Vacuum wall	
Design life (yr)	10
Integrated neutron wall loading (MW-yr/m ²)	2.5
Yield strength — 10 yr (ksi)	75
Uniform elongation — 10 yr (%)	>1
Radiation swelling — 10 yr (%)	<4
Limiting criterion	Ductility
Coolant panel	
Design life (yr)	5
Total burn cycles — 5 yr	10 ⁶
Fatigue lifetime (yr)	5
Radiation lifetime (yr)	8
Limiting criterion	Thermal fatigue
Low-Z coating	
Design life (yr)	3-5
Limiting criterion	D-T sputtering

*Based on a neutron wall load of 0.5 MW/m² and a plant capacity factor of 50%.

temperatures below 500°C, the residual uniform elongation, which is estimated to be $\geq 1\%$ at the end of the 10 year life, is considered to be acceptable. The lifetime of the low-Z coating is limited by erosion caused primarily by D-T physical sputtering. A design life of 5 years for a 100 to 200- μm thick beryllium coating appears feasible. Only limited data exists with which to estimate the lifetime of the ceramic current breaker; however, bulk radiation effects will likely be the limiting criteria.

In addition to the extensive radiation damage, the coolant panel will be subjected to severe thermal cycling produced by heat deposition on the surface during the plasma burn. Temperature variations in the hottest coolant panel during operation are shown in Figure II-20. The spike is caused by the radiation when argon is injected to terminate the burn. The strain range for the burn cycle depends on the difference between the maximum and minimum values of ΔT during the cycle, and the strain range for the plant warm-up/cool-down operating cycle is a function of the average ΔT during the burn cycle. Assuming that the duration of the operating cycle is long enough that stress relief occurs, the strain range for the coolant panels with sliding supports is 0.085% for the burn cycle and 0.14% for the warm-up/cool-down operating cycle. These values correspond to fatigue design lifetimes for the coolant panels of 5×10^6 burn cycles and 1×10^5 operating cycles. Thus, thermal fatigue will limit the life of the coolant panel to 5 years, which corresponds to $\sim 10^6$ burn cycles, for the current design parameters.

Although the current first-wall system design is based to a large extent on available materials and existing technology, it appears that adequate mechanical integrity of the system can be maintained for suitable reactor lifetimes under the postulated EPR conditions.

H. Blanket/Shield System

The blanket/shield system consists of the blanket, the inner bulk shield, the vacuum duct penetration shield, and the biological shield. In order to insure penetration of the equilibrium field into the plasma region without intolerable distortion or phase delay, the blanket and bulk shield are constructed of 688 electrically insulated blocks, as illustrated in Figure II-21.

The blanket is made up of 0.28-m thick stainless steel blocks, as shown in Figure II-22. Each of the 16 segments of the vacuum chamber is covered by

Figure II-20. Transient temperature histories (0.5 MW/m^2). Locations on the coolant exit plane at the surface of the beryllium coating (upper curve), at the surface of the stainless steel in contact with the coating (middle curve), and at the stainless steel surface in contact with the coolant (lower curve).

Figure II-21. EPR Blanket/Shield Arrangement

Figure II-22. Blanket Block

17 blanket blocks. The blocks are cooled with pressurized water flowing in a network of 1-cm diameter drilled channels, with each block having an independent cooling system.

The bulk shield surrounding each of the 16 segments of the vacuum wall and blanket consists of 1 inner shield block and 25 outer shield blocks, as shown in Figure II-21. The inner shield block is 0.58-m thick and consists of alternating layers of B_4C and stainless steel disposed so as to maximize the attenuation of neutrons and gamma rays. At the top, bottom and outside of the torus, the bulk shield is 0.97-m thick and consists (going radially outward) of 0.03 m of stainless steel, 0.15 m of graphite with 1% natural boron, 0.05 m of stainless steel, 0.65 m of lead mortar and 0.09 m of aluminum. The bulk shield is cooled with H_2O at atmospheric pressure.

Neutral beam lines, vacuum ducts and other penetrations of the outer blanket and bulk shield represent large (~ 0.6 to 1.0 m^2 cross section) streaming paths for neutrons and require special shielding. A special, 0.75-m thick, annular shield surrounds the neutral beam tube after it exits from the bulk shield and extends beyond the TF coils, so that there is no unshielded line-of-sight path from the wall of the beam tube to the TF coils. The inner 0.65 m of this special shield is 50% SS/50% B_4C , followed by 0.05 m of lead and 0.05 m of aluminum.

A pneumatically operated shield plug is closed in the vacuum duct during plasma burn (see Figure II-3). This shield plug consists of two blocks. The inner block is 0.32-m thick and is fabricated of stainless steel and cooled in the same manner as a blanket block. The outer block is 0.58-m thick with a material disposition (SS/ B_4C) similar to that of the inner shield.

The blanket, shield and vacuum vessel assembly weighs over 2700 metric tons. This weight is supported from beneath the reactor on 16 individual frames. The frames can move vertically approximately two meters to facilitate replacement of the blanket and shield blocks. The load is transferred through 32 columns from the reactor foundation to the 16 frames, which in turn support the reactor shielding blocks. The blanket block layer rests on the inner portions of the shield blocks on insulated roller pads to accommodate the high temperature of the blanket and the accompanying thermal expansion. The 350 metric ton vacuum vessel rests on the inner side of the blanket.

Extensive analyses have been performed to evaluate the performance of the blanket/shield system. These analyses are based on a nominal neutron wall load of 0.5 MW/m^2 and a plant capacity factor of 50%. The neutronics effects vary significantly around the wall in the poloidal direction, and a conservative analysis is employed. Results are summarized in Tables II-14 and II-15. Radial distributions of the neutron heating rate and of the atomic displacement are shown in Figures II-23 and II-24.

The 4-cm first wall and the 28-cm blanket region receive $\sim 90\%$ of the neutron and gamma energy. For the most part, the properties and requirements of the blanket material are the same as those of the first wall. The nuclear heating varies from a maximum of 3.5 W/cm^2 to a minimum of 0.3 W/cm^3 . The radiation damage level adjacent to the first wall is $\sim 1.7 \text{ dpa/year}$ and drops by a factor of two every $\sim 7 \text{ cm}$ going through the blanket. Operating temperatures in the load bearing portions of the blanket are, like the first wall temperatures, restricted to $\leq 500^\circ\text{C}$, but may be allowed to rise above this level in non-structural components. In general, the less severe radiation environment of the blanket will mean that property changes will be less than in the first wall. After 10 years at a wall loading of 0.5 MW/m^2 and a 50% capacity factor, the swelling in the blanket adjacent to the first wall is expected to remain below 2%, the uniform elongation will drop to $\sim 3\%$, and the yield strength will increase to $\sim 75 \text{ ksi}$. As the neutron radiation is attenuated through the blanket, the swelling will be reduced to zero after a few cm, and the tensile properties will approach those of unirradiated material ($\sim 22\%$ uniform elongation and $\sim 20 \text{ ksi}$ yield strength). The effect of creep and fatigue will be less than in the first wall since the blanket is not exposed to the surface radiation from the plasma and will not undergo the large thermal cycling of the first wall. Helium production rates will still be high in the first few cm, but the temperature limit of 500°C should insure against helium embrittlement, which is observed at temperatures above 550°C .

The bulk shield will receive $\sim 7\%$ of the total radiation energy produced in the EPR. No degradation of the bulk properties of stainless steel is expected. The boron carbide located in the inner shield is a brittle material with moderate tensile strength and high compressive strength. The major

Table II-14. Summary of Blanket Design Parameters

Design basis operating life (yr)	10
Nominal power during burn (MW)	400
Design basis neutron wall loading (MW/m ²)	0.5
Plant capacity factor (%)	50
Blanket structure	
Thickness (m)	0.28
Type metal/volume fraction	316-SS/0.9
Type coolant/volume fraction	H ₂ O/≤0.05
Penetration volume fraction	
Inner blanket	~0.02
Outer blanket	~0.05
Maximum temperatures (°C)	
In support structures	500
In bulk materials	550
Nuclear parameters	
Maximum heat deposition (W/cm ³)	3.5
Maximum fluence at 2.5 MW-yr/m ² (n/m ²)	5 × 10 ²⁶
Maximum dpa at 2.5 MW-yr/m ² (dpa)	17
Maximum helium production at 2.5 MW-yr/m ² (appm)	230
Maximum hydrogen production 2.5 MW-yr/m ² (appm)	600
Mechanical parameters	
Design stress in support structure (ksi)	<10
Minimum material yield stress (ksi)	20
Ductility at 2.5 MW-yr/m ² (% uniform elongation)	>3
Swelling at 2.5 MW-yr/m ² (% of initial volume)	<2
Maximum torque from pulsed fields (ft-lb)	125,000
Coolant parameters	
Type	H ₂ O
Maximum pressure (psig)	2000
Pressure drop (psig)	<15
Maximum velocity (m/s)	2.4
Pumping power (MW)	<1
Coolant inlet temperature (°C)	40
Maximum coolant exit temperature (°C)	309
Residual activity from the first-wall/blanket/shield	
after 2 yr operation in Ci/MWt	
Immediately after shutdown	3.5 × 10 ⁶
1 yr after removal	8.0 × 10 ⁵
10 yr after removal	7.0 × 10 ⁴
100 yr after removal	60

Table II-15. Summary of Shield Design Parameters

Design basis operating life (yr)	10
Shield structure	
Thickness (m)	
Inner bulk shield	0.58
Outer bulk shield	0.97
Beam duct shield	0.75
Evacuation duct shield (movable plug)	0.90
Biological shield	1.5
Materials	
Inner shield	304-SS/B ₄ C
Outer shield	304-SS/Pb mortar/C/Al
Beam duct shield	304-SS/B ₄ C/Pb/Al
Evacuation duct shield (movable plug)	306-SS/B ₄ C
Biological shield	Concrete
Temperature (°C)	≤ 90°C
Coolant	H ₂ O
Maximum torque from pulsed fields (ft-lb)	253,000
Maximum nuclear heating in bulk shield (W/cm ³)	0.3
Fraction of fusion power deposited in shield	~ 0.07
Maximum energy current at outer surface of bulk shield (W/cm ²)	
Neutrons	~ 2 × 10 ⁻⁴
Gammas	~ 5 × 10 ⁻⁵

Figure II-23. Neutron Heating in the Water Coolant as a Function of Depth in the Blanket/Shield for Three Locations with Respect to the Neutral Beam Duct: a—at the wall of the beam duct, b—at 10 cm from the wall of the beam duct, and c—at 30 cm from the wall of the beam duct.

Figure II-24. Atomic Displacement in Stainless Steel as a Function of Depth in the Blanket/Shield for Three Locations with Respect to the Neutral Beam Duct: a-at the wall of the beam duct, b-at 10 cm from the wall of the beam duct, and c-at 30 cm from the wall of the beam duct.

effect of radiation on boron carbide is the buildup of helium from (n, α) reactions that can induce swelling and cracking if it is present in high concentrations. Neutron irradiation can also substantially reduce the thermal and electrical conductivity. The first few cm of boron carbide will produce ~ 3500 appm of helium during a 10 year lifetime, but helium production will fall off rapidly past this point. This amount of helium is not expected to induce significant swelling or cracking if a sufficient porosity exists to accommodate the gas. Helium escaping from the boron carbide must be vented to prevent buildup of gas pressure within the shield. For the conditions expected in the EPR, the graphite in the outer bulk shield will densify rather than swell. It is expected that the volume change of graphite due to irradiation can be minimized by a suitable choice of material and should not present a problem. Helium production in the first few cm of the graphite with 1% boron will reach ~ 770 appm after a 10 year lifetime. The materials lying past the first layer of boron carbide in the inner shield and the graphite in the outer shield receive a relatively small neutron fluence, and the bulk properties should not be adversely affected. The lead mortar and aluminum in the outer shield will operate at temperatures below 100°C , which is well below $\sim 150^{\circ}\text{C}$, where the lead mortar will begin to break down.

The radiation damage to and nuclear heating in the bulk shield regions in the vicinity of the beam ducts are high (see Figures II-23 and II-24) and are comparable with those for the blanket. The 20-cm region of the bulk shield surrounding each beam duct is constructed with similar materials and provided with the same degree of cooling as that of the blanket. The walls of the beam ducts present problems similar to those encountered for the first wall, and they are designed accordingly.

The radioactive inventory as a function of time for the EPR is shown in Figure II-21. The level of neutron induced activation after two years operation is 3.5×10^6 Ci/MWt and decreases by a factor of 4 one year after shutdown and more rapidly for longer times. The curies per thermal megawatt are fairly independent of neutron wall loading for the range of 0.1 to 5 MW/m².

Figure II-25. Radioactivity and Biological Hazard Potential as a Function of Time after Shutdown Following Two Years of Operation

At shutdown, the decay heat is 2.5% of operating power and only drops about 20% during the first few minutes after shutdown, which is the period of time that is very crucial to emergency cooling.

I. Access and Maintenance

During operation, the biological dose in regions external to the TF coils is about 10^6 mrem/hr, which is too high to permit access to the inside of the reactor building for any reasonable length of time. Outside the 1.5-m thick concrete building wall, the dose is about 1 mrem/hr. The biological dose in the vacuum chamber inside the first wall is 6×10^9 mrem/hr at shutdown and after one year of cooldown the dose is 1×10^9 mrem/hr. After one day of cooling, the dose is 600 mrem/hr at a position above the reactor at the location of the TF coils and 2 mrem/hr outside the TF coils. The latter result is based upon adequate shielding of penetrations and of the neutral beam injector. These calculations indicate that the dose rate is too high to permit unshielded personnel access to the reactor during operation. At best, limited access would be allowed within a few days of shutdown. Personnel exposure can be reduced by two orders of magnitude by 10 cm of lead shielding.

The general approach to maintenance for the EPR is by use of remote handling apparatus. All large components will be repaired in place, where possible. This includes the vacuum vessel and the lower EF and OH coils. Smaller components like the blanket and shield blocks will be repaired in the hot cells. Special in-vessel remotely operated equipment will be designed to repair, replace and inspect any portions of the vacuum vessel or first-wall panels that have been damaged. Support facilities for remote operations include a remotely-operated overhead crane/manipulator with a shielded personnel cab, floor-mounted snorkel type units for servicing the vertical portions of the reactor and basement-positioned apparatus for maintaining the lower components of the reactor. A full-scale, quarter section mockup of the reactor is vital to all remote operations because it will be used to program the repair apparatus and perform practice runs.

J. Tritium

The EPR tritium handling system must separate tritium and deuterium from the spent fuel and must be capable of building atmosphere cleanup in the event of a large tritium release. The key tritium facility operating parameters are given in Table II-16, and a schematic of the facility is shown in Figure II-26. The principal assumptions applied in determining these parameters are: (1) that the throughput/ burnup ratio will be approximately 50; (2) that the fuel cycle turnaround time (fuel holdup) time will be four hours or less; and (3) that the initial tritium inventory of ≤ 1.5 kg will be supplemented as needed (from an outside production facility) to match the burnup encountered during operation. The fuel cycle turnaround time is determined mainly by the regeneration cycle on the cryosorption pumping system for the toroidal plasma chamber. The present plan is to carry out this regeneration cycle on a four-hour basis.

Analysis of the cleanup requirements following a massive release of tritium (≤ 100 gm) to the reactor hall have resulted in the definition of an atmospheric detritiation procedure. The volume of the reactor hall is $\sim 2.8 \times 10^5 \text{ m}^3$ ($\sim 10^7 \text{ ft}^3$), and the assumption has been made that the normal operating level of tritium in the hall atmosphere (air) will be maintained at the recommended MPC level for workers, $5 \mu \text{ Ci/m}^3$. Provision is made to handle a release that would result in a $\leq 10^6$ increase in the tritium activity in the reactor hall atmosphere. This is based on the assumption that the escape of 100 gm of tritium represents the largest credible event that could occur in the reactor building without a simultaneous breach of the building containment boundaries. The cleanup is to be completed within two days using a catalyst system operating at room temperature. Key parameters for this system are listed in Table II-17.

Calculations have been made to estimate the rate of tritium permeation from the plasma chamber into the first-wall cooling water circuit. These calculations show that the tritium level in the first-wall cooling water ($\sim 10^3 \text{ l}$) increases to a maximum of $\sim 1 \text{ Ci/l}$ after one year. The handling practices associated with this pressurized cooling water would essentially be the same as those currently applied in the pressurized D_2O primary circuits of heavy water reactors that commonly run up to 10 Ci/l .

Table II-16. Summary of Tritium-Handling Facility Parameters

	TFC B max	
	8 T	10 T
<u>General</u>		
Power during burn (MW)	200	500
Burn cycle duty cycle (%)	75	75
Plant availability factor (%)	67	67
Tritium burnup (g/day)	26	64
Throughput/burnup ratio	50	50
Tritium delivery rate (g/hr)	60	150
Fuel cycle turnaround time (hr)	4	4
Plant inventory (kg)	0.6	1.5
Annual tritium consumption at 50% capacity factor (kg)	6.4	16
<u>Tritium Inventory Disposition</u>		
Cryosorption pumps (g)	240 ^a	600 ^a
Getter beds (g)	240 ^a	600 ^a
Distillation columns (g)	10 ^a	25 ^a
Fuel cycle hardware (g)	10 ^a	25 ^a
Storage (g)	~500 ^a	~1300 ^a
Anticipated mean inventory (g)	600	1500
<u>Fuel Cycle</u>		
Nature of fuel processing and recycle systems	{ Nonmetallic element removal Debris removal Isotopic enrichment Fuel storage Fuel delivery	
Type of mainstream enrichment	Cryogenic distillation	
No. of columns	6	
No. of equilibrators	1	

^aMaximum value at any single time.

Figure II-26. Fuel Cycle Scenario for the ANL/EPR

Table II-17. Atmospheric Cleanup System

Reactor ball volume (m ³)	$\sim 2.8 \times 10^5$
Baseline tritium level (μ Ci/m ³)	5
Clean-up time (days)	≤ 2
Maximum decontamination factor	10^6
Air circulation rate (emergency) (cfm)	$\geq 10^5$
Volume of catalyst (m ³)	20
Catalyst operating temperature (°C)	27

Potential off-site tritium exposure calculations have been made. Two events leading to off-site exposure due to the release of tritium have been considered. The first event considers 2% ($\sim 4 \times 10^5$ Ci) of a total inventory of 2 kg of tritium released at ground level as water vapor in an accident. The dose commitment for an individual at the site boundary is 23.5 rem (whole body) at 500 m and 7.4 rem (whole body) at 1000 m as compared to the 10 CFR 100 guideline for total body dose in an accident of 25 rem. The second event is the continuous daily release of 100 Ci of tritium, for which the concentration at 500 m is $\sim 5 \times 10^{-9}$ Ci/m³ and at 1000 m is $\sim 2 \times 10^{-9}$ Ci/m³. ERDA Manual 0524 gives the uncontrolled concentrations guide as 2×10^{-7} Ci/m³.

K. Facility Description

The reactor complex consists of eleven major facilities covering an area of 65,000 square meters. The focal point is the reactor containment building (Figure II-27), a structure 73.2 m in diameter by 50 m high made of reinforced concrete, 1.5 m thick, to meet both structural and biological requirements. A thin steel membrane, 1 cm thick, lines the inside walls of the building, forming a barrier in the event of a tritium release. The seal is carried through all penetrations and access ways. A reactor pedestal is provided slightly offset from the center of the building for convenience in positioning the 300 ton overhead polar crane used in assembly and maintenance of the reactor. The building is equipped for remote maintenance of the reactor.

Figure II-27. Reactor Containment Building

A second containment building, ~ 26 m diameter and 24 m high with a two-story laboratory wing, is provided for tritium storage and fuel reprocessing. This building can be sealed in the event of accidental tritium release. A mockup and maintenance building, 38 m wide, 85 m long and 24 m high with a full coverage 230 ton bridge crane, is located adjacent to the reactor containment and tritium facility. This building contains two hot cells for reactor component repair and experimental services. Non-functioning neutral beam injectors are transferred directly through an access door from the reactor building. An area for magnet coil winding and assembly along with a full-size quarter-section mockup of the reactor is provided. An experimental apparatus setup area is available, as well as storage facilities located in the lower area of this building. A power supply facility (70 m by 32 m) is located adjacent to the reactor building, housing the bulk of the magnet and plasma heating power supply equipment. Directly connected to this facility, but located below grade some 70 m in distance, is a cryogenically-cooled energy storage facility, 15 m in diameter by 3 m deep.

A control center consisting of the control room, computer center, laboratories and offices is housed in a four-story building 50 m by 62 m. Other structures include the reactor ventilation building, the cooling towers and the power supply substation.

Heat transport and cooling tower facilities capable of removing up to 550 MW of reactor and component waste heat are provided. These consist of a heat exchange building, a pump house with demineralizer equipment and six cooling towers mounted in two banks. Cryogenic equipment, dominated by the TF coil system requirements, are located adjacent to the reactor in a building having ~ 200 m² of floor space. This facility is complete with a liquid He storage vessel, He gasometer, compressor area and a set of high pressure helium storage vessels.

A building 75 m by 48 m capable of housing a 100 MWe turbine generator system is included in the site plan. The size and scheduling of this unit is tentative but is included in the initial design. A power substation rated at 150 MW maximum, 132 kV power feeds the facility. Emergency power of ~ 2 MW will also be provided in strategic areas by diesel driven MG sets.

L. Costs and Schedule

The estimated total direct capital cost for the EPR is \$579 M. Adding 25% for engineering and 25% for contingency brings the grand total to \$868 M. A cost breakdown is given in Table II-18.

A detailed design and construction schedule has been developed. Eight years are required from the initiation of preliminary design to initial operation, and six years are required from the initiation of detailed Title II engineering design to initial operation of EPR. This schedule is based on two-shift/5-days-a-week operation.

Table II-18. Plant Capital Investment Direct Cost Estimate

	<u>\$M*</u>
Structures and Site Facilities	67.3
Reactor	248.6
Reactor Plant Facilities	245.0
Auxiliaries	<u>18.0</u>
Total	578.9
Engineering (25%)	144.7
Contingency	<u>144.7</u>
Grand Total	868.3

* FY 1976 dollars.

CHAPTER III

PLASMA PHYSICS & PERFORMANCE ANALYSIS

The physics basis of the EPR design is the experimental and theoretical work done to date on the tokamak confinement concept. There is a substantial literature on the subject, and several useful reviews (Refs. 1-3) have been published.

In this chapter, a range of MHD equilibrium states is established for the EPR plasma, and a corresponding envelope of steady-state performance parameters is developed. A reference case is selected for detailed burn cycle dynamics simulations, which determine the requirements upon the plasma driving and heating systems, the reactor power performance, the plasma breakdown requirements and the plasma fueling requirements.

A. MHD Equilibria and Steady-State Plasma Performance

The steady-state (flat-top) plasma performance parameters for the EPR are obtained from a consistent solution of the MHD equilibrium equations and the plasma particle and power balance equations. MHD equilibria are obtained for different pressure profiles and degrees of diamagnetism/paramagnetism. The equilibria are characterized by values of the plasma current, I_p , the safety factor, q , the total plasma-to-magnetic pressure ratio, β_t , and the poloidal plasma-to-magnetic pressure ratio, β_p . Plasma temperatures and the energy confinement required for a power balance are determined by solving particle and power balance equations for the various components of the plasma, employing spatial profiles which are consistent with those of the MHD equilibrium. Recycling from the wall and refueling are assumed to maintain a constant density. The results of the MHD equilibrium calculation and of the plasma particle and power balance calculation are combined to obtain the thermonuclear power and related quantities. The details of the calculational models are described in Appendix C.

1. MHD Equilibria

The MHD equilibria, which are taken to be axisymmetric, are found by writing the ideal equilibrium equation, $\nabla p = \vec{J} \times \vec{B}$, in terms of the flux function, ψ , which is defined so that the poloidal field is given by:

$$\vec{B}_p = \frac{\hat{\phi}}{2\pi R} \times \nabla \psi \quad (\text{III-1})$$

where $\hat{\phi}$ is the unit vector in the azimuthal direction. It can be shown that the magnetic field lines are tangent to the surfaces $\psi = \text{const.}$ and that the pressure, p , and the quantity, $F \equiv RB_t$, are functions of ψ only, where R is the major radius and B_t is the toroidal field. Thus, p and F are also constant on the flux surfaces. The equilibrium equation is usually solved by specifying $p(\psi)$ and $F(\psi)$ and solving for $\psi(\vec{r})$. This procedure is treated in more detail, and definitions of the various quantities are given, in Appendix C.

The parameterization of p and F that is used in this report is:

$$p(\psi) = p_0 \hat{\psi}^\alpha \quad (\text{III-2})$$

$$F^2(\psi) = F_0^2 (1 - \delta \hat{\psi}^\beta) \quad (\text{III-3})$$

$$\hat{\psi} = \left[\frac{\psi_\ell - \psi}{\psi_\ell - \psi_m} \right] = \begin{cases} 0 & \text{at limiter} \\ 1 & \text{at magnetic axis} \end{cases} \quad (\text{III-4})$$

where ψ_ℓ is the value of ψ at the edge (limiter), and ψ_m is the value of ψ at the magnetic axis. The parameters α and β characterize the profiles in terms of the function $\hat{\psi}$, and δ is a parameter that characterizes the diamagnetic/paramagnetic effect of the plasma. (The plasma is paramagnetic if $\delta < 0$ and diamagnetic if $\delta > 0$.) Outside the plasma $p = 0$, and $F = F_0 = R_{\text{magnet}} \times B_{\text{max}}^{\text{TFC}} = \text{const.}$, where R_{magnet} is the position of the superconductor on the inside leg of the TF coil, and $B_{\text{max}}^{\text{TFC}}$ is the field at the superconductor. Only equilibria with $\alpha = \beta$ are considered here. In the usual large aspect ratio approximation, which is valid near the magnetic axis, $\psi \propto r^2$. The profiles are then approximately:

$$\hat{\psi}^\alpha \sim \left(1 - \frac{r^2}{a^2} \right)^\alpha, \quad (\text{III-5})$$

where a is the plasma radius. These shapes are shown in Figure III-1 for a variety of values of α .

As shown in Appendix C, if ψ is a solution corresponding to a given $p(\psi)$ and $F(\psi)$, then $\lambda\psi$ is also a solution, provided:

$$\begin{aligned} p_0 &\rightarrow \lambda^2 p_0 \\ \delta &\rightarrow \lambda^2 \delta \\ F_0 &\rightarrow F_0 \\ \alpha &\rightarrow \alpha \\ \beta &\rightarrow \beta, \end{aligned} \quad (\text{III-6})$$

Figure III-1. Radial Profiles

where λ is an arbitrary positive constant. The associated quantities which characterize the solution, ψ , scale as:

$$\begin{aligned}\beta_t &\rightarrow \lambda^2 \beta_t \\ I_p &\rightarrow \lambda I_p \\ \beta_p &\rightarrow \beta_p \\ q &\rightarrow \lambda^{-1} q.\end{aligned}\tag{III-7}$$

Thus, for a given profile shape ($\alpha = \beta$) one would like to take λ as large as possible in order to get the highest β_t . Tokamaks are unstable, however, if $q < 1$, so the largest allowable λ is the one that makes $q(0) = 1$. (For $\alpha = \beta > 0$, $q \geq q(0)$.) In this section λ is chosen so that $q(0) = 1$ for all the equilibria considered. (It is also possible to scale the solution with F_0 , but the highest β_t is the one for the highest F_0 , so that F_0 will be taken to be $R_{\text{magnet}} B_{\text{max}}^{\text{TFC}}$.)

Figures III-2 to III-4 show the parameters (β_t , β_p , $q(\psi)$ and I_p) which characterize the equilibria that may be obtained for various values of the profile exponent ($\alpha = \beta$) and diamagnetic/paramagnetic (δ) parameters in a circular tokamak plasma with dimensions ($R_0 = 6.25$ m, $a = 2.1$ m, $R_{\text{magnet}} = 2.79$ m) and maximum toroidal field strength ($B_{\text{max}}^{\text{TFC}} = 10$ T) corresponding to the ANL EPR design. For each value of the profile, there exists a range of equilibria corresponding to different values of the diamagnetic/paramagnetic parameter, δ . For a fixed profile, as the parameter δ is increased (which is equivalent to increasing β_p), the plasma current distribution becomes increasingly pushed to the outside of the torus, with current reversal eventually occurring on the inside of the torus. The dashed line in Figures III-2 to III-4 defines the locus of points at which current reversal occurs.

For a given profile, the maximum value of β_t -- hence, the maximum thermonuclear power -- occurs for a value of the parameter δ that is nearly the same as the value for which current reversal occurs. Thus, for a given profile, the equilibrium corresponding to the value of δ for which current reversal occurs is designated as the "optimum" solution and is shown by the dashed line in Figures III-2 to III-4. It is interesting that the value of β_p^*

* In this report β_p is defined as in Appendix C. Other definitions, although they agree at lower values of β_p , do not agree at high β_p . In particular, the definition used by Callen and Dory⁴ is always higher for the same equilibrium. For example the Shafranov limit⁵ for the definition used here is $\beta_p \leq 1/2 A$, but for the Callen and Dory definition is $\beta_p^{\text{CD}} \leq \infty$.

Figure III-2. MHD Equilibria at $B_{\max}^{\text{TFC}} = 10$ T -- Total Beta.

9-III

Figure III-3. MHD Equilibria at $B_{\max}^{\text{TFC}} = 10$ T — Poloidal Beta.

Figure III-4. MHD Equilibria at $B_{\text{max}}^{\text{TFC}} = 10 \text{ T}$ — Safety Factor.

corresponding to this optimum solution is confined to the rather narrow range $1.8 \geq \beta_p \geq 1.6$ for the profile range $1 \geq \alpha = \beta \geq 2$, although the corresponding variation in β_t is quite large. This value of β_p is approximately the Shafranov⁵ limit $\beta_p \leq A/2 = 1.5$, although the separatrix is not especially close to the plasma, as can be seen in Figure IV-3. The optimum equilibrium not only has the largest value of β_t , for a given profile, but also has the largest value of $q(\psi_\ell)$ and the smallest value of I_p which can be obtained with that profile without current reversal in any part of the toroidal cross section of the plasma. Since a higher $q(\psi_\ell)$ is favorable for stability and a lower I_p implies less demanding requirements on the plasma driving system, the dashed lines in Figures III-2 to III-4 represent optimal solution loci in these two respects as well.

It is apparent from Figure III-2 that broader profiles are associated with higher values of β_t and I_p and lower values of $q(\psi_\ell)$. The range $1 \leq \alpha = \beta \leq 2$ is thought to include the equilibria relevant to an EPR plasma. The reference design profile was chosen by selecting the profile whose optimum solution has $q(\psi_\ell) = 3.0$, a value below which confinement has experimentally been found to deteriorate.^{2,6} From Figure III-4, this is seen to correspond to $\alpha = \beta \approx 1.3$.

The poloidal magnetic fields and the toroidal current density are shown in Figure III-5. The flux lines for the poloidal field are calculated by constraining the plasma boundary to be a circle of radius 2.1 m. The field due to the plasma current loop alone is calculated, and the remaining part is the required external field. Alternatively, one could specify the placement of the external coils and their currents, along with the plasma current, and let the plasma boundary be determined. It is found that if the external coils are such as to give a nearly circular plasma edge, then the MHD equilibrium parameters obtained in the two ways are the same. The parameters in this section were calculated by the fixed boundary method, and a combination of the two methods was used, as described in Chapter IV, to determine the location of the coils and currents that would give this equilibrium.

The field lines are shown separated by equal amounts of flux, ψ . The same lines are also pressure, $F = RB_t$, and safety factor contours, although not separated by equal p , F or q intervals. Figure III-6 shows the

Figure III-5. MHD Equilibria at $B_{\max}^{\text{TFC}} = 10$ T — Flux and Current Contours

Figure III-6. MHD Equilibria at $B_{\text{max}}^{\text{TFC}} = 10 \text{ T}$ — Profiles.

flux function, pressure, and current density profiles along the mid-plane, and q as a function of ψ . The pressure in this high beta equilibrium is peaked substantially toward the outside, with the current density being peaked even more to the outside.

2. Steady-State Performance

Using the parameters ($q(\psi_\ell)$, β_t , β_p , I_p) of this reference design MHD equilibrium solution, the performance parameters of the reference design were calculated with the global plasma particle and power balance code described in Appendix C, for an average electron temperature of $\bar{T}_e = 10$ keV, a value chosen on the basis of studies discussed in Reference 7. In performing the global calculations, which are based upon spatial averages over the plasma, it was necessary to decompose the pressure profile into the product of temperature and density profiles -- this was done by assuming the functional form of Equation III-5 for both and requiring that $\alpha_n + \alpha_T = \alpha = 1.3$. A relatively broad density profile ($\alpha_n = 0.3$) was chosen to reflect the effect of recycling, and a parabolic ($\alpha_T = 1.0$) temperature profile was chosen. An impurity concentration of 0.5% background ^{16}O and 5.0% wall-sputtered ^9Be was chosen based on burn cycle dynamics calculations that will be discussed in a subsequent section. The D-T ion density, hence the thermonuclear power, is limited by the value of β_t which is determined by the MHD solution -- these relationships are discussed in Appendix C. The performance parameters for the reference design are summarized in Table III-1, for two values of the maximum toroidal field, $B_{\text{max}}^{\text{TFC}}$. Operating at $B_{\text{max}}^{\text{TFC}} = 10$ T, the upper limit for NbTi superconductor, and $\beta_t = 0.048$, the reference design produces 638 MWt during the flattop portion of the burn pulse and will be an ignition device if energy confinement is as good as predicted by trapped-ion-mode (TIM) theory. At $B_{\text{max}}^{\text{TFC}} = 8$ T, the reference design produces 261 MWt and will be an ignition device if energy confinement is about 4 times the TIM prediction.

The reference design point serves to provide a set of nominal parameters which characterize the plasma performance of the EPR. However, as is apparent from Figures III-2 to III-4, there is a rather wide range of MHD equilibria which may obtain, depending upon the pressure profile ($\alpha = \beta$) and

Table III-1. Plasma Performance Design Summary
Steady-State Conditions

Beta poloidal, β_p	1.7	
Beta toroidal, β_t	0.048	
Safety factor, $q(0)$	1.00	
Safety factor, $q(\psi_i)$	3.05	
Plasma center, R_0 (m)	6.25	
Magnetic axis, R_m (m)	6.86	
Minor radius, a (m)	2.1	
Aspect ratio, A	2.98	
Volume, V_p (m ³)	544	
Profile exponents		
Pressure, $\alpha = \beta$	1.3	
Density, α_n	0.3	
Temperature, α_T	1.0	
Diamagnetic/paramagnetic parameter, δ	0.059	
Electron temperature, T_e (keV)	10.0	
Ion temperature, T_{DT} (keV)	9.6	
Confinement required for ignition, nr_E (s/m ²)	2.4×10^{20}	
Impurity (%)		
^{16}O	0.5	
^{20}Ne	5.0	
Effective charge, Z_{eff}	1.3	
Peak field at TF coils, B_{max}^{TFC} (T)	10.0	8.0
Vacuum field at centerline, B_{t0} (T)	4.47	3.58
Vacuum E , E_0 (T)	27.9	22.3
Plasma current, I_p (MA)	7.58	6.06
Plasma pressure (Nt/m ²)		
Peak, p_0	10.4×10^5	6.6×10^5
Average, $\bar{p}$	3.8×10^5	2.4×10^5
Flux function (Wb)		
Magnetic axis, ψ_m	0.0	0.0
Limiter, ψ_l	39.5	31.6
Density (m ⁻³)		
D-T, $\bar{n}_{DT}$	9.4×10^{19}	6.0×10^{19}
Electron, $\bar{n}_e$	1.0×10^{20}	6.5×10^{19}
Alpha, $\bar{n}_\alpha$	1.1×10^{18}	6.9×10^{17}
Thermonuclear power, P_T (MWE)	638	261
Neutron wall load, P_n (MW/m ²)	0.86	0.35
Ratio of nr required for ignition to nr predicted by ELM, α_{ELM}	1.0	4.0

the diamagnetic/paramagnetic parameter, δ . Furthermore, for each MHD equilibrium solution, a range of plasma conditions is possible. Thus, it is necessary to consider the range of MHD and plasma power balance solutions which define a performance envelope for the EPR plasma.

Variation of the diamagnetic/paramagnetic parameter, δ , for the reference profile ($\alpha = \beta = 1.3$, $\alpha_n = 0.3$, $\alpha_T = 1.0$), is examined in Table III-2. This variation corresponds to a curve of the type shown in Figures III-2 to III-4 for $\alpha = 1.3$. The peak pressure (p_o), thermonuclear power (P_T), neutron wall load (P_w), average DT density ($\bar{n}_{DT}$) and β_t all have their maximum values at the optimum MHD solution. Similarly, the ratio (α_{TIM}) of the energy confinement predicted by TIM to the value required for ignition is minimized at the optimal MHD solution. There is no incentive to operate in the questionable regime where current reversal ($J_T < 0$) occurs. Operation below the optimum, $\beta_p = 1.72$, results in lower power, a lower safety factor, $q(\psi_\ell)$, higher confinement requirements, and larger required plasma current than for the reference design point.

Variation of the profile ($\alpha = \beta$) is examined in Table III-3, where the performance parameters for the optimum MHD solution for each profile are tabulated. This variation corresponds to the loci defined by the dashed lines in Figures III-2 to III-4. For the plasma power balance calculations, a broad density profile ($\alpha_n = 0.3$) was fixed, and the temperature profile was varied as required ($\alpha_T = \alpha - \alpha_n$). The plasma performance parameters are strongly dependent upon the pressure profile. The power output (P_T) and related parameters ($\bar{n}_{DT}$, P_w , p_o , β_t) increase dramatically with a broadening pressure profile. The value of $n\tau_E$ required for ignition increases only slightly, but the ratio α_{TIM} decreases rapidly with a broadening pressure profile. If energy confinement does not degrade in the EPR for $q(\psi_\ell) < 3$, then the EPR could operate at higher plasma currents with broader pressure profiles ($\alpha < 1.3$) to achieve higher β_t and power output than are associated with the reference design.

Even if the pressure profile ($\alpha = \beta$) and the diamagnetic/paramagnetic parameter (δ) are fixed, there exists a wide range of solutions to the plasma power balance equations, corresponding to different plasma conditions. Thus, the reference design MHD solution corresponds to a variety of possible plasma solutions with corresponding performance parameters.

Table III-2. Sensitivity of the Plasma Performance to the Diamagnetic/Paramagnetic Parameter, δ
 $(\bar{T}_e = 10, \bar{T}_{DT} = 9.6, n\tau_E = 2.4 \times 10^{20} \text{ s/m}^3, 0.5\% \text{ } ^{16}\text{O} \text{ and } 5\% \text{ } ^9\text{Be} \text{ impurities, } \bar{Z}_{\text{eff}} = 1.3, q(a) = 1, \alpha = \beta = 1.3, \alpha_n = 0.3 \alpha_T = 1.0)$

$B_{\text{max}}^{\text{TFC}}$ (T)	δ	β_p	β_t	$q(\psi_R)$	I_p (MA)	P_O (10^5 Nt/m^2)	R_m (m)	$\bar{n}_{DT}$ (10^{20} m^{-3})	P_T (MW)	P_w (MW/m ²)	a_{TIM}	$J_L < 0$
8.0	-0.061	0.5	0.031	1.7	9.0	3.9	6.5	0.38	107	0.14	8.5	no
	-0.036	0.7	0.036	1.8	8.6	4.6	6.6	0.45	148	0.20	6.1	no
	-0.020	0.8	0.039	1.9	8.2	5.1	6.6	0.49	175	0.24	5.2	no
	0.000	1.0	0.043	2.0	7.8	5.6	6.7	0.53	206	0.28	4.4	no
	0.024	1.2	0.046	2.3	7.2	6.1	6.7	0.57	239	0.32	3.9	no
	0.037	1.4	0.047	2.5	6.8	6.4	6.8	0.59	253	0.34	3.8	no
	0.051	1.6	0.048	2.8	6.4	6.5	6.8	0.60	260	0.35	3.8	no
	0.059	1.7	0.048	3.0	6.1	6.6	6.9	0.60	261	0.35	4.0	no
	0.066	1.8	0.048	3.4	5.8	6.7	6.9	0.60	258	0.35	4.2	yes
	0.079	2.2	0.046	4.5	5.1	6.7	7.0	0.58	241	0.32	3.1	yes
	0.091	2.5	0.043	7.6	4.3	6.6	7.1	0.53	205	0.28	7.5	yes
10.0	-0.061	0.5	0.031	1.7	11.2	6.1	6.5	0.60	261	0.35	2.2	no
	-0.036	0.7	0.036	1.8	10.7	7.2	6.6	0.70	360	0.48	1.6	no
	-0.020	0.8	0.039	1.9	10.3	7.9	6.6	0.76	426	0.58	1.4	no
	0.000	1.0	0.043	2.0	9.8	8.7	6.7	0.83	503	0.68	1.2	no
	0.024	1.2	0.046	2.3	9.0	9.5	6.7	0.90	583	0.79	1.0	no
	0.037	1.4	0.047	2.5	8.5	9.9	6.8	0.92	617	0.83	1.0	no
	0.051	1.6	0.048	2.8	7.9	10.2	6.8	0.93	635	0.86	1.0	no
	0.059	1.7	0.048	3.0	7.6	10.4	6.9	0.94	638	0.86	1.0	no
	0.066	1.8	0.048	3.4	7.2	10.5	6.9	0.93	630	0.85	1.1	yes
	0.079	2.2	0.046	4.5	6.3	10.5	7.0	0.90	588	0.79	1.3	yes
	0.091	2.5	0.043	7.6	5.3	10.3	7.1	0.83	500	0.68	2.0	yes

Table III-3. Plasma Performance of the Optimum Equilibria as a Function of Profile

$[\bar{T}_e = 10 \text{ keV}, 0.5\% \text{ } ^{16}\text{O} \text{ and } 5\% \text{ } ^9\text{Be} \text{ Impurities}, \bar{Z}_{\text{eff}} = 1.3, q(a) = 1]$

$R_{\text{max}}^{\text{TFC}}$ (T)	$\alpha = \beta$	α_n	α_T	δ	β_p	β_t	$q(a)$	I_p (MA)	P_0 (10^5 Nt/m^3)	$\bar{n}_{DT}$ (10^{20} m^{-3})	$\bar{T}_{DT}$ (keV)	P_T (MW)	P_w (MW/m ²)	$n\tau_E$ (10^{20} s/m^3)	α_{TIM}
8.0	1.0	0.3	0.7	0.083	1.8	0.084	2.4	7.8	9.4	1.09	9.6	747	1.01	2.7	1.4
	1.2		0.9	0.066	1.7	0.058	2.8	6.6	7.4	0.72	9.6	367	0.50	2.5	2.8
	1.3		1.0	0.059	1.7	0.048	3.0	6.1	6.6	0.60	9.6	261	0.35	2.4	4.0
	1.4		1.1	0.053	1.7	0.040	3.3	5.6	6.0	0.50	9.6	187	0.25	2.4	5.4
	1.6		1.3	0.043	1.7	0.029	3.9	4.8	4.9	0.35	9.6	98	0.13	2.2	10.5
	1.8		1.5	0.036	1.6	0.021	4.5	4.1	4.0	0.25	9.7	53	0.07	2.2	19.5
	2.0		1.7	0.030	1.6	0.016	5.2	3.5	3.4	0.18	9.8	29	0.04	2.1	37.2
10.0	1.0	0.3	0.7	0.083	1.8	0.084	2.4	9.8	14.6	1.70	9.6	1820	2.46	2.7	0.4
	1.2		0.9	0.066	1.7	0.058	2.8	8.2	11.6	1.13	9.6	896	1.21	2.5	0.7
	1.3		1.0	0.059	1.7	0.048	3.0	7.6	10.4	0.94	9.6	638	0.86	2.4	1.0
	1.4		1.1	0.053	1.7	0.040	3.3	7.0	9.3	0.78	9.6	457	0.62	2.4	1.4
	1.6		1.3	0.043	1.7	0.029	3.9	6.0	7.6	0.54	9.6	239	0.32	2.2	2.7
	1.8		1.5	0.036	1.6	0.021	4.5	5.1	6.3	0.39	9.7	129	0.17	2.2	5.1
	2.0		1.7	0.030	1.6	0.016	5.2	4.4	5.3	0.28	9.8	70	0.09	2.1	9.7

Different decompositions of the pressure profile (α) into density (α_n) and temperature (α_T) profiles such that $\alpha = \alpha_n + \alpha_T = 1.3$ are examined in Table III-4. The power output is maximized by a very broad temperature profile ($\alpha_T \sim .1$) and a peaked density profile ($\alpha_n \sim 1.2$). The energy confinement required for ignition is minimized by comparable temperature and density profiles. It is more reasonable, however, to expect a broad density profile with a less broad temperature profile, and it should be noted that the performance is not so sensitive to α_n and α_T for fixed α as it is to changes in α .

For comparison, the performance parameters that result from the global plasma power balance when the temperature and density profiles are assumed to be flat ($\alpha_n = \alpha_T = 0$) are also shown in Table III-4. These solutions are based upon the same MHD solution ($I_p, \beta_c, \beta_p, q(\psi_i)$), plasma impurity concentration and average electron temperature ($\bar{T}_e = 10$ keV) as are the other global power balance solutions in the table. Accounting for the temperature and density profiles in the global power balance model results in a significant increase in power output and a significant decrease in the energy confinement required for ignition, relative to the use of flat temperature and density profiles. The manner in which these profiles are incorporated into the global power balance model is described in Appendix C.

For a given set of parameters characterizing the MHD equilibrium, and for a given decomposition of the pressure profile into temperature and density profiles, solutions to the plasma power balance equations can be obtained for a range of average ion and electron temperatures as shown in Table III-5. For a fixed β_c , an increase in average temperature requires a decrease in average density, and vice-versa. The fusion cross section* $\langle\sigma v\rangle$ is a rapidly increasing function of the ion temperature in the vicinity of $T_{DT} = 10$ keV, and the fusion reactivity parameter, $\langle\sigma v\rangle/T^2$, has a broad maximum around $T_{DT} \sim 13$ keV. The thermonuclear power is proportional to the reactivity. Because of higher central temperatures associated with a parabolic temperature profile, the total thermonuclear power output is maximized by operation at an average ion temperature of $\bar{T}_{DT} \approx 7-8$ keV. The energy confinement required for ignition decreases with temperature over the range

* Fusion cross section data are taken from References 8 and 9 for $T \leq 10$ keV and from References 10 and 11 for $T > 10$ keV.

Table III-4 Plasma Performance as a Function of Temperature and Density Profiles

($\bar{T}_e = 10$ keV, .5% ^{16}O and 5% ^9Be impurity, $I_p = 7.58$ MA, $Z_{\text{eff}} = 1.3$, $q(0) = 1$, $q(a) = 3.0$, $\alpha = \beta = 1.3$, $\bar{n}_p = 1.7$, $\bar{n}_t = 4.8\%$)

$B_{\text{max}}^{\text{TFC}}$ (T)	α_n	α_T	$\bar{n}_{DT}$ (10^{20} m^{-3})	$\bar{T}_{DT}$ (keV)	P_T (MW)	P_w (MW/m ²)	$n^2 E$ (10^{20} s/m^2)	I_{TIM}
8.0	0	1.3	0.66	9.8	250	0.34	2.8	4.9
	0.3	1.0	0.60	9.6	261	0.35	2.4	4.0
	0.6	0.7	0.58	9.5	271	0.37	2.3	3.6
	0.9	0.4	0.60	9.5	278	0.36	2.4	3.6
	1.2	0.1	0.67	9.6	280	0.38	2.8	4.2
	1.3	0	0.70	9.7	277	0.37	3.0	4.6
10.0	0	1.3	1.03	9.8	611	0.82	2.8	1.3
	0.3	1.0	0.94	9.6	638	0.86	2.4	1.0
	0.6	0.7	0.91	9.5	662	0.89	2.3	0.9
	0.9	0.4	0.94	9.5	680	0.92	2.4	1.0
	1.2	0.1	1.04	9.6	683	0.92	2.8	1.1
	1.3	0	1.10	9.7	677	0.91	3.0	1.2
8.0	0	0	0.70	9.6	188	0.25	4.4	6.6
10.0	0	0	1.10	9.6	460	0.62	4.4	1.7

Table III-5 Plasma Performance as a Function of Temperature

(0.5% ^{16}O and 5% ^9Be impurity, $\bar{Z}_{\text{eff}} = 1.3$, $q(0) = 1$, $q(a) = 3.0$, $\alpha = \beta = 1.3$, $\alpha_n = 0.3$, $\alpha_T = 1.0$, $\mu_p = 1.7$, $\beta_t = 4.8\%$)

T_{FC} B_{max} (T)	$\bar{T}_e$ (keV)	$\bar{T}_{\text{DT}}$ (keV)	$\bar{n}_{\text{DT}}$ (10^{20} m^{-3})	P_{T} (MW)	P_{W} (MW/m ²)	$n\tau_E$ (10^{20} s / m^3)	α_{TIM}
8.0	5	5.0	1.25	249	0.34	7.9	0.3
	6	5.9	1.04	270	0.36	5.0	0.5
	7	6.8	0.88	278	0.37	3.8	0.8
	8	7.8	0.77	277	0.37	3.1	1.5
	9	8.7	0.67	271	0.37	2.7	2.4
	10	9.6	0.66	261	0.35	2.4	4.0
	11	10.5	0.54	249	0.34	2.2	6.3
	12	11.4	0.49	236	0.32	2.1	9.7
	13	12.4	0.44	222	0.30	2.0	15.6
	5	5.0	1.96	607	0.82	7.9	0.07
	6	5.9	1.62	660	0.89	5.0	0.1
	7	6.8	1.38	679	0.92	3.8	0.2
	8	7.8	1.20	677	0.91	3.1	0.4
10.0	9	8.7	1.05	662	0.89	2.7	0.6
	10	9.6	0.94	638	0.86	2.4	1.0
	11	10.5	0.84	608	0.82	2.2	1.6
	12	11.4	0.76	576	0.78	2.1	2.5
	13	12.4	0.69	543	0.73	2.0	3.8

considered, but, since the TIM theory predicts a $\bar{T}^{-11/2}$ scaling, α_{TIM} increases with $\bar{T}$. It is apparent that the power performance could be improved relative to the reference design by operation at a lower average temperature.

3. Subignition Plasma Performance Analysis

In the event that the required energy confinement for ignition is greater than that which is obtained, the EPR plasma can be operated in a beam-driven mode. The reduction in required energy confinement that results from supplemental power injection into the plasma is shown in Table III-6. The parameter ξ is the ratio of the beam heating power (including suprathreshold fusion) to the alpha heating power, and Q_p is the ratio of the total power less the injected power to the injected power. Up to $P_B = 60$ MW of supplemental beam heating is currently included in the EPR design. It can be seen that increased supplemental heating is associated with lower required $n\tau_E$ and α_{TIM} , little change in wall loading, and increased total power. Most of the increase in total power is composed of the injected power itself -- the fusion power, as indicated by the neutron wall loading, is little changed.

Since the confinement that will be obtained in an EPR is uncertain at this time, it is instructive to look at the performance as a function of the confinement parameter, $n\tau_E$. Trapped-ion-mode (TIM) confinement theory is the most pessimistic of the confinement theories which are thought to be plausible for the reactor regime,² so the parameter $\alpha_{TIM} = (n\tau) / (n\tau \text{ predicted by TIM})$ is useful in gauging the reasonableness of a given value of $n\tau_E$. This parameter is shown in Figure III-7 for $\bar{T}_e = 5$ and 10 keV and $B_{max}^{TFC} = 8$ and 10 T. It can be seen that $\alpha_{TIM} < 10$ in all cases and that $\alpha_{TIM} \lesssim 1$ for a wide range of parameters. Thus, it appears that, even with the pessimistic TIM prediction, there exists a wide range of $n\tau_E$ over which operation in the beam-driven mode is possible.

The parameter Q_p , shown in Figure III-8, measures the efficiency with which thermonuclear power is produced in the beam-driven plasma. At ignition, $Q_p = \infty$, and net electrical power production is plausible for $Q_p \gtrsim 10$. An EPR should achieve $Q_p > 10$ if $n\tau_E > 1 \times 10^{20} \text{ s/m}^3$ at $\bar{T}_e = 10$ keV or if $n\tau_E > 4 \times 10^{20} \text{ s/m}^3$ at $\bar{T}_e = 5$ keV. For ignition, $n\tau_E > 2 \times 10^{20} \text{ s/m}^3$ is required at $\bar{T}_e = 10$ keV and $n\tau_E > 8 \times 10^{20} \text{ s/m}^3$ is required at $\bar{T}_e = 5$ keV.

Table III-6. Subignition Plasma Performance as a Function of Neutral Beam Power

($\bar{T}_e = 10$ keV, 0.5% ^{16}O and 5% ^9Be impurity, $\bar{Z}_{\text{eff}} = 1.3$, $q(a) = 3.0$, $\alpha = \beta = 1.3$, $\alpha_n = 0.3$, $\alpha_T = 1.0$, $\beta_p = 1.7$, $\beta_t = 4.8\%$)

$B_{\text{max}}^{\text{TFC}}$ (T)	ξ	q_p	P_B (MW)	$\bar{n}_{DT}$ (10^{20} m^{-3})	$\bar{T}_{DT}$ (keV)	P_T (MW)	P_w (MW/m ²)	$nT E$ (10^{26} s/m^2)	η_{TJM}
8.0	0.0	∞	0	0.60	9.6	261	0.35	2.4	1.0
	0.2	27.4	10	0.59	9.7	276	0.36	1.9	3.3
	0.4	14.0	19	0.59	9.8	291	0.37	1.6	2.8
	0.6	9.5	29	0.58	10.0	305	0.37	1.4	2.5
	0.8	7.3	38	0.57	10.1	319	0.38	1.2	2.3
	1.0	5.9	48	0.56	10.2	332	0.38	1.0	2.1
	2.0	3.2	93	0.53	10.9	394	0.41	0.6	1.5
	13.8	1.0	269	0.26	15.7	539	0.36	0.09	1.2
10.0	0.0	∞	0	0.94	9.6	638	0.86		1.0
	0.2	27.5	24	0.93	9.7	674	0.88	1	0.9
	0.4	14.1	47	0.92	9.8	710	0.90		0.7
	0.6	9.6	70	0.90	10.0	745	0.91	1.4	0.7
	0.8	7.3	94	0.89	10.1	779	0.92	1.2	0.6
	1.0	5.9	117	0.88	10.2	812	0.94	1.0	0.5
	2.0	3.2	227	0.82	10.9	961	0.99	0.6	0.4
	13.8	1.0	658	0.41	15.7	1320	0.89	0.09	0.3

Figure III-7. Confinement Requirement for Subignition, Beam-Driven Operation.

Figure III-8. Power Amplification for Subignition, Beam-Driven Operation.

Figure III-9 shows the total power, P_T , which is the thermal power out of the plasma by all means (neutrons, radiation, heat). It can be seen that power production improves with decreasing $n\tau_E$, so that many of the technological objectives of an EPR can be met in a subignition device, even if net power production is not possible. The increased power output at low values of $n\tau_E$ is, of course, a direct consequence of the increased supplementary heating, as can be seen in Table III-6.

The wall load, P_w , of 14 MeV neutrons on the first wall is shown in Figure III-10. The wall loading is relatively insensitive to $n\tau_E$, at $\bar{T}_e = 10$ keV, but can be substantially increased at $\bar{T}_e = 5$ keV by operation at low $n\tau_E$. As with the total power, however, more supplementary heating is needed for operation with low values of $n\tau_E$.

4. Particle and Radiation Fluxes on the First Wall

A major utilization of the EPR will be as a radiation test facility. Its large wall area, $A_w = 592 \text{ m}^2$, is an advantage for this purpose. The particle and radiation currents incident on the first wall and the corresponding spectra are shown in Table III-7 for $B_{\text{max}}^{\text{TFC}} = 8$ and 10 T. Both ignition (reference) and highly driven ($\xi = 1$) cases are shown. Table III-8 shows the neutron flux energy distribution at the first wall, normalized to a mean neutron wall load of $P_w = 0.5 \text{ MW/m}^2$. The poloidal variations in the neutron wall loading, as well as those in the various response rates, are given in detail in Section A of Chapter IX. These poloidal variations are affected by the curvature of the toroidal geometry and even more by the spatial distribution of the neutron source strength, which is taken to be shifted according to the magnetic flux surfaces of the reference design.

The fast alphas are those that escape before they undergo a collision. These alphas fall into two categories: those born on banana orbits that intersect the wall¹² and those trapped in asymmetries in the toroidal field.^{13,14} It is estimated in Section 5 that about 1.2% of the total alphas fall in the first category for $B_{\text{max}}^{\text{TFC}} = 8$ T, and about 0.4% for $B_{\text{max}}^{\text{TFC}} = 10$ T. The number of alphas falling in the second category depends on the field ripple, which has a maximum value of 1.3%. The alphas lost by this means will be lost near the outside edge of the plasma in localized regions between the toroidal field coils. It is assumed that 1% of the total alphas will be lost in this manner.

Figure III-9. Power Output for Subignition, Beam-Driven Operation.

Figure III-10. Neutron Wall Load for Subignition, Beam-Driven Operation.

Table III-7. First-Wall Particle and Radiation Currents for the EPR
Reference Design

($\bar{T}_e = 10$ keV, $\bar{T}_{DT} = 9.6$ keV, 0.5% ^{16}O and 5% 9Be Impurities,
 $\alpha = \beta = 1.3$, $\beta_c = 0.048$)

Species	$B_{max}^{TFC} = 10 T$		$B_{max}^{TFC} = 8 T$		Approximate Spectrum
	Ignition	Driven ($\beta=1$)	Ignition	Driven ($\beta=1$)	
	Current (particles/m ² /s)				
Fast Neutrons	1.3×10^{12}	4.2×10^{12}	1.6×10^{12}	1.7×10^{12}	$E = 14.1$ MeV
Deuterium	1.6×10^{19}	3.5×10^{19}	6.8×10^{18}	1.4×10^{19}	$\bar{T}_D = 200$ eV, ^c Maxwellian
Tritium	1.6×10^{19}	3.5×10^{19}	6.8×10^{18}	1.4×10^{19}	$\bar{T}_T = 200$ eV, ^c Maxwellian
Fast Alpha ^a	3.1×10^{15}	9×10^{15}	3.1×10^{15}	1.7×10^{15}	$E = 3.5$ MeV
Thermalized Alpha ^b	3.8×10^{17}	4.2×10^{17}	1.6×10^{17}	1.7×10^{17}	$\bar{T}_\alpha = 200$ eV, ^c Maxwellian
Total Neutrons	1.6×10^{19}	1.7×10^{19}	6.4×10^{18}	7.0×10^{18}	See Table III-8 ^d
Radiation					
	Current (MeV/m ²)				Approximate Spectrum
Brakestrahlung	2.2×10^{-2}	1.9×10^{-2}	9.0×10^{-3}	7.8×10^{-3}	$\bar{E}_B = 10$ keV, Maxwellian ^e
Synchrotron	9.4×10^{-3}	4.9×10^{-3}	2.5×10^{-3}	2.2×10^{-3}	Approx. Flat (ω_c to $20 \omega_c$)

^a It is estimated that 1.5% of the alphas escape before slowing down (or $B_{max}^{TFC} = 10T$ and 2.2% for $B_{max}^{TFC} = 8T$, because they are on banana orbits that intersect the wall or because they are trapped in azimuthal asymmetries in the magnetic field.

^b The calculational model assumes the alphas slow down, then diffuse out, so the alphas hitting the wall have the plasma edge temperature. On the other hand, there is an equilibrium distribution of slowing-down alphas with average energy of 3.3 MeV. Some of these will most likely diffuse out before they slow down, but an estimate of the number of these is not included.

^c A plasma edge temperature of 200 eV is assumed.

^d The distribution in Table III-8 is normalized to $P_{wall} = 1.5$ MW/m². The four cases in this table correspond to $P_{wall} = 0.86, 0.94, 0.95$ and 0.98 MW/m², respectively.

^e A small amount of line and recombination radiation with a discrete spectrum is included.

Table III-8. Neutron Flux and Currents at the Inner Surface of the EPR First Wall
(Results Normalized to a neutron wall loading of 0.5 MW/m²)

Group	E (Top) (eV)	Flux (n/m ² /s)	J_n (n/m ² /s)	Group	E (Top) (eV)	Flux (n/m ² /s)	J_n (n/m ² /s)
1	1.4913 (+7) ^b	4.318 (+17)	2.264 (+17)	25	4.0752 (+5)	3.046 (+17)	7.564 (+16)
2	1.3499 (+7)	1.938 (+16)	4.274 (+15)	26	3.0197 (+5)	1.998 (+17)	4.984 (+16)
3	1.2214 (+7)	2.806 (+15)	6.410 (+14)	27	2.2371 (+5)	1.455 (+17)	3.656 (+16)
4	1.1052 (+7)	2.626 (+15)	5.970 (+14)	28	1.6573 (+5)	1.859 (+17)	4.682 (+16)
5	1.0006 (+7)	5.562 (+15)	1.027 (+15)	29	1.2227 (+5)	3.134 (+17)	7.922 (+16)
6	9.0484 (+6)	1.957 (+15)	4.474 (+15)	30	6.7379 (+4)	1.192 (+17)	3.018 (+16)
7	8.1873 (+6)	9.302 (+14)	2.192 (+14)	31	3.1828 (+4)	8.654 (+16)	2.198 (+16)
8	7.4082 (+6)	1.343 (+15)	3.098 (+14)	32	1.5034 (+4)	7.838 (+16)	1.995 (+16)
9	6.7032 (+6)	1.135 (+15)	2.632 (+14)	33	7.1017 (+3)	1.066 (+17)	2.708 (+16)
10	6.0653 (+6)	1.731 (+15)	4.006 (+14)	34	3.3546 (+3)	1.001 (+17)	2.510 (+16)
11	5.4681 (+6)	2.188 (+15)	5.052 (+14)	35	1.5846 (+3)	5.794 (+16)	1.471 (+16)
12	4.9655 (+6)	2.796 (+15)	6.476 (+14)	36	7.4852 (+2)	4.748 (+16)	1.206 (+16)
13	4.4933 (+6)	3.336 (+15)	7.738 (+14)	37	3.5158 (+2)	4.038 (+16)	1.025 (+16)
14	4.0657 (+6)	4.094 (+15)	9.516 (+14)	38	1.6702 (+2)	3.440 (+16)	8.732 (+15)
15	3.6788 (+6)	4.950 (+15)	1.151 (+15)	39	7.8893 (+1)	2.848 (+16)	7.228 (+15)
16	3.3287 (+6)	5.838 (+15)	1.163 (+15)	40	3.7267 (+1)	2.252 (+16)	5.718 (+15)
17	3.0119 (+6)	7.874 (+15)	1.838 (+15)	41	1.7603 (+1)	1.674 (+16)	4.248 (+15)
18	2.7253 (+6)	1.244 (+16)	2.904 (+15)	42	8.3153 (+0)	1.146 (+16)	2.906 (+15)
19	2.4660 (+6)	5.410 (+16)	1.272 (+16)	43	3.9279 (+0)	7.026 (+15)	1.783 (+15)
20	1.8268 (+6)	8.602 (+16)	2.036 (+16)	44	1.8554 (+0)	3.744 (+15)	9.500 (+14)
21	1.3534 (+6)	1.084 (+17)	2.586 (+16)	45	8.7643 (-1)	1.677 (+15)	4.256 (+14)
22	1.0026 (+6)	1.277 (+17)	2.082 (+16)	46	4.1199 (-1)	2.354 (+14)	8.520 (+13)
23	7.4274 (+5)	2.142 (+17)	5.122 (+16)	---	---	---	---
24	5.5023 (+5)	1.613 (+17)	3.998 (+16)	SUM	---	3.176 (+18)	9.086 (+17)

a Current of neutrons incident on the plasma side of the first wall, but including neutrons which have undergone interactions in the blanket and shield. The current of neutrons incident on the inner surface of the first wall from the blanket side is identical, except in the first group where $J_n = 4.96 \times 10^{15}$ n/m²/s.

b 1.49 (+7) should be read 1.49×10^7 , etc.

5. Suprathermal Alpha Particle Effects*

Some of the fusion-created alpha particles are born on orbits that are not contained in the plasma and are, hence, lost to the first wall before they slow down.¹² In order to calculate the escape fraction, a grouping technique is employed. Each group is comprised of alpha particles born within a given phase space volume, and a characteristic orbit is plotted for each group.¹⁵ (At least 3800 groups are used.) Groups with orbits that extend past the plasma boundary are considered lost. The escape fraction is shown in Figure III-11 as a function of the pressure profile exponent, α , where the profiles are of the form given in Equation III-5. The density profile exponent is $\alpha_n = 0.3$; the temperature profile exponent is $\alpha_T = \alpha - \alpha_n$; and the current density exponent is $\alpha_J = \alpha - 1$. (The corresponding performance characteristics are shown in Table III-3.) In general, other things being equal, the loss fraction is lower for the more peaked profiles, but higher for lower plasma currents. For the "optimum" cases shown, the decrease in current associated with increased peaking is the dominant effect, and the loss fraction increases with profile peaking. For the reference design the loss fraction is 1.2% for $B_{\max}^{\text{TFC}} = 8 \text{ T}$ and is 0.4% for $B_{\max}^{\text{TFC}} = 10 \text{ T}$.

An estimate of the alpha particle heating distribution can also be made by using the group method. It is assumed that there is negligible pitch angle scattering and that the alpha particle deposits most of its energy before significant alterations in orbit occur. The energy deposition is calculated along the orbit as the alpha particle energy decreases from its initial value of 3.5 MeV to a final value of 500 keV, at which time it is assumed to be thermalized. The resulting power deposition profile is shown in Figure III-12. Also shown is the case, labeled "In Situ", where the alpha particle is assumed to slow down on the flux surface on which it is born. (This is the assumption used in the power balance calculations). It can be seen that for the EPR parameters the two calculations do not differ significantly, the maximum difference being about 5%.

*

The calculations in this section were performed by T. W. Perrie, Nuclear Engineering Department, University of Illinois, Urbana, Illinois.

Figure III-11. Fast Alpha Escape Fraction ($\alpha_n = 0.3$, $\alpha_T = \alpha - \alpha_n$, $\bar{T}_e = 10 \text{ keV}$, optimum MHD equilibria)

Figure III-12. Alpha Power Deposition Profile
 $(\alpha = 1.3, \alpha_T = 1.0, \alpha_n = 0.3)$
 $T_e = 10$ keV, reference design)

6. Toroidal Field-Ripple Effects

The use of a finite number of discrete TF coils to generate the toroidal field in a tokamak introduces a field asymmetry that can affect plasma particle and energy confinement. The ripple can produce particle and energy transport by two separate mechanisms. In one, the particles become trapped between maxima in the ripple field, i.e., between the planes defined by two adjacent TF coils, and are thereby prevented from encircling the plasma in the poloidal direction. Consequently, the radial component of the inhomogeneous magnetic field drift is not averaged by the rotational transform and these particles are preferentially lost. The fraction of particles so trapped is small. However, the field ripple asymmetry causes enhanced diffusion of those particles that are trapped in the ordinary magnetic well in tokamaks; i.e. those particles which are in banana orbits. These particles pass over the local ripple wells, but are trapped, as in an asymmetric torus, by the poloidal variation in the toroidal field. The ripple asymmetry introduces a radial drift of the banana guiding centers and this drift produces the increased transport.

Theoretical expressions for transport coefficients for both mechanisms have been developed, and numerical studies of these field-ripple transport effects have been performed for the EPR.¹⁶ With a 2% maximum field ripple, the diffusion coefficients due to the field ripple mechanisms are generally several orders of magnitude greater than the neoclassical values, but are one to two orders of magnitude less than the trapped-ion-mode values, at all radial locations. However, the ion heat transfer coefficient due to the banana-drift mechanism is more than 10% of the trapped-ion-mode value over the outer ($r/a \sim 0.8$) region of the plasma.

In order for the EPR to operate at ignition, the energy confinement must be 1-10 times better than the trapped-ion-mode prediction, depending upon the value of the toroidal field and the MHD equilibrium. The energy losses due to the field ripple must be no larger than those required for ignition. A conservative criterion is obtained by requiring that the ion heat transfer coefficient due to the banana-drift mechanism be everywhere at least a factor of 10 less than the trapped-ion-mode value.

If the toroidal field is generated by a continuously wound torus, the toroidal field variation along a tokamak field line, in terms of the quasi-toroidal coordinates (r, θ, ϕ) , can be expressed as

$$B_t(r, \theta) = \frac{B_{to}}{1 + \frac{r}{R_0} \cos \theta} \approx B_{to} \left(1 - \frac{r}{R_0} \cos \theta\right), \quad (\text{III-8})$$

where R_0 is the major radius, θ is the poloidal angle and B_{to} is the toroidal field at plasma center. In a practical TFC system consisting of N discrete coils, the gaps between the coils break the symmetry of the toroidal field. With toroidal field ripple present, the toroidal field, B_t , varies with poloidal angle θ , with plasma radial position r and with toroidal angle ϕ as well as with the number of coils N . Namely,

$$B_t(r, \theta, \phi) = \frac{B_{to}}{1 + \frac{r}{R} \cos \theta} [1 + \delta(r, \theta) \cos N\phi] \quad (\text{III-9})$$

$$\approx B_{to} \left[1 - \frac{r}{R} \cos \theta + \delta(r, \theta) \cos N\phi\right].$$

The ripple component of the toroidal magnetic field can be represented by

$$\delta(r, \theta) = \delta_0 \left(\frac{r}{a}\right)^k \exp\left(-\left(\alpha \frac{\theta}{\pi}\right)^2\right), \quad (\text{III-10})$$

where δ_0 is the maximum ripple amplitude occurring at the outside of the torus ($\theta = 0$) at the plasma surface ($r = a$).

$$\delta_0 = \frac{B_t^{\max}(r = a, \theta = 0) - B_t^{\min}(r = a, \theta = 0)}{B_t^{\max}(r = a, \theta = 0) + B_t^{\min}(r = a, \theta = 0)}. \quad (\text{III-11})$$

The parameters δ_0 , k and α are dependent on the reactor geometry and in particular upon the number of TF coils, N . For the EPR, detailed magnetic field calculations can be fit by Equation III-10 with $k = 4$ and $\alpha = 3.2$. The field ripple criterion that the ion heat transfer coefficient due to the banana-drift mechanism be a factor 10 less than the TIM value can be expressed

$$\frac{\delta_0}{k + 2} \leq \frac{0.1}{Nq(a)}, \quad (\text{III-12})$$

where $q(a)$ is the safety factor at the plasma surface. A plot of the maximum tolerable field ripple versus the number of TF coils is given in Figure III-13, as a function of $q(a)$. For $N = 16$ and $q(a) = 3.0$, the reference design values, the maximum tolerable field ripple is $\delta_0 \approx 1.3\%$.

B. Transient Performance Analysis

The burn cycle dynamics of the plasma, the plasma driving system (ohmic heating and equilibrium field) and the plasma heating system are analyzed in this section. The plasma dynamics model described in Appendix C, the neutral injection system model described in Chapter V, and the circuit model for the ohmic heating and equilibrium field coil systems described in Figure III-14 are used in the analysis. The reference design plasma conditions defined in the previous section are approximated during the flattop portion of the burn pulse, and the MHD relationships for q , β_t , β_p and I_p are used as limits in the dynamic simulation (e.g. the β_p value from the MHD calculation is taken as an upper limit which constrains the density and temperature). The entire burn pulse -- startup, burn and shutdown -- is included in the analysis. Requirements for the plasma driving and heating systems are established for a range of possible startup modes, and a reference mode is selected. The power performance of the EPR is determined based upon the burn cycle dynamics simulation, and the sensitivity to various factors is evaluated.

1. MHD Equilibria During the Burn Cycle

During the burn cycle the toroidal field at the coils, $B_{\max}^{\text{TFC}}$, is held constant, and it is assumed that the MHD equilibrium evolves with a fixed profile exponent, $\alpha = \beta = 1.3$. The equilibrium at any time is then uniquely characterized by the values of I_p and β_t at that time. The burn cycle dynamics calculation at each time step involves: (1) determination of I_p and β_t from the coupled plasma power balance and plasma driving system circuit equations (to be described); (2) determination of the MHD equilibrium that is characterized by I_p and β_t ; (3) determination of the equilibrium field, B_{EF} , that is required for the equilibrium, and (4) determination of the current, I_{EF} , in the EF coils that is required to produce the equilibrium field. Thus, this procedure determines a self-consistent relationship $I_{\text{EF}} = I_{\text{EF}}(I_p, \beta_t \text{ or } \beta_p)$ that defines the current required in the equilibrium field coils.

Figure III-13. Toroidal Field Ripple.

PLASMA DRIVING CIRCUIT

$$\begin{aligned}
 V_{OH} &= L_{OH} \dot{I}_{OH} + M_{POH} \dot{I}_P + M_{EFOH} \dot{I}_{EF} \\
 V_{EF} &= M_{OH} \dot{I}_{OH} + M_{PEF} \dot{I}_P + L_{EF} \dot{I}_{EF} \\
 -I_P R_P &= M_{POH} \dot{I}_{OH} + L_P \dot{I}_P + M_{PEF} \dot{I}_{EF}
 \end{aligned}$$

$$\begin{aligned}
 M_{POH} &= -.570 \text{ mH}, \quad \kappa = -.247 \\
 M_{PEF} &= -.628 \text{ mH}, \quad \kappa = -.757 \\
 M_{EFOH} &= 7.83 \text{ mH}, \quad \kappa = .0158
 \end{aligned}$$

Figure III-14. Plasma Driving System Circuit Diagram

The determination of the equilibrium, for a given I_p and β_t , is very similar to the determination of the optimum equilibrium in the previous section, except that instead of scaling the solution to give $q(0) = 1$, the solution is scaled to give the required I_p . Characteristics of a range of equilibria, from β_p near zero to beyond the optimum of 1.7 are shown in Table III-9 for the case where the current is the peak current (for $B_{\text{max}}^{\text{TFC}} = 10 \text{ T}$), $I_p^{\text{max}} = 7.58 \text{ MA}$. (It can be seen that equilibria with $\beta_p > 1.7$ have $q(0) < 1$ as well as regions of current reversal.) Similar equilibria can be obtained for other values of the plasma current by scaling the quantities in Table III-9 according to the Eqs. III-6 and III-7 with $\lambda = I_p / I_p^{\text{max}} \leq 1$. (The external field at the center of the chamber, B_o^{EF} , scales as $B_o^{\text{EF}} \rightarrow B_o^{\text{EF}} \lambda = \lambda B_o^{\text{EF}}$.) The equilibrium and the required B_o^{EF} corresponding to a particular value of β_t for that current can then be found. The equilibrium current, I_{EF} , is assumed proportional to B_o^{EF} . Since $\lambda \leq 1$, this procedure insures $q(0) \geq 1$ and no reversed currents if β_p does not exceed 1.7.

For a given I_p and β_t it has been found that β_p , $q(\psi_\ell)$, and B_o^{EF} are relatively insensitive to the profile exponent, $\alpha = \beta$, although $q(0)$ varies with the profile. Thus, the required current in the EF coils is relatively insensitive to the choice of profile shape.

2. Plasma Driving and Heating System

In estimating the plasma driving requirements for the EPR, a circuit model has been used to describe the plasma, the OH coil system, and the EF coil system. Once the plasma equilibrium condition and current profile are determined, it is possible to fix the locations of and currents in the ohmic heating (OH) and equilibrium field (EF) coils. This in turn fixes the three self inductances and the three mutual inductances. It is possible to arrive at a plasma driving system design which (1) meets the requirements for MHD equilibrium in a circular cross section plasma, (2) decouples the OH and EF coil systems and (3) keeps the OH field small at the plasma. A free boundary MHD code is used to determine the exact placement and currents for the EF coils that are required for the plasma equilibrium, as described in Section IV-B.

The poloidal coil system-plasma circuit is shown in Figure III-14. The self and mutual inductances of the plasma, the OH coil, and the EF coil

Table III-9. MHD Equilibria for the Peak Plasma Current, $I_p^{\max} = 7.58 \text{ MA}$ ($\alpha = \beta = 1.3$; $B_{\max}^{\text{TFC}} = 10 \text{ T}$)

β_p	β_t	$q(\psi_\ell)$	$q(0)$	p_0 (10^5 Nt/m^2)	δ	B_o^{EF} (T)	R_m (m)	$J_t < 0$
0.0018	0.000048	2.4	1.6	0.009	-0.060	0.24	6.4	no
0.0036	0.000095	2.4	1.6	0.018	-0.060	0.24	6.4	no
0.0071	0.00019	2.4	1.6	0.037	-0.060	0.24	6.4	no
0.014	0.00038	2.4	1.6	0.073	-0.059	0.24	6.4	no
0.028	0.00074	2.4	1.6	0.14	-0.058	0.25	6.4	no
0.054	0.0014	2.4	1.6	0.28	-0.056	0.25	6.4	no
0.10	0.0027	2.4	1.6	0.53	-0.054	0.26	6.4	no
0.19	0.0050	2.4	1.6	0.96	-0.048	0.27	6.4	no
0.32	0.0083	2.4	1.5	1.6	-0.041	0.28	6.5	no
0.54	0.014	2.4	1.5	2.8	-0.028	0.31	6.5	no
0.70	0.018	2.5	1.4	3.6	-0.018	0.33	6.6	no
0.82	0.021	2.5	1.4	4.3	-0.011	0.34	6.6	no
1.0	0.026	2.6	1.3	5.2	0.000	0.36	6.7	no
1.2	0.032	2.7	1.2	6.7	0.017	0.39	6.7	no
1.4	0.037	2.8	1.1	7.8	0.029	0.41	6.8	no
1.6	0.044	2.9	1.0	9.3	0.047	0.44	6.8	no
1.7	0.048	3.0	1.0	10.4	0.059	0.46	6.9	no
1.8	0.053	3.2	0.9	11.5	0.073	0.48	6.9	yes
2.2	0.066	3.7	0.8	15.0	0.113	0.53	7.0	yes
2.5	0.087	5.3	0.7	21.0	0.185	0.60	7.0	yes

were calculated using the actual current distribution in the coils and approximating the plasma by six current blocks. If the sign convention is chosen so that the plasma, ohmic heating, and equilibrium field currents are positive at the end of the heating period, then it is clear by Lenz's Law that the mutual inductances M_{POH} and M_{PEF} are negative. The EF and OH coils are designed to make M_{EFOH} small. (Coupling constants are defined in the usual way, $K_{12} = M_{12}/\sqrt{L_1 L_2}$.) Power supply losses are not modeled dynamically since they are small but are reckoned in the energy balance. The average plasma resistance, R_p , is calculated using a multiple of the Spitzer formula. For the reference case $\eta = 3 \eta_{sp}$ has been assumed. The plasma heats up due to $I_p^2 R_p$ ohmic heating and subsequently due to beam heating, which reduces the resistivity. The plasma power and particle balance model of Appendix C is used.

This model is inapplicable during the early stages of the startup, i.e. when $\bar{T}_e < 100$ eV and $I_p < 100$ kA. A separate simulation is done, in which a special coil is placed near the plasma, providing a higher toroidal electric field and a high order multipole null in the plasma to get the plasma started (see section III-C). The principal result of importance here is that the startup coil, expending about 4 V-s of flux in about 10 ms, will produce a plasma of sufficient current and electron temperature to continue growing with the OH coil alone, when the startup coil is fired ≈ 0.3 s after the initiation of the 2 s OH current reversal. At that time only 4 V-s of OH coil flux have linked the plasma vessel. This is small compared to the total flux change available. Thus, for OH reversal times in the range of this study, initial startup does not pose a major problem. Much longer OH reversal times might pose major power supply and coil problems, however.

The equilibrium field must follow the ramp-up of the plasma current in order to maintain plasma equilibrium. The required field depends upon both the plasma current and the plasma pressure, as discussed in the previous section. The EF field rises in an almost linear fashion, minimizing the EF coil maximum power.

The power supply philosophy for the plasma driving system is as follows. The EF coil must be programmed and therefore has been chosen to be a phase

controlled rectifier circuit operating out of magnetic energy storage (see Chapter VI). Since the magnetic energy is fixed by the required equilibrium field, the principal variable cost is associated with the power. Thus, the plasma heating cycle should be as slow as possible to minimize this EF power cost. The OH field need not be programmed but must cycle as rapidly as possible to build up the plasma current and heat the plasma rapidly in order to avoid wasting energy. Thus, high power is required in the OH circuit, and the power supply cost should depend primarily on stored energy, as opposed to power. High voltage multidrum homopolar generators are well suited to this task. They can operate efficiently, and have the property that their effective capacitance can be smoothly varied by changing the magnetic field, allowing control over the OH cycle time. They further can be connected in series or parallel to vary the voltage or capacitance. In this application the OH energy is in the coil, and the drums are nearly at rest except during OH field reversal, so that losses are minimized. An additional small phase controlled rectifier provides flux for maintaining the plasma current during the burn and for resetting the OH current prior to current reversal during shutdown. This rectifier can operate out of the same energy storage unit used with the EF coil.

By reversing the OH current rapidly, the plasma becomes conductive quickly, and the volt-seconds provided by the EF coil are effective in increasing the plasma current. Thus, typically, the plasma current reaches 70% of its final value at the end of the OH current reversal. The remaining current is induced by the EF coil.

The process of plasma heating can be augmented by initiating the beam injection during the OH current reversal. There is then a trade-off between the beam energy consumed and the reduction in OH energy requirements which must be considered.

The basic burn cycle under consideration is then:

- (1) Initiation of OH current reversal at $t = 0$, with reversal time Δt_{OH} ;
- (2) Initiation of beam heating at time $t_B \leq \Delta t_{OH}$, with beam power P_B to the plasma;
- (3) EF current ramp-up;
- (4) Ignition followed by DT burning; and
- (5) Shutdown.

This burn cycle is shown schematically in Figure III-15, with times corresponding to the reference burn cycle.

With reference to Figure III-14, at the start of the cycle, switch S1 is closed and switch S2 is opened (at zero voltage) with negative current in the OH coil. The current in the OH coil flows in the homopolar generator, C, accelerating the drums. The homopolar generator appears as a capacitance in the circuit. When the current is reversed and the voltage is zero again, switch S2 is closed and S1 is opened. Henceforth, the current flows through the power supply, which controls the voltage on the OH coil. At the end of the burn, during shutdown, the power supply voltage, V_{PS} , is reversed and readjusts the level of I_{OH} so that reversal with the homopolar generator leaves the state of I_{OH} as it was at the beginning of the cycle. At the start of cycle, switch S3 is opened and V_{EF} is programmed to maintain equilibrium. At the end of beam heating, switch S3 can be closed, or, more likely, a small control voltage will be maintained to control plasma position. At the end of the burn, the EF coil is ramped back to zero during shutdown.

At this point the importance of decoupling the EF and OH coils becomes clear. Strong coupling would cause a large induced voltage in the EF circuit, due to the OH pulse, greatly complicating and increasing the requirements on the EF power supply.

It is worth mentioning that, since the plasma is diamagnetic, currents flow in the poloidal direction causing a toroidal flux change which links the TF coils. For the cases considered here, the flux change is about 1/4 V-s on the time scale of the EF current. The induced voltage in the total TF coil system is about 600 volts. TF coil stability would be enhanced by maintaining constant TF coil current, (as opposed to maintaining constant TF coil flux, which would cause a field change at the coils). Thus, the TF coil power supplies must be programmed to cancel this induced voltage.

3. Startup Requirements

The determination of the design requirements for the plasma driving and heating systems involves finding a compromise among several, often conflicting, technological and cost limitations. These design requirements are quite sensitive to the time sequence of the startup (and shutdown) procedure. A range of potential startup procedures were simulated dynamically,

Figure III-15. Burn Cycle Scenario

following the format described in the previous section. The main parameters of interest are: (1) energy transfer from the homopolar supply (U_{OH}) -- peak OH power (P_{OH}) requirements are less important; (2) peak power in the EF supply (P_{EF}) -- EF energy transfer (U_{EF}) does not vary appreciably since the plasma is very conductive during most of the EF ramp-up; (3) total beam energy to heat the plasma (U_B) and the total energy (U_{BE}) drawn from the energy storage unit for the beam heating; (4) maximum rate of change of the field in the OH coil ($\dot{B}_{OH}$) -- the maximum coil voltage (V_{OH}) is proportional to $\dot{B}_{OH}$; and (5) maximum field (B_{OH}) in the OH coil -- the OH coil current is proportional to B_{OH} . The technological aspects of the tradeoff between current and voltage are treated in Chapter IV on coil design and in Chapter VI on power supplies, and the relationship between U_B and U_{BE} is discussed in Chapter V on neutral injectors.

Parameters varied in the study are: (1) OH current reversal time, Δt_{OH} -- essentially determined by C, the equivalent capacitance of the homopolar system (see Table III-10); (2) time of beam firing, t_B ; (3) beam power to the plasma, P_B ; (4) plasma resistivity, η ; and (5) oxygen concentration, as a measure of the effect of impurities. The results of the study are summarized in Table III-11, for a variety of possible startup procedures and conditions. Selected results are also plotted in Figures III-16 to III-20, to illustrate important features.

Table III-10. Capacitance Requirements for OH Homopolar Generator

Reversal Time Δt_{OH} (s)	Capacitance (F)
0.5	0.050
1.0	0.224
2.0	0.897
3.0	3.586

Table III-11. Plasma Driving and Heating System Requirements

Case	Δt_{OH} (s)	t_B (s)	P_B (MW)	Other	OH								EF ^a		BEAMS		P_{Net}^d (MW)
					V-s Total	V-s ^b Res	B_{OH} Max (T/s)	B_{OH}^c Max (T)	U_{OH} Max (MJ)	P_{OH} Max (MW)	I_{OH} Max (kA)	V_{OH} Max (kV)	P_{EF} Max (MW)	V_{EF} Max (kV)	U_B (MJ)	U_{BE} (MJ)	
1	0.5	0.5	60	$n = 3n_{sp}$, 0.5% oxygen concentration	44.0	12.8	16.6	2.81	428	2390	45.1	118	650	42.5	263	907	43
2	1.0	1.0	60		53.3	21.9	9.9	3.43	603	1790	51.9	71	365	17.6	265	914	44
3	2.0	2.0	60		72.3	40.6	6.7	4.40	1060	1620	70.4	48	389	10.4	268	924	41
4	1.0	0.5	60		47.9	15.8	9.1	3.02	502	1520	48.4	65	561	27.4	283	975	45
5	<u>2.0</u>	<u>1.0</u>	<u>60</u>		<u>61.2</u>	<u>29.9</u>	<u>5.8</u>	<u>3.78</u>	<u>776</u>	<u>1220</u>	<u>60.5</u>	<u>41</u>	<u>416</u>	<u>20.8</u>	<u>286</u>	<u>986</u>	<u>43</u>
6	4.0	2.0	60		79.0	45.6	3.6	4.80	1240	989	76.8	26	373	30.7 ^e	324	1117	39
7	2.0	0.5	60		47.8	14.8	4.6	3.02	503	770	48.4	33	398	18.9	319	1100	43
8	2.0	1.0	20		72.9	39.9	6.4	4.65	1140	1490	74.4	46	291	9.1	364	1255	35
9	2.0	1.0	40		65.1	33.3	6.0	4.03	857	1330	64.5	43	338	12.6	302	1041	40
10	2.0	1.0	80		60.9	27.8	5.8	3.76	776	1220	60.2	41	503	25.7	289	997	44
11	2.0	1.0	60		45.9	13.6	4.5	2.90	474	722	46.3	32	403	16.9	301	1037	43
12	2.0	1.0	60		78.4	45.8	7.2	4.79	1210	1910	76.7	51	471	23.3	291	1003	43
13	2.0	1.0	60		52.6	20.4	5.0	3.29	595	927	52.7	36	413	27.4	285	983	56
14	2.0	1.0	60		78.7	46.0	7.3	4.76	1240	1900	76.2	52	432	17.4	321	1107	28

a) $I_{EF} = 80$ kA, $U_{EF} = 1500$ MJ, EF Coil Provides 50 V-s to plasma

b) Resistive volt-sec

c) max during heating; further increase during burn

d) P_{Net} is for max cycle length

e) Shortly after transient during beam turn on.

Figure III-16. Effect of OH Current Reversal Time on Plasma Driving and Heating System Requirements.

Figure III-17. Effect of Beam Initiation Time on Plasma Driving and Heating Systems Requirements.

Figure III-18. Effect of Beam Power on Plasma Driving and Heating System Requirements

Figure III-19. Effect of Plasma Resistivity on Plasma Driving and Heating System Requirements

Figure III-20. Effect of Oxygen Impurity on Plasma Driving and Heating System Requirements

In Figure III-16, U_{BE} , U_{OH} , P_{EF} , B_{OH} and $\dot{B}_{OH}$ are plotted as a function of the length, Δt_{OH} , of the OH current reversal. For times shorter than 2 s, a penalty is paid in the EF power requirement and in a very high $\dot{B}_{OH}$, with essentially no compensating reduction in U_{BE} or B_{OH} . On the other hand, the plasma current and temperature builds up rapidly, and U_{OH} and the resistive volt-seconds are considerably reduced by shorter Δt_{OH} . For times longer than 2 s, there are large increases in U_{OH} and U_{BE} because the OH voltage is quite ineffective at increasing the plasma current in the presence of the higher resistive losses associated with the cooler plasma. In fact the current rises slowly until U_{OH} reaches its maximum, wasting about one-half the OH flux, and more beam energy is required to heat the plasma. P_{EF} reaches its minimum for $\Delta t_{OH} = 4$ s, the maximum value occurring after ohmic heating, during beam heating. $\dot{B}_{OH}$, on the other hand, is reduced by the longer Δt_{OH} , but not to the level of present day technology. The two second reversal ($\Delta t_{OH} = 2$ s) is chosen as the reference case.

The ohmic heating requirements can be reduced by using supplemental beam heating during the OH current reversal period, Δt_{OH} , to reduce the resistive volt-seconds. In Figure III-17, the beam initiation time, t_B , is varied during the ohmic heating current reversal, for $\Delta t_{OH} = 2$ s. At $t = 0.5$ s, the plasma current (~ 150 kA) has not really started, and injection heating too early may enhance the formation of plasma skin currents, leading to unfavorable confinement. (This phenomenon is not included in the global model simulation.) The compromise position is to adopt $t_B = 1$ s for the reference case, but to design the coils and power supplies for the more demanding case in which the beam heating is not initiated until the end of the ohmic heating current reversal ($t_B = \Delta t_{OH} = 2$ s).

Figure III-18 shows the dependence upon beam power. At lower beam power, penalties are paid in all parameters except the EF coil power. The 20 MW case is singular in that ohmic heating (via the rectifier) is still effective after the homopolar swing (Δt_{OH}) because of the slow plasma heatup with $P_B = 20$ MW. The EPR will start up satisfactorily with only 40 MW of power, and there is no reason to go beyond 60 MW. Thus, selection of $P_B = 60$ MW for the reference design provides some margin.

It is important to test the sensitivity of the design to parameters less easily controlled than those above. For this purpose the plasma

resistivity and impurity concentrations are varied between $\eta = \eta_{sp}$ and $\eta = 6\eta_{sp}$, and from 0-1% oxygen impurity. The results are shown in Figures III-19 and III-20. The principal effects of increased plasma resistivity on impurity concentration are increased requirements on the ohmic heating energy (U_{OH}) and field (B_{OH}). Calculations at $\eta = 10 \eta_{sp}$ were unstable, with small variations in ohmic heating stored energy causing large variations in results, because of difficulty in starting the plasma. Better results would be obtained by shortening the OH cycle time if the resistivity were this high. The effect of high impurity concentration (1% oxygen) is to keep the plasma cool for a longer time (due to line and recombination radiation) and to increase the resistivity. The poloidal coil requirements for this case are similar to the $6 \eta_{sp}$ case.

The considerations above led to a choice of case 5 in Table III-11 as the reference case for performance calculations. Table III-12 presents a more detailed list of the properties of the reference case.

A more demanding case (no. 3 in Table III-11) was chosen as the design basis for the power supply, energy storage and coil systems. The EF coil system is designed to provide 50 V-s to the plasma, and the OH coil system is designed to provide 85 V-s, for a total of 135 V-s. About 4.5 V-s are required for a 1 minute burn, so 135 V-s provides an ample margin.

4. Reference Case Burn Cycle Simulation

In order to illustrate features of the burn cycle dynamics of the EPR, the reference case (case 5 in Table III-11) has been chosen for detailed examination.

As an impurity control measure, the reactor first wall is coated with beryllium. The reasons for this choice are discussed in detail in Appendix B. Briefly, analysis shows that some form of low-Z coating must be used; a bare stainless steel wall results in unacceptably high radiation from the sputtered ions. There are a number of possible coating materials, but Be appears to give the longest burn times and the best power performance.

The sputtering of Be by incident particles is a function of the particle kinetic energy. The sputtering coefficients used here have been obtained by taking a Maxwellian average of the differential-energy sputtering data,

Table III-12. Plasma Heating and Driving System
Reference Case Parameters

<u>Plasma</u>		
Maximum Current	7.58	MA
Maximum Thermal Energy	270	MJ
Resistive Energy Dissipation ($\eta = 3\eta_{sp}$)	90	MJ
<u>Ohmic Heating System</u>		
Current Reversal Time	2	s
Peak Current	60.5	kA
Peak Voltage	41	kV
Maximum Energy Transfer	776	MJ
Peak Power	1220	MW
Equivalent Capacitance of Homopolar Generator	0.896	F
Maximum Field in Coil	3.8	T
Maximum Field Rise in Coil	5.8	T/s
Volt-seconds to Plasma		
Inductive, startup	31.3	V·s
Resistive, startup	29.9	V·s
Resistive, burn	4.0	V·s
<u>Equilibrium Field System</u>		
Peak Current	80	kA
Peak Voltage	20.8	kV
Maximum Energy Transfer	1480	MJ
Peak Power	416	MW
Volt-seconds to Plasma	50	V·s
<u>Neutral Beam Injection System</u>		
Beam On	1.0	s
Beam Duration	4.77	s
Beam Energy	180	keV
Beam Power to Plasma	60	MW
Energy Deposited in Plasma	286	MJ
Power Required to Operate Injection System	485	MW
Directly Recovered Electrical Power	278	MW
Net Electrical Power to Operate System	207	MW
Net Electrical Energy to Operate System	986	MJ
<u>Magnetic Energies</u>		
$\frac{1}{2} L_P I_P^2$	332	MJ
$\frac{1}{2} L_{EF} I_{EF}^2$	1660	MJ
$\frac{1}{2} L_{OH} I_{OH}^2$	876	MJ
$M_{POH} I_P I_{OH}$	- 259	MJ
$M_{PEF} I_P I_{EF}$	- 386	MJ
$M_{EFOH} I_{EF} I_{OH}$	39	MJ
Total	2262	MJ

assuming a fixed plasma edge temperature of 200 eV. With this assumption, the average energy of the particles incident on the first wall is $\frac{3}{2} \times 200 = 300$ eV. This corresponds to the peak of the D-T sputtering curve; a value of $S_{D-T} = 0.028$. This is therefore a worst case assumption, with respect to beryllium sputtering, although a quite plausible one. The other sputtering coefficients used are $S_{\alpha} = 0.07$, $S_{Be} = 0.35$ and $S_n = 0.0005$. Wall reflection coefficients used are $R_{DT} = 0.98$, $R_{\alpha} = 0.98$ and $R_{Be} = 0.05$. The plasma-wall interaction model, which includes recycling and charge-exchange, is discussed in Appendix B.

The general features of the burn cycle are shown schematically in Figure III-15. The basic burn cycle, which is the focus of this section, begins just after plasma breakdown and continues for ≈ 55 seconds. The cycle begins when the ohmic heating (OH) system homopolar generator is connected to the previously charged OH primary coil. The OH primary current then reverses in 2.0 s, inducing about 2/3 of the final plasma current and ohmically heating the plasma. At $t = 2$ s, the OH coil is connected to the OH power supply and is disconnected from the homopolar generator. Midway during the OH current reversal, at 1.0 s, injection of 60 MW of neutral beam power is initiated. The beam injection is initiated early (before the plasma current is fully ramped up) in order to reduce the resistive volt-seconds that would otherwise be expended. At about 5.8 seconds the plasma reaches ignition and the beam heating is terminated. (If confinement is subignition, beam heating is continued as required during the plasma burn.) During this period, from 0-5.8 s, the current in the equilibrium field (EF) coil is continuously adjusted to maintain the plasma in MHD equilibrium. The dependence of the EF current on the plasma current and on β_p is determined by MHD equilibrium calculations. At the time the beam is turned off, the EF current, the plasma current and the plasma β_p have reached their maximum values. The OH current continues to increase during the burn phase to offset resistive losses in the plasma.

After about 5 seconds, the plasma is in the burn phase, where substantial thermonuclear power is produced. Were it not for the buildup of He and Be, the plasma would remain at this ignition point indefinitely, in the absence of MHD instabilities. Because of the buildup of He and Be the plasma gradually cools off. At about 48 s the plasma is no longer

producing significant power and the shutdown period is initiated. (The cooling off of the plasma can be delayed by supplemental heating -- e.g. with beams.) For the next 5 seconds the OH, EF and plasma currents are all reduced somewhat in preparation for the final rampdown. During this period the OH current is reduced to the value it had at $t = 2$ s, so that the OH homopolar generator will handle the same current swing on shutdown and startup. The plasma cools rapidly during this time. Finally, for the last 2 seconds of the burn cycle, the OH homopolar generator is re-connected, all currents are ramped down, and the cycle terminates. These currents are shown in Figure III-21, and β_p is shown in Figure III-22. In the next ~ 15 seconds the vacuum ports are opened, the toroidal chamber is evacuated, and preparations are made for the next burn cycle.

The effect of the burn cycle sequence just described on the pulsed power and energy requirements is shown in Figure III-23, III-24, and III-25. The total OH coil energy transfer can be seen in Figure III-23. The very large power spikes at the beginning and end of the cycle occur during the period when the OH coil current is allowed to flow through the homopolar generators, leading to a reversal of the OH coil current. During the burn, power is supplied to the OH coil by a rectifier to provide OH voltage to maintain plasma current. At the end of the burn, the rectifier phase is shifted to invert (~ 66 MW) to reduce the plasma current and recover the magnetic field energy put in during the burn. During the rectifier phase, homopolar generator energy losses are replenished by the rectifier. When the OH current reaches the proper level, the OH coil is again connected to this homopolar, leading to another current reversal ending at the current value at which the cycle started. During this reversal, the plasma current is reduced to essentially zero. The OH system is dormant during the pumpdown of the vacuum vessel.

Figure III-24 shows the EF coil energy and power requirements. Essentially, the coil is ramped up in ~ 7 s during startup and down in ~ 7 s at the end of the burn, with the current chosen to provide equilibrium for the assumed profile. Some adjustment is necessary during the burn to accommodate variations in the plasma β_p . Since the EF coil current is ramped slowly, it does not provide much ohmic heating, but produces the final flux necessary to induce the final plasma current. As a result, there is little

Figure III-21. Plasma, OH and EF Currents During Reference Burn Cycle

Figure III-22. Poloidal Beta During Reference Burn Cycle.

Figure III-23. OH Co-1 System Power and Energy Transfer During Reference Burn Cycle.

Figure III-24. EF Coil System Power and Energy Transfer During Reference Burn Cycle

A) ENERGY AND POWER DEMAND

B) POWER CONNECTIONS

Figure III-25. Power and Energy Transfer Out of the Energy Storage Unit During Reference Burn Cycle.

$I_{pp}^2 R$ loss in the EF circuit. The peak power is typically 400 MW and can come either three quarters of the way through the OH cycle or near the end of beam heating, depending on the cycle.

The homopolar generators communicate with the external power grid only through the OH rectifier and the energy storage system, so that the peak power produced by the homopolar generators does not appear in the storage system or the external electric grid. The neutral beam system, the OH rectifier and the EF rectifier are the elements which operate out of the energy storage system. In Figure III-25, the sum of the powers and energies of the three systems are shown for the reference burn cycle. As discussed in Chapter V on neutral beams, an instantaneous electrical efficiency of 0.29 is assumed for the beams, that is, pulsed electrical energy of 207 MW must be supplied to produce 60 MW of beam power. This is because the electrical energy recovery is simultaneous, while the thermal-electric energy recovery is averaged over the cycle. The net energy withdrawn at the end of the cycle is 1.44 GJ, of which 986 MJ is due to beams, and 85 MJ is $I_{pp}^2 R$ heating. Another 370 MJ must be added for power supply losses* so that the average power to operate these systems is 20.6 MW, based upon a 70 s cycle with a 55 s burn and a 15 s replenishment. This power is provided uniformly. The maximum energy withdrawal occurs at the end of beam heating, which is also the time of maximum power. Then the energy storage unit must provide 2.44 GJ and a power of 620 MW at maximum depletion. This is discussed in more detail in Chapter VI.

Variation of plasma particle densities during the burn cycle are shown in Figure III-26. The initial D-T ion density is $0.89 \times 10^{20} \text{ m}^{-3}$, composed of equal parts of deuterium and tritium. During the cycle, the ions can be lost in two ways, by fusion and by transport to the first wall. The D-T ion loss can be replenished by a combination of recycling from the wall and an external refueling source. The neutral beam, when on, can also help replenish the plasma. By varying the refueling rate from the external source, the ion density can be controlled within certain limits. For the first 14 seconds of the cycle the density is held constant; the refueling current needed to do this is shown in Figure III-26, assuming 98% recycling.

* A 5% loss is assumed in the OH/EF energy transfer. This value is substantially larger than the losses explicitly identified in Chapter VI.

Figure III-26. Particle Densities and Refueling Current During Reference Burn Cycle.

When the beam is present the current can be generally much smaller. (Because the beam tends to change the deuterium fraction of the plasma, the deuterium composition of the refueling current can be lowered slightly to compensate; this is a minor perturbation.) As soon as the plasma reaches thermonuclear temperatures, at about 5 seconds, substantial fusion alpha heating takes place. (The alphas thermalize with the background plasma and about 99% are retained.) The He density therefore starts to build up, at nearly a linear rate. As the D, T, He, and neutrons strike the first wall, Be atoms sputter off and accumulate in the plasma. When the Be density reaches about 3% of the ion density, the self-sputtering by Be becomes significant. Since each He or Be ion brings 2 or 4 electrons into the plasma, the electron density (not shown) soon becomes significantly larger than the D-T ion density. Since the electrons, Be and He are about as hot as the D-T, they too contribute to the overall plasma kinetic pressure and hence to the β_p . Moreover, the fast alphas and injected beam particles contribute to the plasma β_p . At about 14 seconds the alpha contribution is about 8% of the total. Since the EPR is assumed to be pressure-limited, the D-T density must be reduced if the β_p limit is not to be violated. The assumed limit is $\beta_{p \text{ max}} = 1.72$, with about a $\pm 10\%$ deviation allowed. To reduce the ion density, the refueling current is lowered, and, in fact, for a brief period shut off altogether. By 24 seconds $\bar{n}_{DT}$ (and $\bar{n}_e$) has dropped sufficiently so that β_p is within safe bounds. Refueling is interrupted, and $\bar{n}_{DT}$ is held approximately constant thereafter. (The value then is $\bar{n}_{DT} = 0.807 \times 10^{20} \text{ m}^{-3}$.) The refueling current is also shut off at the very end of the cycle.

Temperature variations during the burn cycle are shown in Figure III-27. The ion and electron temperatures are 15 eV at the start of the cycle, i.e. just above the ionization potential for hydrogen. Because of the short equilibration times between electrons and ions, $\bar{T}_e$ and $\bar{T}_i$ are almost equal at all times. During the first second ohmic heating takes the plasma to about 60 eV. (This portion of the burn cycle is examined in greater detail in Section III-C.) When the beam is turned on, the plasma heats rapidly, at an average rate of 2.4 keV/s. When the beam is turned off, the plasma cools slightly, settling into an equilibrium by about 9 s.

Figure III-27. Ion and Electron Temperatures During Reference Burn Cycle.

Were it not for the subsequent buildup of He and Be, the plasma would remain indefinitely at this ignited equilibrium point.* Instead, the D-T density must be reduced to compensate the He and Be buildup, less alpha heating of the plasma occurs, and the plasma gradually cools off.

The plasma poloidal beta, β_p , is the ratio of the plasma kinetic pressure to the poloidal magnetic pressure. At very low plasma currents, e.g., $I_p \leq \frac{I_{p \text{ max}}}{10}$, a minimum value of $\frac{I_{p \text{ max}}}{10}$ has been used in computing β_p in order to avoid certain numerical problems. (This only applies to the periods 0 to 0.95 s and 53.7 to 55 s.) During the first second of beam heating, both the plasma current and the temperature increase. They tend to offset each other and are responsible for the oscillatory behavior of β_p shown in Figure III-22. At 2 seconds $I_p = 5.32$ MA, or about 70% of its final value. The magnetic pressure, which is proportional to I_p^2 , doubles by the end of the beam heating period, but the kinetic pressure increases by almost a factor of 5. β_p rises accordingly. The cutoff time for the neutral beam is keyed to β_p . When β_p reaches the nominal maximum of 1.72 the beam is turned off. Subsequently, β_p oscillates slowly for a number of reasons. The accumulation of He and Be tend to raise β_p because of their kinetic pressure. As the ion density is reduced, the plasma temperature falls, tending to reduce β_p . The actual behavior of the poloidal beta in a reactor will depend on the control scheme used and the accuracy and time constants of the plasma diagnostics. The control scheme used for this analysis (discussed in Appendix C) has been chosen on the basis of simplicity. Even so, the control scheme is successful in keeping the plasma within its pressure limit and in limiting excursions in β_p to about $\pm 5\%$ during most of the burn phase.

After about 40 sec, β_p decreases rapidly as the temperature drops. However, during the rampdown period, I_p drops very rapidly, causing an abrupt spike in β_p . Because of the uncertainties in the representation of this phase of the burn pulse and because of the numerical treatment of β_p at low currents, it can be considered that β_p could exceed β_p^{max} at the time of the spike. As a consequence, MHD equilibrium could be lost at this time. The plasma could then be assumed to deposit its remaining energy on the wall in a very short time. The shutdown period might present a problem in this regard. The plasma current should be ramped down

* Assuming that recycling and refueling take place.

at a time such that when (and if) MHD equilibrium is lost, the residual plasma energy is as low as possible. At the same time, the plasma must be hot enough (and ionized enough) to actually support a current; if the plasma is too cold, the current will decay resistively. In this analysis, this problem has been resolved by the judicious choice of rampdown rate. At the point where β_p peaks, only 4.5 MJ of thermal energy remains in the plasma, a trivial amount ($< 1.5\%$ of the peak thermal energy). The plasma current at the time of the β_p peak is about 0.9 MA. This also represents less than 1.5% of the maximum inductive energy and is probably inconsequential if lost. A more serious concern is the potentially high voltage transient that could be induced across the OH and EF power supplies by the sudden loss of plasma current.

The constituents of the plasma power balance are shown in Figures III-28 and III-29. The neutral beam power, P_B , is a square wave of 60 MW flattop. The ohmic power, P_Ω , is appreciable only early and late in the cycle when $\bar{T}_e$ is low and the plasma resistance is large. The neutron power, P_N , increases rapidly as the beam heats the plasma and fusion starts. The peak in P_N just prior to the beam termination is due to the substantial supra-thermal fusion between the fast beam deuterons and the background plasma tritons. During the burn, P_N oscillates a bit but basically decreases with time as both n_{DT} and T_{DT} decrease. Figure III-28 illustrates the deleterious effect of the He and Be buildup; P_N is only at its peak, design-point value a relatively short time.

The transport power, P_{TR} , arises from the convection and conduction of heat from the plasma to the first wall. During the burn P_{TR} falls off, partly because the plasma energy decreases, but primarily because the confinement times increase with the accumulation of He and Be. ($\tau \propto Z_{eff}$ in the trapped-ion-mode regime.) P_{TR} peaks during shutdown when confinement deteriorates and the plasma energy is transported to the wall.

The power radiated to the first wall, P_R , is almost entirely in the form of x-rays arising from bremsstrahlung, line and recombination processes, the latter type being important only at low temperatures (≤ 1 keV). There is a very small microwave component due to cyclotron radiation, which is almost entirely negligible at these temperatures. P_R tends to peak during the shutdown because of line and recombination radiation from Be and the 0.5%

Figure III-28. Power Components During Reference Burn Cycle.

Figure III-29. Power Components During Startup

oxygen background impurity. The peak is modest but could be quite large if higher Z materials were present, if for example an element like argon were injected to intentionally terminate the burn. The amount of argon would then have to be tailored to keep the radiation power loading on the first-wall acceptably low. Shut-down with argon injection is a design option.

Figure III-29 shows an expanded view of the various power terms during the startup period. Also shown is the alpha-heating power ($P_\alpha \sim 0.2 P_N$) and the ohmic heating ($P_\Omega = I_p^2 R_p$) power. The net power available to heat the plasma is given by the sum of the ohmic, beam and alpha components minus the radiation and transport components.

Ohmic heating increases at the start of the cycle as the plasma current is induced. When the beam is turned on, the electron temperature increases rapidly, R_p falls rapidly, and P_Ω decreases, in spite of the continued increase in I_p from 1-2 s. For the rest of the cycle P_Ω is insignificant (~ 0.5 MW) except for the shutdown period when the plasma temperature is again low. The contribution of ohmic heating to the total heating energy for the startup period is small; however, ohmic heating is very important in helping to overcome the large radiation peak at 1-1.3 s. This peak is due to line and recombination radiation arising from the 0.5% oxygen background of the plasma. The plasma temperature at ~ 1 s is 100-200 eV; this is the temperature range within which oxygen, in the O^{+6} state, contributes the most radiation. The net heating power at 1.1 s is only 2 MW; however, at only 0.1 s later, the net heating power has risen to 34 MW. It might appear that a smaller value of beam power would not be sufficient to heat the plasma past this point. For smaller P_B however, the plasma takes longer to reach the critical temperature. For $P_B = 20$ MW, for example, it takes 1.25 s. The plasma current is higher at this time and so P_Ω is higher; for $P_B = 20$ MW, $P_\Omega^{\max} = 53$ MW, also yielding a net heating power of about 2 MW. Of course there are conditions that might preclude reaching ignition with any given beam power, e.g. a higher oxygen concentration.

The transport power ($\propto \frac{\bar{T}}{\tau_E}$) varies directly with the plasma temperature and inversely with the (ion and electron) energy confinement times. As the plasma is heated the confinement times are determined* by successively less

* Neoclassical and anomalous energy confinement times are computed, and the lesser of the two is used in the calculation.

collisional transport regimes, each of which scales differently with temperature. The energy transport mechanism which dominates the power loss is indicated on the P_{TR} curve: PS denotes Pfirsch-Schluter, PL = plateau, BAN = banana and TIM = trapped-ion-mode. At very early times and low temperatures ($T_e < 10$ eV), there are several effects (e.g. energy reflection from the wall, sheath effects in a partially ionized plasma) which would tend to make the effective energy confinement better than the Pfirsch-Schluter or pseudo-classical formulas would predict. These effects are modeled by a linear (in temperature) enhancement of the confinement, relative to the theoretical expressions, below $T_e = 30$ eV, so that there is no enhancement at 30 eV, but a substantial enhancement at 15 eV. (This phase of the burn cycle is treated more explicitly in Section C of this chapter.) Thus, initially P_{TR} increases because of increasing temperature and decreasing confinement. As the current becomes significant, the confinement, which scales as I_p^2 , increases, and P_{TR} decreases. After ~ 1.5 s, the increase in temperature is more important than the decrease in confinement, and P_{TR} generally increases.

The energy components shown in Figure III-30 for the burn cycle are the integrals of the respective power curves, with the following exceptions: U_p is the plasma thermal energy and U_T is the total energy to the first wall ($U_T = U_N + U_{TR} + U_R$). The net electrical energy output of the reactor is a composite quantity defined as:

$$U_{net} = [U_T \eta_T - \frac{U_B}{\eta_B} - U_{REF} - U_{PUMP} - U_{\Omega} - |U_{PS}|(1-\eta_{PS}) - U_{OTHER}] + U_{F \text{ Profile } \eta_T} \quad (III-18)$$

where:

- U_T = thermal energy output of plasma
- U_F = thermonuclear energy output of plasma
- U_B = beam heating energy to plasma
- U_{Ω} = ohmic heating energy to plasma
- U_{REF} = refrigeration energy needed for a single burn pulse = 930 MJ
- U_{PUMP} = coolant pumping energy = $0.02 U_T$
- $|U_{PS}|$ = 2-way energy transferred by plasma driving system power supply =

$$\int_0^t (|P_{OH}| + |P_{EF}|) dt$$

Figure III-30. Cumulative Energy Components During Reference Burn Cycle.

$U_{\text{OTHER}} = \text{contingency allowance} = 260 \text{ MJ}$

$\eta_T = \text{thermal-to-electrical conversion efficiency} = 30\%$

$\eta_B = \text{neutral beam power efficiency} = 34\%$

$\eta_{PS} = \text{energy transfer efficiency} = 95\%$

$$\epsilon_{\text{Profile}} = 1 - \frac{\text{Fusion Power with Reference Profiles}}{\text{Fusion Power with Flat Profiles}} = 0.40$$

The burn cycle dynamics calculations are based upon a plasma power balance model in which the temperature and density profiles are flat ($\alpha_n = \alpha_T = 0.0$). It was shown in Table III-4 that such a model underpredicts the thermonuclear power output by $\sim 40\%$, relative to a power balance calculation in which the temperature and density profiles are treated explicitly. The term $U_{F \text{ profile}}$ is added to the net energy computation to account for this profile effect upon thermonuclear energy production. For the reference case, $U_{\text{net}} < 0$ for $t \leq 20 \text{ s}$, and $U_{\text{net}} = 2.09 \text{ GJ}_e$ at $t = 55 \text{ s}$.

The cycle averaged net electrical power is defined by:

$$\bar{P}_{\text{net}} = \frac{U_{\text{net}} \big|_{\text{end of cycle}}}{\text{cycle time}} = \frac{2.09 \text{ GJ}_e}{55 \text{ s}} = 38.0 \text{ MWe} \quad (\text{III-19})$$

where the cycle time is 55 s for the reference case. The gross electrical power, averaged over the cycle, is defined by:

$$\bar{P}_e = \frac{(U_T + U_{F \text{ Profile}} + U_{BR}) \big|_T}{\text{cycle time}} = 91.6 \text{ MWe} \quad (\text{III-20})$$

where U_{BR} is the energy thermally recovered from the neutral beam injection system.

5. Power Performance

The net energy flow for the reference case burn cycle is shown in Figure III-31a, and the energy balance is summarized in Table III-13, for the case with thermal energy conversion. A total of 16.34 GJ of energy is incident on the first wall; of this, 15.96 GJ is produced by fusion and 0.38 GJ results from beam and ohmic heating of the plasma. An additional 0.45 GJ of thermal energy is recovered from the beam injection system, so that the total thermal energy to the thermal conversion system is 16.79 GJ. If this energy is converted to electricity with an efficiency of 30%, then 5.04 GJ of electrical energy is produced in a burn pulse. Of this, 1.51 GJ are required to operate

14-III

Figure III-31a. Net Energy Flow in Reference Burn Cycle (with Thermal Energy Recovery)

Table III-13. Energy Balance -- Reference Case Burn Cycle

Thermonuclear energy produced		15.96 GJ
Energy input to plasma		0.38 GJ
Beam Injection	0.29 GJ	
Ohmic Heating	0.09 GJ	
Energy to first wall		16.34 GJ
Neutrons	78%	
Transport	13%	
Radiation	9%	
Thermal energy from injectors		0.45 GJ
Electrical energy produced ($\eta_T = 30\%$)		5.04 GJ
Electrical energy required		2.95 GJ
Makeup to Energy Storage	1.44 GJ	
Auxiliaries	1.51 GJ	
Net Electrical energy		2.09 GJ
Waste heat to environment		13.86 GJ
Auxiliaries	1.51 GJ	
OH/EF system	0.37 GJ	
NB direct conversion	0.23 GJ	
Thermal Conversion	11.75 GJ	
Waste heat rejected to environment, average over operating cycle		198 MW
Electrical power required to operate EPR, average over operating cycle		42.1 MW
Gross electrical power		
averaged over burn cycle, $\Delta t = 55$ s		91.6 MW
averaged over operating cycle, $\Delta t = 70$ s		72.0 MW
Net electrical power		
averaged over burn cycle, $\Delta t = 55$ s		38.0 MW
averaged over operating cycle, $\Delta t = 70$ s		29.9 MW

auxiliary systems (refrigeration system - 0.93 GJ; coolant pumping system - 0.33 GJ; other systems - 0.25 GJ) and 1.44 GJ are required to make up deficits in the energy storage system due to losses in the neutral beam injection system (0.98 GJ) and the OH/EF system (0.46 GJ). This leaves 2.09 GJ net energy which would be available for transfer to an electrical power grid. Averaged over the reference operating cycle - a series of 55 s burn cycles separated by 15 s replenishment intervals - this corresponds to 29.9 MWe supplied to the power grid. Waste heat rejected to the environment is 98 MW, averaged over the reference operating cycle.

Although thermal energy conversion at $\eta_T = 30\%$ is assumed for the sake of computing the power performance, the EPR may well operate without a thermal conversion cycle. In this case the entire energy requirements to operate the system would be drawn from the electrical the power grid and the thermal energy could be dumped to the environment. Only 2.95 GJ per burn cycle, or an average power of 42.1 MW, is required from power grid to operate the EPR in the reference operating cycle. In this case, 438 MW average power would be rejected to the environment. A net energy flow is shown in Figure III-31b.

For the reference case, the burn pulse is of 55 s duration. Both shorter and longer burn pulses are achievable by varying the mode of operation. Shorter cycles can be achieved either by injecting a high-Z impurity to "poison" the plasma or by not refueling during the burn. Longer cycles can be achieved by supplementing the fusion alpha heating with beam heating when the plasma begins to cool. The net electrical power achievable under these different operating conditions is plotted as a function of the length of the burn pulse in Figure III-32.

Injection of a high-Z material enhances the radiation loss and cools the plasma. Argon ($Z = 18$) is an appropriate gas in that it is easy to handle and a relatively small amount (1/4% of the DT density) is sufficient to quench the plasma in about 5 seconds. The curve labeled "reference case" in Figure III-32 indicates the performance which can be obtained if the reference case burn pulse is terminated at earlier times ($t < 55$ s). The minimum burn pulse duration for breakeven ($P_{Net} \geq 0$) is ~ 25 s. Generally,

Figure III-31b. Net Energy Flow in Reference Burn Cycle (without Thermal Energy Recovery)

Figure III-32. Power Performance as a Function of Operating Mode

$\bar{P}_{net}$ increases with burn pulse duration because some of the major energy deficits -- beam heating, refrigeration, plasma driving system energy-transfer losses -- are more-or-less independent of the burn pulse duration, while the gross energy produced increases with the duration of the burn pulse. Actually, quenching the burn pulse a few seconds before its natural self-quench eliminates the relatively cool and non-productive tail of the burn pulse, thereby increasing the power output averaged over the entire burn pulse.

On the other hand, if neutral beam heating is used to supplement the fusion alpha heating, the plasma temperature can be maintained and the productive duration of the burn pulse can be extended. In the simulations that are summarized by the curve labeled "supplemental beam" in Figure III-32 supplemental beam heating was employed to maintain the plasma temperature as high as possible without violation of the $\beta_p^{max} \leq 1.72$ limitation. The required beam power increases with time -- as the impurity radiation losses increase -- to a maximum value of 35 MW for a 95 s burn pulse. For shorter burn pulses the required beam power is substantially less -- e.g. an 80 s burn pulse requires a maximum of 15 MW of supplemental heating. An increase of as much as ~ 30% in $\bar{P}_{net}$, relative to the reference case, can be obtained by employing supplemental beam heating.

Adequate refueling to replace D and T ions which are burned in fusion or which fail to recycle (98% reflection is assumed) is assumed in the reference case. A breakeven ($P_{net} > 0$) burn pulse is predicted without refueling (but still assuming 98% reflection); however, the power performance and maximum burn pulse duration are significantly degraded, relative to the reference case.

The manner in which the startup is effected and the efficiency of energy transfer in driving the plasma current and in heating the plasma have an effect upon the power performance, as well as upon the requirements for the plasma driving and heating systems. The power performances of several cases, which differ only in the length of time, Δt_{OH} , in which the OH current is reversed and time, t_B , at which injection heating is initiated, are shown in Figure III-33. These cases correspond to cases 2, 5, 6 and 7 in Table III-11. Increasing the startup time has the obvious effect of displacing the burn pulse to a later time. However, the major effects of increasing the startup time are: (1) to increase the maximum energy transferred in the OH

Figure III-33. Power Performance as a Function of OH Current Reversal and Beam Turn On Times

system (to offset increased plasma resistive losses), thus to increase the energy losses in the OH system; and 2) to increase the beam heating energy required to offset the increased energy transport to the wall during the startup.

The injected beam heating power has a substantial effect upon the power performance, as shown in Figure III-34. The cases shown correspond to cases 5, 8, 9 and 10 in Table III-11. The time required to heat to operating temperature varies inversely with the beam power, but not linearly. The beam heating energy varies inversely with the beam power because of the larger energy transport losses associated with the slower startups. The energy transferred, hence the associated losses in the OH system varies inversely with the beam power because of the increased plasma resistive losses associated with the slower startups. Both of these energy variations tend to saturate for beam powers at and above the reference value of $P_B = 60$ MW. From Figure III-34, it is apparent that heating with $P_B = 40$ MW would result in only a slight degradation in performance, and that even $P_B = 20$ MW results in satisfactory values of $\bar{P}_{net}$. However, with $P_B = 20$ MW the minimum burn pulse duration required for breakeven is greatly increased. The longer burn pulse associated with $P_B = 20$ MW results from the delayed buildup of He. With a capability for $P_B = 60$ MW, the EPR heating system appears to have ample margin.

The efficiency with which the injected beam power is produced and the efficiency with which the energy transferred in and out of the OH and EF system during the burn pulse are both important in the determination of the net electrical power produced. Several representative cases are shown in Figure III-35. The reference case is based upon a beam power efficiency, η_B , of 34% (which assumes thermal energy recovery at 30% efficiency and electrical energy recovery at 85% efficiency in the injection system) and an energy transfer efficiency, η_{ps} , of 95% for the plasma driving systems. The use of D^- ion sources in injection heating or the use of RF heating could result in heating efficiencies near 60%, while $\eta_B = 25\%$ represents a possible consequence of using higher beam energy. Energy transfer efficiencies on the order of $\eta_{ps} = 85\%$ represent very pessimistic lower limits. It is apparent that although these efficiencies are important, they do not appear to be make-or-break quantities with regard to the EPR power performance. This situation would change if it were necessary

Figure III-34. Power Performance as a Function of Injected Beam Power

Figure III-33. Power Performance as a Function of Energy Transfer Efficiencies.

to constantly provide supplemental beam heating to compensate for subignition energy confinement.

Calculations up to this point have been based on the assumption of a fixed plasma edge temperature of 200 eV. This temperature affects the calculation in two ways. The sputtering coefficients depend upon the mean energy of particles leaving the plasma, hence upon the edge temperature. The charge-exchange and ionization rates depend on the edge temperature. These effects are discussed in Appendix B.

For beryllium, the maximum in the D-T sputtering yield occurs for mean particle energies in the range 200-400 eV, as shown in Appendix B. Thus, an edge temperature of 200 eV is a worst case with respect to the effect of wall-sputtered impurities upon the plasma performance. Operation with an edge temperature of 1 keV significantly improves P_{net} . Operation at an edge temperature of 60 eV more than doubles P_{net} and results in very long burn pulses. These results are shown in Figure III-36.

6. Plasma Heating

a. Neutral Beam Penetration

Recent experiments on ORMAK,¹⁷ ATC¹⁸ and TFR¹⁹ have established the feasibility of neutral beam injection heating of a plasma and have indicated that the theoretical understanding of the phenomenon is quantitatively adequate. However, substantive questions remain about the penetration and heating of large plasmas by neutral beams.

For the beam energies of interest, charge exchange accounts for less than 5% of the fast ion production; the remaining 95% of the fast ions being produced by ion impact and, to a minor extent, electron impact phenomena.^{20,21} The ion impact cross sections are functions of the nuclear charge of the plasma ion, scaling as Z^2 relative to the proton impact cross section.²⁰ The total "ionization" cross section is only weakly dependent on plasma temperature for the energies of interest, allowing the mean free path for attenuation of the neutral deuteron beam to be written

$$\lambda = 2.75 \times 10^{17} U_B (\text{keV}) / n_e (\text{m}^{-3}) \cdot Z_{eff}$$

This expression is valid for $40 \text{ keV} \leq U_B \leq 200 \text{ keV}$.

Figure III-36. Power Performance as a Function Plasma Edge Temperature

Analysis²² indicates that the ion deposition profile will be concentrated at the center of a plasma of radius a when $\lambda \geq a/4$. This criterion is probably valid for perpendicular as well as tangential injection. Using this criterion, the required beam energies for the EPR are in the range ~ 150 - 250 keV, depending upon Z_{eff} and the electron density corresponding to the different cases. A deuteron energy of $U_B = 180$ keV has been chosen as the reference value.

Numerical studies of beam penetration into the EPR plasma have been carried out to supplement the above analysis. These studies simulated the injection of a pencil beam, consisting of only the highest energy component, into a plasma with an assumed radial density distribution comparable to the reference design case. Power deposition profiles per unit volume and per plasma ion are shown in Figures III-37 and III-38, respectively.

The curves labeled 1 and 2 correspond to a relatively broad ($\alpha_n = 0.3$) and a parabolic ($\alpha_n = 1$) density profile, respectively, a deuteron energy of 180 keV, $Z_{eff} = 1.3$ and a plasma density comparable to the reference design case at $B_{max}^{TFC} = 10$ T. The power deposition per unit volume is relatively uniform, excepting the geometrical peaking about the plasma axis at $r = 0$. However, the power deposition per plasma ion increases with radius, again excepting the geometrical peaking at $r = 0$; this implies preferential heating of ions in the outer regions of the plasma, which could reduce the effectiveness of the beam in heating the bulk of the plasma and would tend to broaden the temperature profiles, possibly even resulting in an inverted profile. The consequences of these power deposition profiles with regard to plasma heating and temperature profiles depend upon the heat transport mechanisms within the plasma.

The power deposition profiles, both on a per unit volume and a per plasma ion basis, can be shifted toward the center of the plasma by reducing the plasma density. Curve 3 in Figures III-37 and III-38 was obtained by reducing the average plasma density to one-half the value used to obtain curve 1. The reduced density is comparable to the reference case at $B_{max}^{TFC} = 8$ T. A reduced density startup, in which the plasma is heated to ignition temperatures at reduced density, and then additional fuel is injected, is also a possibility.

Figure III-37. Power Deposition Profile, Per Unit Volume.

Figure III-38. Power Deposition Profile, Per Plasma Ion.

Increasing the deuteron energy is another way of shifting the power deposition profile towards the center of the plasma, if this should prove to be necessary. Curves 4-6 in Figures 111-37 and 111-38 correspond to curves 1-3, except that the deuteron energy is 300 keV. Of course, increasing the deuteron energy from 180 to 300 keV results in a substantial decrease in injection system power efficiency (see Chapter V) when positive ion sources are used.

b. RF Energy Absorption

In rf heating, energy is imparted to the plasma by means of high frequency electromagnetic radiation. The basic processes involved are the initial generation of the rf energy, transmission from the source of an appropriate launching structure, and absorption of energy by the plasma. The most likely frequency bands for rf heating in the EPR are the electron cyclotron resonance ($f = 100$ GHz), where the rf power is used to accelerate electrons perpendicularly to the magnetic field; the ion cyclotron resonance ($f = 25$ MHz), where ions are accelerated the same way; and the lower hybrid resonance ($f = 1$ GHz), where collective plasma oscillations are excited which ultimately transfer energy to the plasma particles. Although rf heating of plasmas has been tried for many years with some success, it is only recently that high power experiments have been planned or attempted on tokamaks.

At the lower hybrid frequency, experiments on the ATC device have demonstrated effective coupling to the plasma at the 200 kW level.²³ Ions were heated more than electrons, however. Experiments on Alcator using a single, grill-type element achieved up to 80% absorption without any deleterious effects on the plasma.²⁴ Edge heating was dominant in these experiments. For EPR size devices the central question for lower hybrid heating concerns possible nonlinear effects that may prevent penetration of the wave into the bulk plasma.

Experiments on rf heating at the ion cyclotron frequency also seem promising. Results from the ST tokamak at the 1 MW level showed that power could be produced at 90% efficiency, and twenty per cent of this power was ultimately absorbed by the ions, raising their temperature several hundred eV.^{25,26} Recent experiments in the ATC also indicate successful heating of ions. A larger machine should have higher absorption efficiency, because

surface effects should be less severe, and confinement of high energy ions should be better.

Large, high power (5 MW) experiments with ion cyclotron and lower hybrid resonance heating are now planned for PLT and PDX. The former results should be available by 1979-1980.

Although generation and transmission of rf power are well understood below 10 GHz, above this frequency high power tubes have not yet been available. Recently, Varian²⁷ has surveyed the possibility of producing tubes with frequencies in the ECRH range ($f = 100$ GHz) and has concluded that 100-200 kW CW operation is possible with efficiencies of up to 30%. No devices have yet been produced, however, and no high power experiments will be possible until these tubes become available.

7. Plasma Refueling and Recycling

a. Requirements

Based on the analysis of possible operating modes for the EPR it appears essential to refuel the plasma during the burn pulse; to do otherwise results in a truncated burn time and severely reduced output power, as shown in Figure III-32. An external refueling source can also serve as an effective control mechanism. The amount of refueling needed is equal to the difference between the gross particle loss rate of the plasma and the natural recycling that may occur. The combination of recycling plus refueling can be thought of as the overall "replenishment" rate.

Ions are lost from the plasma by burnup from fusion and by transport to the wall. Both burnup and transport losses tend to peak near the start of the burn and diminish thereafter as the plasma cools. For the reference case, neglecting the beam heating period itself and the initial transient thereafter, the peak loss occurs at 8.0 seconds. At this time, the burnup rate is 0.45% of the total D-T ions per second and the transport loss is 20% per second. (The fractional burnup, defined as the ratio of burnup loss to transport loss, is therefore 0.0225.) At 40 seconds into the cycle, when the plasma has cooled off considerably, the burnup rate has decreased to 0.29% per second, and the transport rate to 7.2% per second.

In the reference case analysis it was assumed that all of the ions reflected from the wall were recycled into the plasma. The particle reflection coefficient for D-T, R_{DT} , was taken as 0.98, but because of neutral charge-exchange phenomena, the effective recycling is 0.966 (see Equation C-4). This means that 96.6% of the D-T particles leaving the plasma will be recycled; in other words for the data at 8 seconds, the recycling rate is 19.3% per second. The refueling requirements would therefore be $20.4 - 19.3 = 1.1\%$ per second. For different reflection coefficients the recycling fraction scales accordingly.

It should be noted that the concept of a single reflection coefficient really represents the composite of several phenomena occurring at the wall: re-emission, desorption, backscattering, etc. The exact value of the reflection coefficient, R_{DT} , is uncertain, but it is believed to be very close to unity. For a value of R_{DT} exactly equal to unity, the required refueling rate would be equal to the burnup rate. If R_{DT} were as low as 0.9, the refueling rate would be 2.5% per second. Maximum refueling and replenishment rates for the reference case are shown in Table III-14.

Table III-14. Refueling and Replenishment Rates-Reference Case

	<u>Ions/s</u>
Replenishment Rate (Total influx required to balance confinement and burn up losses)	9.57×10^{21}
Refueling Rate (Replenishment rate minus wall recycling)	5.60×10^{20}

Clearly, the question of recycling is a fundamental one, not only in determining the amount of external refueling, but in determining whether the plasma will perform acceptably. The DT ions will be reflected at the wall, and there is no mechanism for removing them from the vacuum chamber in the EPR design. Since the plasma acts as an effective pump for particles in the vacuum chamber, it seems reasonable to assume that the reflected D-T will return to the outer region of the plasma, as it seems to do in

those tokamak experiments in which no special measures are taken to inhibit recycling. The relevant question is whether the recycled D-T will diffuse back into the hot plasma core or accumulate in the cool edge region. In fact, if there is no recycling into the hot core, it is difficult to visualize how the EPR could be operated for more than a few particle confinement times ($\tau_p \sim 5-10$ s), since the buildup of DT density in the edge regions would quickly be many times the bulk density.

A preliminary analysis of this important topic has been performed using a 2-node model described in Appendix C. This model treats the plasma as composed of two regions, a hot central core, surrounded by a thin, relatively cool, corona. The core is coupled to the corona by means of energy and particle transport. The corona interacts with the wall through energy and particle transport, wall reflection and sputtering, and charge-exchange of incoming neutrals.

A series of calculations have been performed for plasma and wall conditions typical of the EPR reference design case. The results to date, while still preliminary, indicate that effective recycling will occur. It appears that the corona may act as a kind of series connection between the core and the wall, considerably reducing the particle transport from the core. The corona density is maintained by recycling. In effect, the particle confinement time of the plasma core is much larger than the simpler, global analysis would indicate. (The energy confinement times are largely unaffected.) Typical effective confinement times for the core are in the range 50-300 seconds, depending on the specific parameters used.

A parallel result from the 2-node analysis is that the core density can be maintained virtually constant by providing an appropriate refueling current to the corona only. The implication of this result is that refueling can be accomplished by merely pulsing D-T gas in at the outer edge of the plasma.

These results, although preliminary, are encouraging. However, it is certainly not possible to conclude at this time that the EPR can operate without some mechanism for injecting fuel into the central core region of the thermonuclear plasma.

b. Pellet Injection

Considerable theoretical and experimental effort has been directed recently at the method of pellet injection. The range of D-T ice pellets has been estimated by Tonks,²⁸ Gralnick,²⁹ Rose,³⁰ and General Atomic³¹ for thermonuclear plasmas. Although there have been others, the most applicable experiment was done on ORMAK by Foster, Colchin, Hendricks, and Turnbull.^{32,33} In this experiment, pellets 70 μ in diameter were successfully injected into the ORMAK plasma. The observed range seemed to agree well with theoretical estimates.

The range of a particle in a thermonuclear plasma goes roughly like $r \sqrt{V/T_e}$; thus, the highest injection velocities and radii are desired. Two methods seem most appropriate. The simplest is release by a centrifuge at high velocities. The highest obtainable speed of such a device is in the range of $3-5 \times 10^3$ m/s, using specially developed materials.³⁴ Laser ablation of particles has also achieved some success in acceleration of ice fragments. In this method, a slow (50 ns) laser pulse is used to vaporize one side of an ice pellet. The remaining part recoils at high velocities.³⁵ Existing ablation techniques tend to fragment the pellet, making these techniques unsuitable for pellet fueling.

In order to achieve a range of 1-2 m with a pellet velocity of 5×10^3 m/s, theoretical estimates^{28,29,30} indicate that the pellet radius must be about 2 mm. (The General Atomic model predicts that a larger radius would be required.³¹) Pellets of this size would have to be injected at a rate of 6/s to satisfy the replenishment requirement shown in Table III-14. When recycling at the wall is considered, the pellet injection frequency would be only 0.35/s to meet the refueling requirement. Replacing the D-T mixture burned as fuel should require only about two pellets per burn cycle.

In the ORMAK experiment the pellets were formed by a jet of liquid H₂. This method of pellet production produces small pellets at fairly low velocities (10^2 m/s) and is difficult to scale up to the EPR requirements. A more realistic pellet injector might involve a centrifuge spinning at high speed in a vacuum with apparatus at the tip to produce D-T ice droplets. The mechanical problems involved in doing this would be considerable, as the velocities required would be 10-20 times higher than can be reached

with a solid rotor of steel or aluminum. Gas guns have also been studied;³¹ however, forming, accelerating and guiding a pellet through a gun at high velocities are also difficult mechanical problems, and solutions have not yet been found.

c. Other Methods

Since neutral beams are comparatively inefficient and difficult to build in the sizes required, the other alternatives seem to be methods which refuel the plasma at its edges. The simplest is, of course, the injection of DT gas. Another is the cold gas blanket; however, the effects of a high neutral pressure on the tokamak plasma are not fully understood.³⁶ In order to circumvent this problem, the use of plasma guns has also been studied. The net effect of plasma gun refueling (assuming impurities can be eliminated) would be similar to that of a cold gas blanket; however, the neutral gas pressure should be much lower.

The primary objection to the plasma gun is that the impurity fraction of existing guns is high. This is primarily due to the fact that the plasma is produced by means of an arc between two metal electrodes. If the plasma can be produced away from solid material by means of focused rf beams, then the contamination due to solid impurities should be negligible. The plasma could be guided from its point of production to the tokamak plasma by means of dc electric fields. The required electric force is $E = B_t v$, where B_t is the toroidal magnetic field, and v is the desired pellet velocity. As shown by Baker and Hammel,³⁷ plasma should move through the toroidal field of the tokamak until it reaches a point where the electric field can be shorted. This would happen at the plasma surface.

Estimates have been made of the required number and size of plasma guns, assuming guns can be built that can produce 4×10^{19} ions/s with low impurities. (Existing guns can now produce 6×10^{19} ions/s in 0.1 ms pulses, with a fairly high impurity content.) Only 15 of these would be required to replace the plasma lost ($\tau_p = 4$ s, 96.6% reflection), but if the wall reflectivity was neglected, then the number of guns would increase to 30.

C. Plasma Initiation

1. Normal Initiation Mode

The quasi-stable plasma in a normal tokamak is produced by a toroidal electric field. This field serves two purposes: the plasma is created by a Townsend avalanche process and plasma current is induced. The plasma current ohmically heats the plasma and creates the rotational transform. The time development of an EPR plasma has been studied at early times by means of a model described in Appendix C, part 6. A typical set of results from the calculation is shown in Figure III-39. In this model, the plasma is considered as an inductance in series with a resistance. Breakdown is assumed to occur at $R = 6.25$ m.

The driving voltage, V_{Loop} , produced by changing flux in the ohmic heating or startup coil, rises in about 2 ms to about 550 V and stays at this voltage for ~ 6 ms. In the first 1.2 ms, the electron avalanche converts essentially all the neutral gas in the tank to plasma. As soon as the density of electrons is sufficiently high, the current will start to rise and the power produced by ohmic heating will also rise. When the plasma heats up, the resistance drops and it becomes possible to maintain a current with reduced voltage. During most of this time, the principal source of power loss is ionization and re-ionization of plasma particles. Impurities can, however, produce a significant loss of power, as has been pointed out elsewhere.³⁸

The primary problem of startup in large reactors is the constraint imposed on the current density by the MHD stability condition $q = rB_t/RB_p \geq 2.5$. As pointed out by Hawryluk and Schmidt,³⁸ the maximum allowed average current density $J = B_t/(2 q \mu_0 R)$ decreases with reactor size, thus the maximum ohmic heating power

$$P_{\text{OH}} = \eta \bar{J}^2 = \eta \left[\frac{B_t}{2 q \mu_0 R} \right]^2 \quad (\text{III-21})$$

would be much less than in existing devices. For a large device like the EPR, this can impose serious problems. For example, since P_{OH} is proportional to $(B_t/R)^2$, the maximum power density at the outside edge of the plasma tank is only about one-tenth of the power available at the inside

Figure III-39. Plasma Breakdown — Normal Start-up Cycle

edge of the tank. Since the power level required to create a discharge is dependent on factors like impurity levels, it is not obvious that it will be possible to develop enough power to start the plasma under all conditions. The maximum current, current densities, and P_{OH} values for different values of q , R , and a are shown in Figure III-40.

In the example of Figure III-39, the minor radius of the plasma was held to a constant 0.35 m during breakdown. This comparatively small radius was chosen to avoid the problem of skin currents. These skin currents may prevent the applied field from penetrating into the center of a large plasma, inhibiting the development of the desired current distribution $J(r)$. The best solution seems to be to start with a small plasma and increase the plasma radius and current simultaneously, essentially by adding current to the outside edge of the discharge.³⁹

There are difficulties, however, in producing a plasma with a small radius in a large vacuum tank. The primary problem is that the plasma will tend to ionize, hence trap, any neutral atoms which penetrate its surface. This process will tend to pump all the residual gas in the torus into the plasma and increase the density by a factor of $(Vol_{\text{tank}}/Vol_{\text{discharge}})$. Although this problem has not been serious with existing devices where the plasma occupies most of the vacuum chamber, the dimensions of the EPR will again introduce difficulties. Assuming a plasma radius of 0.24 m (comparable to what is planned in the initial stages of the TFTR discharge) in a vacuum tank 2.4 m in minor radius and an initial plasma density of $n_e = 2 \times 10^{19} \text{ m}^{-3}$, the neutral density in the tank must be $2 \times 10^{19} \times (1/100) = 2 \times 10^{17} \text{ atoms/m}^3$, or $0.6 \times 10^{-5} \text{ Torr}$. If the residual gas were pure D-T mixture, there would be no problem in using it; however, this gas would be expected to contain enhanced fractions of impurities, particularly helium, which will not pump out as fast as hydrogen. One would like to dilute these impurities with pure gas.

Prolonged running at small radii and low currents should be avoided because the plasma confinement is poor under these conditions, and the startup becomes very inefficient.

As shown in Figure III-39, oxygen impurity radiation losses are very large at about $T_e = 50 \text{ V}$, causing enhanced power losses at about 50 eV. These

Figure III-40. Plasma Breakdown — Sensitivity

should probably be minimized by raising the electron temperature above 100 eV as soon as possible.

While plasma power losses are minimized by starting up the plasma current quickly, the large voltage required to do this could cause runaway electrons. These runaways are electrons which freely accelerate around the torus unimpeded by collisions with other electrons, since the collision frequency drops as $E_e^{-3/2}$, where E_e is the energy of the electron. Runaway production is strongly related to the ratio $\vec{E}/2\vec{E}_c$, where $\vec{E}_c = 2 \times 10^{-19} \times n_e/T_e$ is the Dreicer runaway field.⁴⁹ Since $E = \eta J$, runaway production drops when $E/T_e/n_e$ decreases. Since T_e and n_e are essentially constant at breakdown, runaway production can be minimized by making E small at the end of the avalanche process, when it seems most runaways are produced.

It has been assumed in this discussion that the plasma current will be created by a special startup coil, separate from the ohmic heating coil, because the time constants and power levels of this coil are different from the OH coil. A preliminary design of this coil is discussed in Chapter IV. At the end of the breakdown phase, however, the total resistive part of the loop voltage, $I_p R_p$, will have to be comparable to the driving voltage produced by the ohmic heating coil, which rises comparatively slowly from zero.

Calculations have shown that a discharge with $T_e \sim 130$ eV, $I_p = 115$ kA, $a = 0.36$ m can be maintained at constant current with a voltage $V_{Loop} = 28$ V. Ohmic heating continues to raise the plasma temperature and lower the resistance, so the voltage requirement drops with time. It is anticipated that a sinusoidal ohmic heating pulse with a half period of two seconds should produce 25-30 V after about 0.3 s, thus plasma initiation should occur at this time. After the plasma is started and the ohmic heating and equilibrium fields provide the driving voltage, it will be possible to increase the minor radius, density and temperature. The contribution to the driving voltage, V_{Loop} , due to the initiation, OH and EF coils is shown in Figure III-41. Starting the plasma at 0.3 s wastes only about 4 V-s of the OH inductive flux.

2. Alternatives

In the event that these conflicting constraints prove to be difficult to satisfy, or if the construction of a breakdown coil proves difficult, it

Figure III-41. Voltages During Startup

should be possible to use auxiliary heating to help with the startup process. Experimentally, electron cyclotron resonance heating (ECRH) seems the most convenient form of auxiliary power, since the total power required is not too large, but it is also possible that heating at the lower hybrid frequency would be useful. Neutral beams would be less desirable, because ions would be heated rather than electrons. The net effect of these forms of auxiliary heating is shown in Figure III-42. By raising the electron temperature, the confinement time will be improved and the resistive voltage $I_p R_p$ will be decreased, enabling the plasma to develop with a much lower driving voltage. By increasing the amount of auxiliary power, it is possible to eliminate much of the resistive fraction of the driving voltage, $V_{Loop}(t)$. A continuous range of possibilities can be achieved by applying different levels of power at different times, with or without a reduction in applied loop voltage, V_{Loop} .

It might also be possible to eliminate that part of the voltage spike needed for the initial electron avalanche if plasma can be introduced into the tank by some other means, such as rf ionization or plasma guns. When voltage is required only to induce plasma current, less is required; and if the power losses due to poor confinement were replaced by auxiliary heating, it may be possible to use the long time constant OH coil entirely, and dispense with the breakdown coil. A low plasma density is preferable in this mode of startup, because less power is required to maintain a plasma at low densities, and the confinement time against ∇B induced drifts is longer.

Estimates have been made using the model of Appendix C, part 6, neglecting impurity losses and assuming all plasma is injected in a fully ionized state. The results show that in about 50 ms, a plasma comparable to those in existing tokamaks could be produced. Because of limited experimental data on plasmas of this density, however, it is difficult to calculate very accurately the rf power levels required to do this.

One of the principal objections to these alternative methods of startup is that the runaway electron problem could be very troublesome, but there are two reasons why the runaways may be less serious than might be thought. The first is that the parameters T_e and n_e can be controlled in a way that runaway production can be minimized. Runaway production is a function of

Figure III-42. Plasma Breakdown — Alternate Start up Cycle

the electric field divided by the critical field⁴⁰

$$\vec{E}/\vec{E}_c \propto I_p R_p / (4 \cdot 10^{19} n_e / T_e)$$

$$\propto I_p / T_e^{1/2} n_e$$

since $R_p \propto T_e^{-3/2}$. By keeping the electron density and temperature up, it might be possible to keep runaway production down. The second reason runaways may be less of a problem is that the small electric fields which will be present in this mode of startup may prevent runaways from being confined. It has been found that runaway production is very sensitive to ripple and errors in the toroidal field, B_t . Tokamaks with more ripple tend to have fewer runaways, and runaways can be eliminated from a discharge by artificially inducing a perturbation in the toroidal field.⁴¹ Work in plasma betatrons has also indicated that the rate of acceleration of electrons may be important, and that slow acceleration ($E = \text{small}$) may cause enhanced resonance blowup of the relativistic beam.⁴² By keeping the E field small and perhaps artificially enhancing the ripple in B_t , it may be possible to maximize losses of relativistic electrons.

3. Requirements for Initiation of Discharge

The total and resistive volt-second requirements for normal breakdown (without additional power) have been calculated for a number of conditions. The result depends on the assumed voltage profile, $V_{\text{Loop}}(t)$; the plasma major and minor radius; impurity levels; anomalous resistivity factor, γ ; and the desired final temperature and current, among other things. The results for a reasonable range of input parameters show a total of about 4 ± 1 V-s are required to produce a 100 kA, 100 eV plasma with a radius of 0.3 m at 10 ms, when the initial plasma is less than 2% oxygen. Of this, 1.2 ± 0.2 V-s are resistive. Additional power input will lower both the total and resistive volt-second requirements roughly equally. Because of constant loss terms, volt-seconds requirements increase with pulse length.

If the field during breakdown has a component parallel to the major axis of the torus, the plasma particles will spiral along the field lines into the wall. In order to eliminate this problem and to localize the discharge in the large vacuum tank, it will be desirable to have a poloidal

multipole null in the vacuum tank at the moment of breakdown. It will also be necessary to have coils inside the blanket and shield which can provide an equilibrium field to stabilize the plasma in the initial 10-50 ms. Because of the rapid changes in plasma current, density, and position, it would probably be best to have these coils fairly near the first wall. The time constant for penetration of the field from the EF coil will probably not be fast enough to handle the breakdown stage. It may be possible to have the same coils serve the combined purpose of breakdown, multipole null, and equilibrium fields for startup. These coils could also be a trim control on plasma position for hot plasmas. The design of this system of coils will be discussed in more detail in Chapter IV, Section B.

The pressure of neutral gas is somewhat critical, and in order to assure that a discharge can be composed primarily of new gas, whose composition can be controlled, it would be best to keep the initial neutral pressure at or below 10^{-5} Torr. Impurities are a critical part of this problem since the residual gas could contain $\sim 8\%$ He and similar Be impurity concentrations from sputtering. It will be important to remove as high a fraction of these impurities as possible. The dependence of the volt-second requirements on the density of an oxygen impurity is shown in Figure III-43. The impurity loss is proportional to $n_e n_I$, so that impurity losses are reduced by reducing the plasma density.

In the initial stages of operation, and after every time that the vacuum chamber is let up to air, it will probably be necessary to do considerable discharge cleaning. Because of the higher current densities possible, it may be most convenient to do this where the major radius R is as small as possible. After a few hot ($T_e > 5$ keV) burns, however, one would expect that the influx of impurities adsorbed to the walls would be negligible.

After thermonuclear burns, considerable helium will be present in the vacuum tank. It is anticipated that the helium fraction will be as high as 8-10% of the D-T mixture. Because cryosorption pumps will not pump He as fast as deuterium and tritium, this fraction may rise to about 30% during the 10-12 sec pumpdown time between pulses. The losses from this helium fraction should be less than the impurity loss from 1% oxygen, and this should not be serious. The principal impurity in a burn should be either

Figure III-43. Effect of Oxygen Impurity on Volt-Second Requirements
 $(I_p = 100 \text{ kA}, T_e = 100 \text{ eV}, R = 6.25 \text{ m and } a = 0.36)$

helium, beryllium or oxygen; losses from beryllium should be comparable to those from helium, and both are less serious than 1% oxygen.

As shown in a previous section, the current, current density, ohmic heating power levels, and inductive volt-seconds required to produce a plasma depend strongly on the major and minor radii of the desired plasma. The reference case discussed in this section ($R = 6.25$ m, $a = 0.36$ m), and many other possible modes would require some sort of movable limiter system which could be inserted into the vacuum tank. If the outside edge of the first wall could be used as a limiter, things would be simpler mechanically, but a penalty would be paid in the available ohmic heating power density.

Preionization would also be required from some source. A number of different methods would be acceptable, the only constraint seems to be that better preionization has led to decreases in runaway flux in some experiments.⁴³ Fast acting valves will also be necessary for adding neutral gas both to start the discharge and, during ohmic heating, to increase the volume and density of the plasma.

D. Research Requirements

The EPR represents a significant extension of plasma operating conditions beyond what has been achieved to date. The current generation of experiments (PLT, T-10, D-III, PDX) will supplement the existing data from ATC, ORMAK, TFR, etc., to provide, together with the results of a vigorous theoretical program, the plasma physics design basis for an EPR which operated in the mid-1980s. If operation of EPR is later, additional results from TFTR, JET, JT-60, etc. will contribute to the design basis. Several specific questions relevant to the EPR are discussed in this chapter within this context.

1. Confinement

The size of the EPR is set by power output requirements. Thus, the principal consequence of the uncertainty in energy confinement is the uncertainty that the reactor can operate at or near net electrical power production (i.e. that the plasma will be at or near ignition). Since supplementary neutral beam heating is to be provided against the eventuality that subignition energy confinement obtains, operation at meaningful power levels is not jeopardized by the uncertainty in confinement, providing that $\eta_{TE} \gtrsim 2 \times 10^{19}$ s/m³. Assurance that this level of confinement can be

achieved should be provided by the current generation of experiments (PLT, T-10, D-III, PDX). Information from the next generation of experiments (JET, TFTR, JT-60, TFTR-UG) should serve to further reduce the uncertainty in confinement and to establish more firmly the injection power requirements for the EPR.

Control of the reactor (e.g. during startup and shutdown and to suppress thermal instabilities) depends upon how the energy confinement scales with temperature and density. The current generation of experiments should establish if the anomalous TEM and TIM scaling will obtain.

If recycling of D and T occurs in the EPR, as it appears to in many current experiments, then the particle confinement is important for the determination of the plasma particle flux to the wall but is relatively unimportant otherwise.

2. MHD Equilibria and Stability

The power output of the EPR is very dependent upon the type of stable equilibria which can be obtained. "Net power" EPR operation is predicated upon equilibria characterized by $\beta_p \sim 1.7$ and $q(a) \sim 3.0$, although reasonable levels of power output are possible for $\beta_p < 1.7$ or $q(a) > 3$. PLT, with injection heating, should provide some further evidence of the attainability of the EPR design values.

Noncircular configurations are of considerable interest because of the promise they offer to operate at higher β , hence higher power output. The EPR design carries along a noncircular option with plasma height-to-width ratios up to 1.5, which seems to be about the upper limit that can be obtained without placing field shaping coils in close to the plasma. Investigation of noncircular plasma configurations in this range on PDX and D-III should establish the stability of noncircular configurations and verify that the improved β -performance obtains.

3. Impurity Control

Attainment of a burn pulse longer than a few seconds requires some means of impurity control. Realization of "net power" conditions requires, in addition to ignition or near ignition confinement, burn pulses of at least $\sim 1/2$ min.

The primary means of impurity control envisioned for the EPR is a $\sim 100 \mu\text{m}$ low-Z (probably beryllium) coating of the first wall. The integrity of the coating can be tested in the laboratory, in materials test facilities, under limited conditions in PLT, D-III, and PDX, and in a more realistic environment in TFTR.

A better understanding of plasma-wall interaction phenomena is required. The effect of the wall on the plasma-edge region should be investigated. Reflection, re-emission, sputtering, absorption, desorption, blistering, and other surface properties of the coated first-wall are essential to determine plasma contamination, wall erosion and recycling for D, T, He, e, n, and photons. More precise data for the radiation power loss from partially-stripped impurity ions are required. Experiments should be performed in PLT, D-III, and PDX to determine if impurities diffuse preferentially towards the center of the plasma when (and if) the transport is nonclassical, as has been observed recently on ATC and previously in the USSR, when the transport was "neoclassical."

4. Heating

The primary supplementary plasma heating means currently envisioned for the EPR is neutral beam injection. The feasibility of substantially raising the ion temperature by neutral beam injection has recently been established on ORMAK, ATC, and TFR, where the energy transfer seemed to obey "classical" laws. Injection experiments on PLT, D-III, and PDX should further establish the plasma physics feasibility of neutral beam heating of plasmas.

Experiments should be carried out in PLT, D-III, and PDX to determine how much penetration of the plasma by the beam is required to heat the plasma satisfactorily and to determine how the presence of impurities affects the beam energy that is required to obtain the requisite penetration and heating. It is important that such experiments be performed on plasmas which exhibit the type of energy transport phenomena expected in the EPR.

RF heating in the lower hybrid, ion cyclotron, or electron cyclotron mode is considered as a backup method for the EPR. Experimental results to date are inconclusive about plasma energy absorption and heating by rf means. Experiments should be performed in plasmas with the relevant characteristic resonances and with launcher characteristics as similar as possible to those for EPR to establish the plasma physics feasibility of rf heating.

5. Refueling

The requirement for refueling is linked to the question of recycling of D and T. If substantial ($\sim 100\%$) recycling obtains, as appears to be the case in some previous experiments, then only a relatively small amount of refueling is required to compensate for burnup (fractional burnup 1-2%). This amount could possibly just be released into the toroidal chamber and would be pumped into the plasma in the same manner as are the recycled D and T.

If the D and T do not recycle appreciably, then refueling is necessary if the particle confinement time is less than a few minutes, as is likely if TIM or TEM confinement scaling obtains. Pellet injection experiments are currently in progress on ORMAK, and it should be possible to establish the plasma physics feasibility and limitations of this method in the next few years. Injection of plasma with a "clean" plasma gun is another possibility which should be examined on PLT, D-III, and/or PDX.

6. Plasma Initiation

The voltage requirements for initial breakdown in an EPR-size torus (~ 40 m around the magnetic axis) are quite large. A better understanding of the phenomena (e.g. filling pressure, wall outgassing) affecting these requirements is important. Alternate means of achieving the breakdown (e.g. injection of high-purity plasma with guns, rf discharges, lasers) should be investigated. Effects (e.g. skin current) which might hinder the establishment of the proper current profile should be better understood. Runaway electrons also require further study, as their production and behavior is very sensitive to plasma parameters.

1. L. A. Artsimivoch, "Tokamak Devices," *Nucl. Fusion*, 12, 215 (1972).
2. S. O. Dean, et al., "Status and Objectives of Tokamak Systems for Fusion Research," WASH-1295 (1974).
3. H. P. Furth, "Tokamak Research," *Nucl. Fusion*, 12, 487 (1975).

4. J. D. Callen and R. A. Dory, "Magnetohydrodynamic Equilibria in Sharply Curved Axisymmetric Devices," *Phys. Fluids*, 15, 1523 (1972).
5. V. S. Mukhovatov and V. D. Shafranov, "Plasma Equilibrium in a Tokamak," *Nucl. Fusion*, 11, 605 (1971).
6. V. S. Vlasenkov, et al., "Magnetic Field Perturbations During a Tearing Instability in the Tokamak T-6 Device," Proc. Fifth Conf. on Plasma Physics and Controlled Nuclear Fusion Research, Tokyo, Paper A2-1 (1974).
7. W. M. Stacey, Jr., et al., "Tokamak Experimental Power Reactor Studies," ANL/CTR-75-2, Argonne National Laboratory, (August, 1975).
8. G. Eder and H. Motz, "Contribution of High-Energy Particles to Thermonuclear Reaction Rates," *Nature*, 182, 1140 (1958).
9. C. F. Wandel, et al., "A Compilation of Some Rates and Cross Sections of Interest in Controlled Thermonuclear Research," *J. Nucl. Inst. and Meth.* 4, 249 (1959).
10. D. J. Rose, "On The Feasibility of Power by Nuclear Fusion," ORNL-TM-2204 (1968).
11. L. G. Kuo-Petravic, et al., "Alpha Particle Heating and the Energy Balance in a Mirror Reactor," Proc. British Nucl. Energy Soc., Nuclear Fusion Reactor Conf., p. 144 (1969).
12. D. G. McAlees, "Alpha Particle Energetics and Neutral Beam Heating in Tokamak Plasmas," Oak Ridge National Laboratory Report ORNL-TM-4661 (1974).
13. J. A. Anderson and H. P. Furth, "Imperfect Axisymmetry in the Tokamak Configuration," *Nucl. Fusion*, 12, 207 (1972).
14. T. F. Yang and G. A. Emmert, "Energetic Particle Loss and Field Bumpiness Due to "D" Shaped Coils in a Tokamak Reactor," Proc. Topical Meeting on the Technology of Controlled Thermonuclear Fusion, San Diego, USAEC CONF-740402P2, Vol. II (1974), p. 400.
15. G. H. Wiley, M. P. Roo, T. W. Petrie, L. M. Hively, "Fusion Product Trajectory Studies," *Bull. Am. Phys. Soc.*, 20, 1259 (1975).
16. J. N. Davidson, "The Effect of Toroidal Field Ripple on Particle and Energy Transport in a Tokamak," ANL/CTR/TM-62, Argonne National Laboratory (1976).
17. L. A. Berry, "Neutral Beam Injection in Tokamaks," *Bull. Am. Phys. Soc.*, 20(10), 1332 (1975).
18. H. P. Eubank, et al., "ATC Neutral Injection Results," *Bull. Am. Phys. Soc.*, 20(10), 1346 (1975); also, R. A. Ellis, et al., "High-Power Neutral Beam Heating in the Adiabatic Toroidal Compressor," MAT-1202, Princeton Plasma Physics Laboratory (1976).

19. R. Dei Cas, "First Results with Neutral Injection in TFR," *Bull. Am. Phys. Soc.*, 20(10), 1332 (1975).
20. A. C. Riviare, "Penetrations of Fast Hydrogen Atoms into a Fusion Reactor Plasma," *Nucl. Fusion*, 11, 363 (1971).
21. D. R. Sweetman, "Ignition Conditions in tokamak Experiments and the Role of Neutral Injection Heating," *Nucl. Fusion*, 13, 157 (1973).
22. J. A. Rome, J. D. Callen, and J. E. Clarke, "Neutral-Beam Injection into a Tokamak: Part I: Fast-Ion Spatial Distribution for Tangential Injection," *Nucl. Fusion*, 14, 141 (1974).
23. W. M. Hooke, PPPL, private communication.
24. B. Richards and R. R. Parker, "Ion Heating in Alcator Near Lower Hybrid Resonance," *Bull. Am. Phys. Soc.*, 20(10), 1313 (1975).
25. J. C. Hosea, "Fast and Slow Wave Generation and Heating in the ST Tokamak," Symposium Report, International School of Plasma Physics, Varenna (1974).
26. J. Adams, et al., "Wave Generation and Heating in the ST Tokamak at the Fundamental Harmonic Ion Cyclotron Frequency," MATT-1094, Princeton Plasma Physics Laboratory (1974).
27. H. R. Jory, E. L. Lien and S. R. Seymons, "Final Report, Millimeter Wave Study Program," Varian Associates (November 1975).
28. L. Spitzer, D. J. Grove, W. E. Johnson, L. Tonks, W. F. Westendorp, "Problems of the Stellarator as a Useful Power Source," USAEC Report NYO-6047 (1954).
29. S. L. Gralnick, "Solid Deuterium Evaporation in a Fusion Plasma," *Nucl. Fusion*, 13, p. 703 (1973).
30. D. J. Rose, "On the Fuel Injection Problem," Culham Tech. Div. Memo 82, Culham, England (1968).
31. "Experimental Power Reactor Conceptual Design Study," General Atomic Company Fusion Staff, General Atomic (July 1975).
32. C. A. Foster, R. J. Colchin, C. D. Hendricks, and R. J. Turnbull, "Solid Hydrogen Pellet Injection into ORMAK," *Bull. Am. Phys. Soc.*, 20, 1300 (1975).
33. R. J. Turnbull, "Pellets for Fusion Reactor Refueling," Annual Progress Report, University of Illinois, COO-2234-9, (January 1976).
34. R. F. Post and S. F. Post, "Flywheels," *Scientific American*, (December 1973).
35. J. E. Osher and G. D. Porter, "Laser Pellet Heating Studies for Plasma Target Production," Lawrence Livermore Report UCID 16872, (1975).

36. B. Lehnert, "Gas Blanket Concept," Proceedings of Third International Symposium on Toroidal Plasma Confinement, Garching (1973).
37. D. A. Baker and J. E. Hammel, "Experimental Studies of the Penetration of a Plasma Stream into a Transverse Magnetic Field," *Phys. Fluids*, 8, p. 713 (1965).
38. R. J. Hawryluk and J. A. Schmidt, "Effect of Low Z Impurities During the Startup Phase of a Large Tokamak," PPPL Report MATT 1201, (January 1976).
39. "The Experimental Thermonuclear Device 'Tokamak-20'," Kurchatov Institute, Moscow, ERDA-TR 58 (1973).
40. R. M. Kulsrud, Y-C Sun, N. K. Winsor, and H. A. Fallon, "Runaway Electrons in a Plasma," *Phys. Rev. Letters*, 31, p. 690 (1973).
41. V. S. Vlasenkov, V. M. Leonov, V. G. Merezkin, and V. S. Mukhovatov, "The Runaway Electron Discharge Regime in the Tokamak-6 Device," *Nucl. Fusion*, 13, p. 509 (1973).
42. J. G. Linhart, P. Gratreau, E. R. Harrison, C. Maisonnier, F. Schneider, and A. Schoch, "Plasma Betatrons," Int. Conf. on High Energy Accelerators and Instrumentation, CERN (1959).
43. S. Von Goeler, Et al., "X-Ray Spectra in the ST Tokamak," Proc. of 3rd Conf. on Toroidal Plasma Confinement, Garching (1973).

CHAPTER IV

MAGNET SYSTEMS

This chapter describes the conceptual design of the EPR magnet system, which consists of the toroidal-field (TF) coils and the poloidal-field coils. The conceptual design of the EPR magnets was part of an integrated design effort, with full interaction with the rest of the EPR system. The magnet design was developed in sufficient detail to define a realistic concept. The philosophy that guided the choice of a design concept from the several candidates was to define a realistic design for a magnet system that could be built, based upon the current state of technology and a feasible development program. The TF coils are described in Section IV-A. The poloidal-field coils, consisting of the initiation-trimming (IT) coils, the ohmic-heating (OH) coils and the equilibrium-field (EF) coils, are described in Section IV-B. The research and development program is described in Section IV-C. The formalism of poloidal flux shielding by normal metal at low temperatures is presented in Appendix F. Eddy current effects on the subdivided blanket and shield due to the pulsed poloidal field are also presented in Appendix F. The magnet protection and safety analysis is presented in this chapter, and the closely related problem of magnet instrumentation and control is described in Section XI-E.

The TF coil uses NbTi as the superconductor because of its great ductility and proven past experience in large magnets. There is an incentive to have the toroidal field level as high as practical to enhance the plasma confinement and the power density. This leads us to pursue a 10 T peak field TF coil design. It is found that it is more economical to operate the 10 T TF coil at 3°K temperature rather than to operate at 4.2°K with a larger amount of superconductor, because the additional refrigeration cost is much smaller than the cost of the additional superconductor. The problem of refrigeration at temperature lower than 4.2°K for large systems is evaluated in Section IV-A-14.

The TF coils will achieve 10 T peak field at 3°K and 8 T peak field at 4.2°K, with about 0.5°K temperature allowance for each case. The 8 T is the minimum goal and the 10 T is the optimal goal of the TF coil design.

The superimposing field from poloidal coils on the TF coil is found to be the single most troublesome factor in the TF coil design. The out-of-plane load is large and creates challenging problems for the structural design. The out-of-plane load also poses a serious threat to the coil stability due to the bending moment and shear stress in the coil structure. Large amounts of ac losses will occur in the TF coil unless the conductor is made of cable. The sponginess of cable conductor adds further to the problem of coil structure integrity. Finally, the most disturbing effect of all, is the pulsed parallel field coupling between twisting filaments at different distances from the conductor axis. If the coupling effect is not countered, the ac losses are proportional to conductor length and the conductor stability is very bad, and the ac losses are large. For this particular reason, it was decided that a field shield must be provided in order to present a sound conceptual design at this time. The suggestion^{1,2} of periodic twisting pitch reversal to decouple the longitudinal changing field has yet to be proven practical or feasible. For these reasons, the field shield was studied in great detail. An aluminum field shield at 18°K temperature was designed. The ac losses and the electromagnetic forces are evaluated. It is shown that the normal metal shield is a reliable and feasible means to overcome the superimposing field. In addition, a superconducting shield design is described.

The requirements for the EF, OH and IT coils are discussed in Chapter III, where the impact of different startup and burn sequences on the required volt-seconds, fields, field rise rates and power is studied. The principle problems in the design of the poloidal coils are the high power and energy transfer requirements that force compromises to be made between voltage, hence insulation requirements, and current, hence transposition requirements in the cable conductors.

Although it is generally recognized that the superconducting TF coils and poloidal coils must be designed with cryostatic stability, cryostatic stability is not well understood. In the past, a magnet was designed for cryostatic stability simply by equating the I_2R loss of a conductor in the normal state to the maximum heat transfer obtainable with liquid helium in the nucleate boiling state. Using heat transfer alone as the main stability

parameter can result in large errors in magnet design. This is discussed in considerable detail.

A better assurance of cryostatic stability of a coil requires good mechanical stability in the conductor and the coil. Four important mechanical instability problems for TF coils are identified. Conductor designs with good mechanical integrity are presented.

The problem of EF penetration through a blanket and shield of ~ 1 m in thickness is a serious one when most of the blanket and shield is made of metallic materials. This problem was studied in great detail. The effect on the blanket and shield design is described in Chapter IX. The eddy current in the blanket and shield will distort the EF pattern and delay its penetration if the blanket and shield is not sufficiently segmented. This is described in Section IV-B-9.

A. Toroidal-Field Coil System

1. Maximum Practical Field

The improvement in energy confinement ($\propto B_t^6$) and power performance ($\propto B_t^4$) at higher toroidal field levels led us to pursue a 10 T TF coil design. NbTi is the best superconductor to use because of its excellent ductility and because much reliable experience exists in large-size superconducting magnets. Although many superconductors of A-15 compounds (e.g. Nb₃Sn, V₃Ga, Nb₃Al, Nb₃Ge or V₃Si) exhibit much higher critical fields, critical current density and critical temperatures than does NbTi, the glass-like brittleness of these materials severely limit their application in large-size magnets, such as the TF coils for an EPR. The development of these materials, if they can be successfully developed at all for large magnets, will take a long time. An expensive project such as the TF coil system for an EPR should not depend on so uncertain an extrapolation into future technology.

The 10 T peak field is as high as is practical for NbTi. A set of fairly representative J-H curves for NbTi (guaranteed performance) at various temperatures and magnetic fields is shown in Figure IV-1. It is seen that the critical current density falls sharply as the magnetic field reaches beyond 8.5 T. However, it is also seen that at 10 T, a factor of 3 to 4 increase in critical current density can be obtained by simply lowering the operating

Figure IV-1. J-H Curve for NbTi Versus Temperature

temperature by 1°K or so. For a given 10 T NbTi TF coil design, one can either choose to operate at a lower critical current density at 4.2°K or at a higher critical current density at reduced temperature. The former choice will require more superconductor but lower cost in refrigeration (350 W/lW) while the latter will require less superconductor but higher cost (500 W/lW) in refrigeration. Since the conductor cost (~ \$20M) for a 10 T EPR/TF coil system is much higher than the refrigeration cost (~ \$5M), the 10 T-3°K design will reduce the overall system cost. Although 3°K (or 180 Torr vapor pressure) is chosen somewhat arbitrarily, it does not appear that any significant increase in critical current density can be achieved by going to lower temperatures.

2. Field Ripple, Access and Coil Size

As described in Section III-A-6, field ripple due to the gaps between TF coils introduces a particle and energy loss mechanism for the plasma. In the present ANL/EPR TF coil design, the operating peak field will be 10 T at 3°K, with a minimum peak field goal of 8 T at 4.2°K operation. Energy losses must be no larger than ~ 10% of trapped-ion-mode (TIM) predictions if ignition is to be achieved at a peak field around 8 T. This requirement can be translated into a ripple criterion, $\delta_0 \leq 0.6/Nq$, where q is the safety factor for MHD stability and N is the number of coils. For $q = 3.0$ and $N = 16$, we have $\delta_0 \leq 0.0132$.

To reduce field ripple, one can either increase the number of TF coils or extend the horizontal bore of the TF coil. An acceptable field ripple can be achieved with 20 TF coils with a horizontal bore of 7.7 m, which was the bore of the TF coils in the EPR scoping study.³ However, it was found that such a 20 TF coil system seriously restricts the design of the mechanical support system for the blanket and shield and makes it very difficult to accommodate the special shield for the neutral beam ducts. Consequently, it was decided to use a 16 TF coil system with the outer leg of the TF coil extended 15 cm further to achieve the required field ripple and provide the necessary access. The field ripple versus coil extension is shown in Figure IV-2.

The horizontal bore of the TF coils must be large enough to contain the plasma and the required blanket and shield around the plasma. The plasma

Figure IV-2. Field Ripple Versus Extension of Outer Coil Leg

diameter is 4.2 m, with 0.3 m scrape-off region around it to reduce the number of fast alpha particles striking the wall. The inner blanket and shield thickness was minimized (0.91 m) in order to maximize the toroidal field in the plasma, thus to enhance the power output of the reactor. An ~ 1.3 m thickness of outer blanket and shield provide the necessary space for both primary energy conversion and radiation protection of the TF coils and other reactor components. These considerations led to a choice of 7.78 m TF coil horizontal bore.

The radial location of the coil centerline for the TF coil inner branch, R_{10} , was determined by the requirements that an OH central flux core of ~ 1.7 m and a thickness of 0.23 m for support cylinder was needed. With 1.7 m flux core radius, the OH coil will supply 85 V-s for plasma driving with a double swing from -5 T to $+5$ T, which is a reasonable central field. The 0.22 m thick support cylinder made of stainless steel will result in a reasonable circumferential stress. In addition, a space of about 0.1 m was provided for the thickness of field shielding and coil form. With these requirements, R_{10} was set at 2.45 m.

With these requirements on R_{10} , the horizontal bore and number of coils established, a pure-tension coil shape was determined. The TF coil system has a maximum of 3 m access. This access is adequate for neutral beam ducts and their side shields, vacuum pumping and operations of assembly and disassembly of blanket and shield blocks.

The theory and the procedure to determine the pure-tension coil shape was described in Reference 3. Figure IV-3 illustrates the TF coil design and Table IV-1 lists the specifications and magnet characteristics of the ANL/EPR TF coil system.

3. Pure-Tension Toroidal-Field Coil Characteristics and In-Plane Hoop Stress

The hoop tension acting on a circular or oval toroidal coil system, either a continuous toroid or a bumpy toroid, is nonuniform. Large bending moments are exerted on the conductor. This produces high peak stresses; consequently, a larger amount of structural material will be needed than in the absence of bending moments. Although it is possible to contain the bending moment by an external mechanical structure, to do so may not avoid conductor

Figure IV-3. EPR Magnet Schematics

Table IV-1. ANL-EPR Toroidal Field Coil System

Superconductor/Stabilizer/Insulator/Support	NbTi/Copper/Fiberglass Epoxy/Stainless Steel
Number of Coils	16
Coil Shape	Pure Torus with $R_{in} = 2.45$ m and $R_{out} = 11.1$ m
Field Ripple	1.3%
Maximum Access	3 m
Peak Field	10 T at 3°E, 8 T at 4.2°E
Vertical Bore	12.6 m
Horizontal Bore	7.78 m
Field in Plasma	4.32 T for 10 T peak field 3.46 T for 8 T peak field
Operational Current	60 kA/Turn
Stored Energy	30 GJ/Total, 1.875 GJ/Coil
Inductance	16.7 H/Total, 1.04 H/Coil
Ampere-Turns	134 MAT/Total, 8.37 MAT/Coil
Turns/Coil	70 x 2
Mean Turn Length	36 m
Total Conductor Length/Coil	151.2 x 10 ³ Ampere-Meters
Approximate Coil Weight/Coil	208 Tons
Coil and Bobbin Cross Section	0.619 m ²
Bobbin Material and Thickness	316 Stainless Steel, 1.25 cm Thick
Winding Cross Section	0.572 m ²
Average Current Density	1352 A/cm ² Average Over Bobbin and Coil 1463 A/cm ² Average Over Coil Winding 3660 A/cm ² Average Over Copper
Smallest Radius of Curvature	1.98 m at 10 T Field Region
Average Hoop Force/Turn	133,134 lb
Average Turn Cross Section	40.6 cm ²
Cross Section Ratio of Stainless Steel to Copper	~ 1.5
Overall Average Stress in Stainless Steel	26,000 psi
Overall Average Stress in Copper	14,500 psi
Length of Straight Segment	8.56 m
Support Cylinder	Outer Radius 2.02 m, Inner Radius 1.80 m
Compressive Pressure	7660 psi
Circumferential Stress	< 74,000 psi

slippage, and large shear stresses may exist from turn-to-turn and/or layer-to-layer. When the radius of curvature is such that the product of transverse force and radius of curvature is constant, the coil is in pure tension with no bending moments; then, a minimum amount of structural material will be needed and the in-plane hoop stress will be uniform with inner skin stress slightly higher than outer skin stress. The mean hoop stress will be the average value of the inner skin stress and the outer skin stress, as illustrated in Figure IV-4. The toroidal coil system will behave exactly like a solenoid. The solenoid is known to the superconducting magnet engineer as the coil which is most stable and has the least mechanical disturbance. Consequently, it is expected that the coil stability of a pure-tension coil system will be better than that of circular or oval coil systems.

To illustrate further the pure-tension coil characteristics, consider a pure-tension 16-coil system with a peak field of 10 T. The radius of curvature, ρ , the transverse force, F_t , and the toroidal fields at the inner coil skin and at the outer coil skin were computed and are shown in Figure IV-5. The ρF_t was shown to be uniform at any angular location. A three-dimensional stress analysis code (the program SOLID SAP⁴) was used to compute the hoop stress. The hoop stress obtained is very close to the product ρF_t within the numerical error of the program SOLID SAP. This indicates that once the radius of curvature around the peak field region near the straight segment of TF coil is known, the average hoop stress is equal to

$$\sigma_{\text{avg}} = 1/2 B_{\text{max}} \cdot I \cdot \rho / A \quad (\text{IV-1})$$

where A is the conductor or coil cross section and I is the current per turn or ampere-turn per coil.

The pure-tension coil profile strongly depends on the number of coils as shown in Figure IV-6. Note that the pure-tension coil shape will become the Princeton D shape⁵ if the coil number is infinite. On the other hand, it will become circular if the number of coils is unity. An implication of this is that any coil of a 16 pure-tension coil system cannot be charged or discharged independently without exerting large bending moments. Consequently, when one or more coils quench, not only is the unbalanced inter-coil force very large, but also the bending moments act on the coils unless all coils carry equal current.

Figure IV-4. Hoop Stresses Distribution of Argonne Pure Tension Coil

Figure IV-5. Pure Tension Coil Characteristics

Figure IV-6. Pure Tension Coil Profile Versus Number of Coils

4. Nuclear Heating and Radiation Damage

Nuclear heating and radiation damage in the TF coil is computed on the basis of a nominal neutron wall loading of $P_W = 0.5 \text{ MW/m}^2$. The actual wall loading depends upon a number of factors, including the maximum field at the TF coil and the plasma MHD equilibrium state, as discussed in Section III-A. A value of $P_W = 0.5 \text{ MW/m}^2$ is a reasonable median value upon which to base these calculations.

(a) Nuclear Heating

With the EPR blanket and shield design the maximum nuclear heating per unit volume in the coil windings is $2.5 \times 10^{-5} \text{ W/cm}^3$, which will generate a temperature rise of $3 \times 10^{-3} \text{ }^\circ\text{K/s}$. This occurs near the peak field region; the nuclear heating decreases exponentially across the winding. The total nuclear heating is 91 W per TF coil and 1460 W for all 16 magnets. This is small in comparison with other heat dissipation in the TF coil.

(b) Radiation Damage to Copper Stabilizer

Copper is used for stabilization and coil protection. Radiation produces lattice imperfections. The low temperature resistivity of copper is completely determined by the lattice imperfections and the magnetoresistivity. The radiation-induced resistivity increases with dose and eventually saturates at an induced resistivity of $3 \times 10^{-7} \text{ } \Omega\text{-cm}$. This resistivity would require large amounts of copper to achieve cryostatic stabilization. To prevent this situation, the blanket and shield is designed to protect the magnet. Neutronic calculations show that a radiation-induced resistivity of $6 \times 10^{-9} \text{ } \Omega\text{-cm}$ will be reached after an integral wall loading of 1 MW-yr/m^2 . In order to account for uncertainties in radiation damage, basic nuclear data and radiation transport, a safety factor, or uncertainty factor, of 2.5 is used. Hence, the copper stabilizer is designed to tolerate an induced resistivity of $1.5 \times 10^{-8} \text{ } \Omega\text{-cm}$. This means that for 0.5 MW/m^2 reference wall loading and assumed 50% capacity factor, four years operation of the TF coil system is possible before the radiation-induced stabilizer resistivity limit is reached. The radiation-induced resistivity can be removed by annealing at room temperature.

(c) Radiation Damage to Insulators

As discussed in Chapter IX, indications are that the aromatic type epoxy resins are the best organic materials to resist radiation damage. The reference magnet design uses fiberglass epoxy as the turn-to-turn and layer-to-layer insulation. The damage to the insulator is mainly the damage of mechanical strength rather than a change in dielectric properties. For the resin, it is found that the mechanical properties are less affected by irradiation near room temperature than other types of insulators. Epoxy is considered 10 to 20 times more resistive to radiation damage than mylar at room temperature. No information exists on radiation damage to organic insulators at 3 to 4°K. Radiation damage to the insulator is not recoverable. The use of epoxy resin as insulator probably will sustain damage up to an integral wall loading greater than 10 MW-yr/m². This is well in excess of the nominal design lifetime of 10 years, or 2.5 MW-yr/m².

(d) Radiation Damage to Superconductor

NbTi is the chosen superconductor. The current-carrying capacity depends on the existence of structure defects. For certain amounts of irradiation, the critical current actually increases. For large doses ($\sim 10^{18}$ n/cm²) of irradiation, it is found that critical current decreases as much as 10 to 15% for most samples. There is insufficient information to guide a magnet designer. To some extent, the damage to NbTi can be recovered by warming up to room temperature. For a TF coil design, some extra NbTi must be reserved to account for uncertainties.

5. Superimposing Fields and Out-of-Plane Load

With a TF peak field of 10 T, the plasma current will be 7.6 MA. The OH coil of ± 67 MA turns will swing a central field of ± 5 T. The EF coil of 37.15 MA turns will generate 0.46 T at the plasma center for plasma equilibrium. Without a field shield, these poloidal coils and plasma currents would superimpose ac fields onto the TF coil windings, exerting an out-of-plane load onto the TF coil, as well as producing large ac losses. The performance and design of the TF coil is severely affected by the superimposing fields. The ac loss is discussed in the next section.

The plasma current and the current in the EF coils contribute most of the superimposing fields. The OH coils have been carefully arranged to produce minimum superimposing fields on the TF coil. The design-basis cycles of the time-varying wave form for OH, plasma and EF currents are shown in Figure IV-7.

To compute the out-of-plane load exerted on the TF winding, the superimposing fields from the OH coils, the EF coils and the plasma were computed at various times in the cycle. The TF coils were divided into 70 angular segments with approximately equal angular spacings. On each angular segment cross section, superimposing fields were computed at nine locations. The out-of-plane load was obtained by Gaussian integration of the $\vec{J} \times \vec{B}$ body force over the conductor segments with proper weighting factors assigned to the values of the superimposing fields at the nine points. Computations at various times were necessary because the OH coils, the EF coils and the plasma contribute different amounts at different times. The distribution of out-of-plane load at 5 representative times is shown in Figure IV-8(a), (b), (c), (d) and (e), respectively. It is important to note that there are many local bending moments exerted on the TF coil. The out-of-plane load of the lower coil half is the mirror image of that for the upper coil half. Surprisingly, the sign of the out-of-plane load reverses rapidly from segment to segment. For this EPR design, the most severe out-of-plane load acts on the coil segments within the angular position between 85° and 125° . The maximum out-of-plane load occurs during the burn phase with a maximum out-of-plane pressure of about 3000 psi at the 100° position. The locations of various local maximum out-of-plane loads vary with time during the burn cycle. To support the out-of-plane load, each mechanical member must be designed to support the out-of-plane load in both directions. In addition, since many bending moments exist along the TF coil, shear members will be effective for the out-of-plane load support. Out-of-plane load support will be discussed in Section IV-A-16.

Since the interaction of superimposing fields with TF currents will produce only out-of-plane loads, the in-plane equilibrium of the pure-tension coil is not affected.

For 8 T operation, both the plasma current and EF coil field will be reduced by a factor of $8/10$; the out-of-plane load will be reduced by $(8/10)^2$.

Figure IV-7. Reference Cycles for OHC, Plasma and EFC

Figure IV-8. Time Evolution of Out of Plane Load. (No Shield)

6. Superimposing Field and AC Losses

The superimposing poloidal fields have both transverse and parallel field components with respect to the TF conductor. To estimate the ac loss when the TF coils are not shielded, the superimposing field was computed at 40 field points along the TF coil centerline at approximately equal angular separation. The ac fields were then decomposed into parallel and perpendicular field components with respect to the TF coil segment as shown in Figure IV-9(a) and 9(b), respectively. Note that the OH coils produce little superimposing field, while the plasma current generates large amounts of parallel fields. The EF coil contributes large amounts of both parallel and perpendicular fields.

If the TF coil is not shielded, the ac losses of a TF coil consist of the ac dissipation in the coil form and the ac loss in the conductor due to both parallel and perpendicular fields. The ac loss in the winding reinforced structure is neglected in this calculation.

(a) AC Loss of Coil Form

The dimensions of the trapezoidal coil form cross section, as well as the cryostat cross section, are shown in Figures IV-10(a) and 10(b). To simplify the calculation, it has been assumed that the superimposing field across a given TF coil cross section is uniform and the ac losses due to the parallel and perpendicular field penetration can be treated separately and then added together.

Consider the parallel field dissipation in the stainless steel coil form. The low conductivity limit applies, since the resistance of the coil form duct per unit length is much greater than the per unit length reactance, $\omega L/\ell = \mu_0 A \omega$, where A is the cross-sectional area of coil form. From Appendix F it is easy to show that the rms parallel field loss per coil is given by

$$P_{||} = \frac{1}{2} \left(\frac{t}{\rho} \right) \frac{A^2}{p} \int_{\ell} \dot{B}_{o||}^2 d\ell \quad (\text{IV-2})$$

where the t is the wall thickness of coil form, p is the perimeter of coil form, ρ is the resistivity of coil form and is equal to $5 \times 10^{-7} \Omega\text{-m}$ at 4.2°K, and A is the cross section of the coil form. The integration $\int_{\ell} \dot{B}_{o||}^2 d\ell$

Figure IV-3a. $B_{||}$ to TF Coil Due to EF, OH and Plasma Currents

Figure IV-9b. $B_{\perp}$ to TF Coil Due to EF, OH and Plasma Currents

AL. ALLOY 300K
 VAC. WALL (3.5cm)
 VAC. SPACING (2.5 cm)
 LN₂ SHIELD WITH
 SUPERINSULATION (5cm)
 VAC. SPACING (2.5 cm)

OH COIL

5cm INSULATION —

S.S. SUPPORT

1.25 cm THICK
 COIL FORM

.25cm THICK
 S.S. WALL

4cm THICK
 ALUMINUM SHIELD
 AT 3°/4.2°K

(a) INNER LEG CRYOSTAT AND COIL FORM

— 3.5cm THICK
 AL. ALLOY

3.5cm VACUUM
 SPACING AND
 SUPERINSULATION

1.25 cm THICK
 COIL FORM

1cm THICK
 S.S. BACKING

SUPERINSULATION

DENSE GAS
 COOLING TUBE

12.5°K-23.5°K
 ALUMINUM SHIELD
 (5cm THICK)

(b) OUTER LEG CRYOSTAT AND COIL FORM

Figure IV-10. Cross Section of Cryostat and Coil Form

carried out numerically over the entire TF coil gives $0.147 \text{ T}^2 \text{ m/s}^2$ from EF coil superimposing field and $0.692 \text{ T}^2 \text{ m/s}^2$ from plasma current. Therefore, for a 60 s burn pulse and a 15 s replenishment time, the ac pulsing losses averaged over a cycle is 30 W/coil from the EF coil and 40 W/coil from the plasma current. Then, the cycle time-averaged parallel field loss is 70 W/coil.

The perpendicular field dissipation per unit length is given by:

$$\frac{P_{\perp}}{\ell} = \frac{(\dot{\phi}/\ell)^2}{(R/\ell)} \quad (\text{IV-3a})$$

Where $R/\ell = 4 \rho/Pt$ and $\dot{\phi}/\ell = 0.92 \dot{B}_{O\perp}$. Hence, the rms perpendicular field loss per coil is given by:

$$\overline{P}_{\perp} = 19467 \int \dot{B}_{O\perp}^2 d\ell \quad (\text{IV-3b})$$

The integration $\int \dot{B}_{O\perp}^2 d\ell$ gives $0.139 \text{ T}^2 \text{ m/s}^2$ from the EF coils and $0.09 \text{ T}^2 \text{ m/s}^2$ from the plasma ring. The cycle time-averaged ac loss is 468 W from the EF coils and 93 W from the plasma current. The cycle time-averaged perpendicular field loss is 561 W/coil. The total ac loss in the coil form is 631 W/coil.

(b) Perpendicular Field Dissipation in Conductor

To estimate the transverse field dissipation in the conductor, it has been assumed that the TF coil conductor is a cabled conductor consisting of many strands of 1 mm diameter, transverse twisting pitch $\ell_p = 2 \text{ cm}$ and filament diameter of 10μ embedded into a single-component copper stabilizer with both transverse and longitudinal resistivity of $5 \times 10^{-8} \Omega\text{-cm}$. Based on the 60 kA conductor specification shown in Table IV-2, the NbTi cross section per turn is 0.53 cm^2 and the copper cross section is 13.5 cm^2 . The NbTi volume/coil is 0.2671 m^3 and the copper volume/coil is 6.820 m^3 . By the methods discussed in Reference 3, the filament loss over a field cycle of $0 \rightarrow 0.3 \text{ T} \rightarrow 0$ is found to be equal to 2670 J, the matrix loss for a cycle consisting of a 2 s linear rise and a 2 s linear fall with $B \sim 0.3 \text{ T}$ is equal to 6762 J. The cycle time-averaged power dissipation due to the perpendicular field is 132 W/coil.

(c) Parallel Field Dissipation in Conductor

Practical superconductors are divided into many filaments to reduce the hysteresis loss when subjected to an ac pulsing field. These fine filaments

Table IV-2. Typical Specifications of 60 kA TF Coil Conductor

Maximum Field (T)	10 ^c	9	8	7	6	5	4	3 ^d
NbTi Critical Current Density ^a (kA/cm ²)	70	100	130	155	180	195	210	250
NbTi Design Current Density ^b (kA/cm ²)	50	70	93	110	140	155	170	210
NbTi Cross-section (cm ²)/Turn	1.2	0.86	0.65	0.55	0.43	0.39	0.35	0.29
Total Resistivity (10 ⁻⁸ Ω -cm)	6	5.6	5	4.5	4.0	3.5	3.0	2.5
Approximate Turn Width (cm)	33	34.7	36.5	38.2	40	41.7	43.4	45.2 to 50.4
Copper Thickness (cm)/Turn	0.46	0.43	0.39	0.35	0.31	0.27	0.24	0.23
Stainless Steel Thickness (cm)/ Turn	0.31	0.34	0.38	0.42	0.46	0.50	0.53	0.54
Insulator Thickness (cm)/Turn	0.2	0.2	0.2	0.2	0.2	0.2	0.2	0.2
Total Cross-section (cm ²)/Turn	32	34	35.5	37.1	38.9	40.5	42	44 to 49
Thickness/Layer(cm)	0.97	0.97	0.97	0.97	0.97	0.97	0.97	0.97
Number of Layers	7	7	7	7	7	7	7	21
Number of Turns	14	14	14	14	14	14	14	42
Current Density In Copper (kA/cm ²)	3.95	4.02	4.21	4.49	4.84	5.33	5.76	5.77

a. Based on 3°K J-H Curve (see Figure IV-1)

b. Based on 3.5°K J-H Curve.

c. Innermost region

d. Outermost region

must be twisted about the conductor axis in order to reduce the coupling eddy current for a changing transverse field. For a parallel changing field, these twisting filaments, at different distances from the conductor axis, will induce different voltages; longitudinal coupling arise and the associated ac loss is quite large. To reduce the longitudinal coupling, it was suggested^{1,2} that the transverse twisting be periodically reversed. Based on this hypothetical conductor with twisting pitch reversal, several authors^{1,6,7,8} have studied the parallel field losses.

The parallel field losses consist of the eddy current loss and the hysteresis loss. The eddy current loss of a coil is given by:⁶

$$P_e = \frac{1}{6} \frac{L_p^2 X_o^2}{\rho} A \int \dot{B}_{||}^2 d\ell \quad (IV-4)$$

Where $\rho = 5 \times 10^{-10} \Omega\text{-m}$, L_p is the periodic length of twisting reversal and $X_o = 2\pi r_o / \ell_p = 0.157$, where r_o is the strand diameter and ℓ_p is the transverse twisting pitch. The parameter A is the coil winding cross section and is approximately equal to 0.6 m^2 . The integral $\int \dot{B}_{||}^2 d\ell$ is equal to $2.09 \text{ T}^2\text{m/s}^2$. With $X_o = 0.157$, one finds the eddy current loss to be equal to 4121 W/coil . The time-averaged eddy current loss is 220 W/coil .

The hysteresis loss per coil is given by:⁶

$$P_e = J_{\text{avg}} r_o \left(\frac{\sqrt{1 + X_o^2} - 1}{4X_o} \right) \left[1 + \left(\frac{I_T}{I_C} \right)^2 \right] A \int \dot{B}_{||} d\ell \quad (IV-5)$$

Where J_{avg} is the average current density in conductor and is equal to $5.115 \times 10^7 \text{ A/m}^2$. The integral $\int \dot{B}_{||} d\ell$ over the entire TF coil gives 2.375 T m/s . The hysteresis loss for $I_T = I_C$ is 1422 W . The cycle time-averaged hysteresis loss is 76 W/coil .

Therefore, neglecting the ac loss in the winding reinforced material, the ac losses per TF coil consist of 631 W loss in the coil form, 132 W loss in the conductor due to the perpendicular field and 296 W loss in conductor due to the parallel field. The total loss per coil is 1059 W . These losses are further summarized in Table IV-4.

7. Normal Metal Field Shielding Design

The TF coil reference design provides normal metal field shielding. The overriding reasons for TF coil field shielding are as follows:

- (1) The existing superconductor manufacturing industry can not yet produce the superconductor composite with longitudinal twisting reversal, as discussed in previous sections. Furthermore, it is not clear that twisting reversal is a practical solution. Existing TF coil conductors under a changing parallel poloidal field will experience a huge ac loss, not to mention the serious problem of conductor stability. The only alternative solution left is to arrange the filaments at equal distance from the conductor strand axis and then cable these strands with full transposition. A plausible cable design is presented in Section IV-A-10.
- (2) With a field shield, a monolithic conductor can be used without excessive ac losses. Without the shield, ac losses can be reduced to a reasonable level, by using cable conductor, provided that the filaments are fully transposed as mentioned in previous paragraphs. There is little experience of using cable conductor in a large magnet. It is fair to say that the sponginess of cable conductor raises questions about its use in a large magnet system with large electromagnetic forces. As far as mechanical integrity is concerned, monolithic conductors are far better than cable conductors. As discussed in Section IV-A-5, the out-of-plane load on a TF coil is a serious threat to TF coil mechanical stability.

Therefore, a field shield for TF coils is exceedingly important. In this section, a detailed analysis of a normal metal shield is presented. The basic concepts and the ac loss estimation for the normal metal shield are described in Appendix F. Since the stability of a superconducting shield is in doubt, the normal metal shield is chosen as the shield reference design. However, as there are many advantages to employing superconducting shields, a preliminary analysis of superconducting shield feasibility is discussed in the next section.

(a) High-Purity Aluminum as Normal Metal Shield

To a good approximation, the resistivity of a metal can be written as

$$\rho = \rho_0 + \rho_{th}(T) + \rho_m(H) + \rho_e(E)$$

Where ρ_0 is a temperature-independent "residual" resistivity that is due to electron scattering from impurities, defects, etc.; ρ_{th} is a temperature-dependent resistivity due to the thermal vibrations of the crystal lattice; $\rho_m(H)$ is magnetoresistivity and ρ_e is the strain-induced resistivity.

High-purity aluminum or copper has a large residual resistivity ratio ($RRR \equiv \rho_{273^\circ K} / \rho_{4.2^\circ K}$), on the order of 10^3 to 10^4 . However, the large magnetoresistivity of copper severely reduces its conductivity gain at low temperatures. As shown in Figure IV-11,^{9,10,11} the magnetoresistivity of high-purity copper at 4 T is more than $10 \times 10^{-11} \Omega\text{-m}$, with little variation with temperature between 4.2°K and 20°K. Aluminum, on the other hand, has a rather small magnetoresistivity that saturates at a rather low field (~ 0.5 T). Figure IV-12 shows the resistivity of aluminum at various temperatures^{9,12,13,14,15,16} and magnet field levels versus the aluminum purity. It is seen that the low magnetoresistivity of aluminum recommends it for use as the normal metal shield. Note that the solid curves are experimental data while the dotted curves were obtained by adding the term $\rho_{th}(T)$ to the residual resistivity curve at 4.2°K and zero fields. $\rho_{th}(T)$ at 4.2°K is negligibly small ($\sim 10^{-14} \Omega\text{-m}$).

For strains less than 10%, the strain-induced resistivity of aluminum is given by¹⁷

$$\Delta\rho = 110 E^{1.19} \times 10^{-11} \Omega\text{-m} \quad (\text{IV-6})$$

where E is the strain in the aluminum. To keep the strain-induced resistivity small, the aluminum must be reinforced. For example, if aluminum is explosively welded to stainless steel, then the stainless steel will be stressed to 60,000 psi while the aluminum suffers a strain of only 0.17%. The 0.17% strain causes little increase in resistivity. For 5000 purity aluminum reinforced by stainless steel, recent data¹⁸ indicate that there is little increase in resistivity for up to 1000 loading cycles.

Figure IV-11. Magnetoresistance of Copper

Figure IV-12. Resistivity of Aluminum as Function of Purity, Temperature and Magnetic Field

The main resistivity increments due to impurities in commercially available aluminum at 4.2°K are as follows:

<u>Impurities</u>	<u>Resistivity Increment (10^{-11} Ω-m/PPM)</u>
copper	0.122
iron	0.140
silicon	0.102
zinc	0.050

For "zone-refined" aluminum of purity 99.9999%, the only detachable impurities are 3 PPM of iron and 0.4 PPM of copper.¹⁵ Such aluminum, after annealing for two hours at 500°C, will often achieve an RRR value of more than 5000. The price¹⁸ of 5000 purity aluminum is about \$20/kg in small quantities and is \$2.82/kg for very large quantities.

The 5000 purity aluminum with stainless steel backing is chosen for the field shield panels. These panels are formed into rectangular ducts around the TF coil as shown in Figure IV-3. For the present design, the shields around the inner leg of the TF coil are seriously restricted by the available space and are, therefore, wrapped directly on the TF coil form. The operating temperature is 3°K or 4.2°K depending upon the 10 T or 8 T operation (see Figure IV-10(a)). The remaining shields will be operated between 12.5°K and 23.5°K with a mean temperature of 18°K (see Figure IV-10(b)). Shield panels inside the TF coil will be subjected to a peak field ranging from 3 T to 10 T. The side panels will have an averaged magnetic field ranging from 1.5 T to 5 T. The outside panels will be subjected to only the ac superimposing field of ~ 0.5 T. The overall averaged magnetic field in the shield is about 4 T. As shown in Figure IV-12, the resistivity of 5000 purity aluminum at 3°K or 4.2°K and 4 T field is about 2×10^{-11} Ω -m. The resistivity at 18°K and 4 T field is about 5×10^{-11} Ω -m. These data are based on transverse magnetoresistivity measurements. The longitudinal magnetoresistivity, in general, is slightly smaller than the transverse values for the same purity of aluminum.

The fundamental frequency for a cycle time, T, is 1/T cps. So the penetration depth is given by

$$\delta = \sqrt{2\rho/\omega\mu} = 503\sqrt{\rho/f} = 503\sqrt{\rho T} \quad (\text{IV-7})$$

where μ is the permeability, ρ is the resistivity, T is the period and f is the frequency. At 18°K and 4 T field, the penetration depth for the 5000 purity aluminum is 3.1 cm for $T = 75$ s. At 3 to 4.2°K and 4 T field, the penetration depth is about 1.95 cm. To provide nearly complete shielding, the 18°K shield should have a thickness of 5 cm. The 3 to 4°K shield should have a thickness of 4 cm.

(b) AC Losses In Aluminum Shield

The reference cycles for the EF coil, the OH coil and the plasma current are shown in Figure IV-7. The ac losses in the field shield can be separated into those due to perpendicular fields and those due to parallel fields. Since the three poloidal field wave forms are not alike, each individual wave form is expanded into a Fourier cosine series with coefficients evaluated separately. The Fourier series representing the resultant superimposing field is equal to the sum of the three individual Fourier series. Let $m = 1, 2, 3$ represent the EF, OH and plasma, respectively, then following the discussion of Appendix F, the time-averaged perpendicular field dissipation is given by:

$$\bar{P}_{\perp} = \frac{1}{2} \frac{\rho p}{\mu_o} \int_{\ell} \sum_{n=1}^{\infty} \frac{1}{\delta_n} \left(\sum_{m=1}^3 A_{nm} B_{m\perp} \right)^2 2d\ell \quad (\text{IV-8a})$$

Setting $\rho = 5 \times 10^{-11} \Omega\text{-m}$, $p \sim 4$ m and $\delta_n = \sqrt{2\rho/\omega_n\mu}$, one obtains:

$$\bar{P}_{\perp} = 63.35 \int \sum_{n=1}^{\infty} \frac{1}{\delta_n} \left(\sum_{m=1}^3 A_{nm} B_{m\perp} \right)^2 2d\ell \quad (\text{IV-8b})$$

Similarly,

$$\bar{P}_{\parallel} = 31.7 \int \sum_{n=1}^{\infty} \frac{1}{\delta_n} \left(\sum_{m=1}^3 A_{nm} B_{m\parallel} \right)^2 2d\ell \quad (\text{IV-9})$$

Where $2d\ell$ is used to include both the upper and the lower TF coil halves.

The total ac losses are given by

$$\bar{P} = \bar{P}_{\perp} + \bar{P}_{\parallel}$$

Since $A_{nm} \propto 1/n^2$ and $1/\delta_n \propto r^{1/2}$, it follows that $\bar{P} \propto \sum_{n=1}^{\infty} n^{-7/2}$. Consequently, if $\bar{P}$ is computed by summing terms up to $n = 5$, the remaining terms will contribute less than 3%.

The angular distribution along the TF coil length measured from $\theta = 0$ of both the parallel and the perpendicular fields is shown in Figure IV-9.

The ac loss distributions, expressed as the power dissipation per meter, are shown in Figure IV-13 and are further tabulated in Table IV-3.

(c) Field Shield Design

Shields around the inner branch of the TF winding are seriously restricted by the available space. Shields with angular locations falling between 122° and 180° must be wrapped around the TF coil form as shown in Figure IV-10(a). Since the centering force compressive pressure is around 7700 psi, while the circumferential stress of the support cylinder is around 60,000 psi, it was decided not to wrap the shield around the support cylinder. The yield strength of aluminum at 4°K to 20°K is around 2000 psi. Therefore, a thin stainless steel skin is needed to bond to aluminum with studs connecting the stainless steel skin with the coil form in order to confine the aluminum and to keep the strain at a low level. The shield must be operated at the same temperature as the TF coil. The total ac losses for this shield are 298.6 W per TF coil, with 147 W as the perpendicular field dissipation and 151.6 W from the parallel field loss.

The remaining shields will be refrigerated between 12.5°K and 23.5°K with a mean temperature of 18°K . The shields will be bonded to a stainless steel backing 1 cm thick. The total ac losses of the 18°K shield is 8.531 kW per coil with 3.295 kW from parallel field and 5.236 kW from perpendicular field losses.

Consideration was given to reducing the ac losses by moving the EF coils further away from the TF coils. If the poloidal coils number 3, 4 and 5 (see Figure IV-3) were raised 1 m higher to reduce their effect on the TF coils, then each of the EF coils excitation must be increased by a factor of 1.063

Figure IV-13. AC Losses Distribution Along TF Coil

Table IV-3. AC Loss in Aluminum Shield

θ^α (degree)	$d\ell^\alpha$ (m)	$\bar{P} / \ell$ (W/m)	$(\bar{P} / \ell)d\ell$ (W)	$\bar{P} / \ell$ (W/m)	$(\bar{P} / \ell)d\ell$ (W)	$\bar{P} / \ell$ (W/m)
9	1.36	6.9	9.4	41.2	56.0	48.1
18	0.95	20.6	19.6	70.4	66.9	91.0
27	0.95	10.2	9.7	133.4	126.7	143.6
36	0.97	3.8	3.7	144.7	140.6	148.7
45	0.90	17.5	15.8	83.2	74.9	100.7
53	0.97	2.7	2.6	71.3	69.2	74.0
65	1.00	5.4	5.4	121.3	121.8	127.2
77	1.15	51.1	58.8	142.6	164.0	193.7
84	1.00	160.1	160.1	150.1	150.1	310.2
90	1.12	460.0	515.2	121.3	135.9	581.3
100	0.95	1153.1	1095.4	18.1	17.2	1171.2
106	0.88	752.0	661.8	280.1	246.5	1032.1
115	0.90	67.0	<u>60.3</u>	308.8	<u>277.9</u>	375.8
18°K Shield AC Loss Summation					2617.8/Half Coil	1647.7/Half Coil
122	0.95	68.4	65.0	5.9	5.6	74.3
128	0.85	2.4	2.0	2.3	2.3	4.7
135	0.85	0.7	0.6	4.9	4.9	5.6
146	1.00	3.7	3.7	15.3	15.3	19.0
162	1.00	2.2	2.2	29.9	29.9	32.1
180	0.50	0.0	<u>0.0</u>	37.6	<u>37.6</u>	37.6
3°K Shield AC Loss Summation			73.5/Half Coil	75.8/Half Coil		

⁽¹⁾ See Figure IV-13.

to generate a circular plasma. In that case, the total ac losses in the 18°K shield would be reduced to about 7.32 kW per coil, a very small reduction considering the disadvantages of moving the coils.

The refrigeration of the shields is discussed in Section IV-A-14.

(d) DC Field Soaking

About 70% of the superimposing field is the dc field component. The dc field will soak through the aluminum shield in a time constant of

$$t \approx \frac{L}{R} \approx \frac{\mu_o A t}{\rho p} \sim 6 \text{ minutes.} \quad (\text{IV-10})$$

where $A \sim 1 \text{ m}^2$, $t \sim 0.05 \text{ m}$, $p \sim 4 \text{ m}$ and $\rho = 5 \times 10^{-11} \text{ } \Omega\text{-m}$ are used. The 6 minutes soaking time constant is considered rather long compared with the current ramping time of about 6 s. Once soak through is complete, the dc field will remain as a static field interacting with the TF coil current producing 70% of the out-of-plane load previously calculated (see Figure IV-8(e)). The out-of-plane load on the TF coil is static with the shield and cyclic if the coil is not shielded. The dc field will soak out 6 minutes after the external poloidal field is removed.

(e) Out-of-Plane Load in Shield

The aluminum shield is now subject to the cyclic out-of-plane loads. For example, if the superimposing fields had a time dependence which was a pure sine wave with no dc component, then the out-of-plane load on the shield would be the same as it would be on the TF coil in the absence of the shield. The loads on the shield are now due to the forces exerted by the toroidal field on the eddy currents in the shield. The distribution of the out-of-plane load on the shield is shown in Figure IV-14. The distribution is very similar to that in Figure IV-8(e) (load on TF coil with no shield) but at only 30% of the magnitude. There are, in addition, in-plane forces and twisting moments. It is cumbersome to display these here but their magnitudes are such that their support appears manageable. The support of the out-of-plane loads is discussed in Section IV-A-16.

Figure IV-14. Out of Plane Load on Al Shield

(f) Conclusions

It appears feasible to build the TF coil with a low-temperature normal metal shield. The shield removes the problem of longitudinal ac field coupling. It is fair to say that the shield is the only presently known method that will guarantee stable TF coil operation under a pulsed poloidal field. The refrigeration for the shield appears manageable. The stresses on the TF coil become static with the shield and are reduced by 30%, while the cyclic stresses on the shield are only 30% of the stresses on the unshielded TF coil.

8. Shield Option - Superconducting Field Shield

As an option to the low-temperature aluminum shields around the TF coils described above, superconducting shields were examined. Two forms of superconducting shields have been considered: coils of superconducting wire that would permit eddy currents in pre-specified directions or a superconducting film that would allow current flow in more general directions.

The film could be deposited on copper plates to be assembled around the TF coil or deposited on a wide copper ribbon to be wound around the TF coil. Like the aluminum shield, the superconducting film would provide almost complete shielding of the TF coil, but the energy losses in the shield itself might be much less for the film than for the aluminum. On the other hand, such a film requires considerable extrapolation from present materials and techniques. For that reason, shields of superconducting wire have received more attention.

The changing magnetic flux through a shorted coil will induce a current, which in turn will produce a flux opposing the applied flux. If the coil is perfectly conducting, the current will exactly cancel the change in flux. A coil wrapped around a section of a TF coil will cancel the change in parallel field, and one wrapped along a section of the TF coil will cancel the change in perpendicular field. Thus, a sufficiently large number of coils might be expected to completely shield the TF coil from any change of flux.

In the calculations, the condition that the flux be unchanged was approximated by the computationally simpler one that the perpendicular field magnitude at the center of each coil, squared and summed over all coils, be a minimum.

The currents required were calculated from expressions relating field and current via vector potential for a filamentary quadrilateral coil.

$$\vec{B} = \text{Curl } \vec{A}; \vec{A} = \frac{\mu_0 I}{4\pi} \oint \frac{\hat{r} dx + \hat{j} dy}{r} \quad (\text{IV-11})$$

In components, the field at (x_0, y_0, z_0) for a coil parallel to the x-y plane is:

$$\begin{aligned} B_x &= -\frac{\mu_0}{4\pi} I \frac{(z_0 - z)}{r D_x} \\ B_y &= \frac{\mu_0}{4\pi} I \frac{(z_0 - z)}{r D_y} \\ B_z &= \frac{\mu_0}{4\pi} I \left[\left\{ \frac{x_0 - x}{r} - \frac{m^{-1}}{\sqrt{1 + (m^{-1})^2}} \right\} \frac{1}{D_x} - \left\{ \frac{y_0 - y}{r} - \frac{m}{\sqrt{1 + m^2}} \right\} \frac{1}{D_y} \right] \end{aligned} \quad (\text{IV-12})$$

where x, y and z are evaluated around the loop, m is the slope of a side of the loop, and

$$D_x = \sqrt{1 + m^2} r + \{1 + (m^{-1})^2\} y - y_0 - (m^{-1})^2 y + m^{-1} (x - x_0)$$

$$D_y = \sqrt{1 + m^2} r + \{1 + m^2\} x - x_0 - m^2 x + m (y - y_0)$$

$$r = \sqrt{(x_0 - x)^2 + (y_0 - y)^2 + (z_0 - z)^2}$$

For a coil not in the x-y plane, the field can be found from the above equations by coordinate transformation.

In general, at the center of each coil there will be field from all the coils, leading to a system of linear simultaneous equations, which must be solved to find the combination of currents that will minimize the field at each.

Calculations showed that 37 shorted superconducting coils wrapped around each TF coil, equally spaced in angle as measured from the center of the plasma, would effectively lower the parallel component of the pulsing field, but that coils of that size would be ineffective in lowering the perpendicular component. Two additional superconducting coils, around the top and bottom of each TF coil, would shield against the vertical component of field in these regions. These coils, with axes vertical and dimensions of 6.0 m by 1.0 m, would have to be driven, but the shorted coils would not. Currents in the shorted coils would be typically 200 kA-turns; in the driven coils, 1000 kA-turns. The locations of the shielding coils are shown in Figure IV-15.

At the center of the plasma, the fields from all the parallel-field shields around one TF coil add up to 1.8 G; the shields around the other 15 TF coils increase the field to 10 G. However, the two vertical-field shields contribute 74 G and the other 30 vertical-field shields increase the number to 235 G, a value that may or may not be too high, depending on its variation across the plasma.

Figure IV-16 shows how the field on the TF coils is decreased by the system of shield coils described above. The shields do not entirely exclude pulsing field from the TF coil, but they do reduce the quantity $\int B_{\text{total}}^2 dl$, which may be used as a measure of the ac losses, by a factor of ten.

A superconducting field shield is expected to experience much smaller eddy current ac losses than a normal metal shield. However, the normal metal shield has been chosen for the design because the technological aspects of the superconducting shield are too uncertain. Coils of superconducting wire wound around the TF coils present serious and unresolved problems in their winding, cooling and support. The other alternative, a superconducting film, is not sufficiently developed to depend upon, and may well experience stability problems due to flux jumping. Thus, with our present knowledge, the normal metal shield seems the better bet.

9. Cryostatic Stability

The classic theory of full stabilization requires that $I_o^2 R \leq q_h p$, where p is the exposed cooling perimeter per unit length, and q_h is the heat flux of

Figure IV-15. Locations of Superconducting Shield Coils

Figure IV-16. Field Screening by Superconducting Shield Coils

pool boiling helium. The general rule of thumb is to pick $q_h = 0.35 \text{ W/cm}^2$ as the recovery heat flux. However, when one surveys superconducting magnet literature, it is quite surprising to find that several magnets¹⁹ based on q_h as low as 0.06 W/cm^2 were not fully stable and did not even achieve the design field. On the other hand, several magnets²⁰ designed with q_h as high as 2.2 W/cm^2 were cryostatically stable in that current shared without thermal runaway when I exceeded the critical current of the conductor. Therefore, it is safe to say of all large cryostatic stable magnet designs, that for a given conductor and coil design, a reliable q_h must be determined experimentally.

In fact, it is not correct to relate the recovery heat flux, which is experimentally determined, to the familiar q_h of pool boiling helium. The reason is that for a section of conductor in a coil, heat removal is possible not only by the q_h , i.e. losing heat to liquid helium through exposed surface area, but also by heat conduction along the conductor in either one or both directions (call this Q_{cond}) and by cooling through insulation to neighboring turns (call this Q_I). In the steady-state situation, the only heating source is $I_o^2 R$. Figure IV-17 illustrates the distributions of q_h , Q_{cond} , Q_I and $I^2 R$ as well as the temperature profile along a conductor segment in a coil undergoing a typical steady-state recovery. Therefore, it is seen that the true full stabilization must be such that²¹

$$I_o^2 R = J_o^2 \rho(B) A \leq q_h p + K_c A_c \frac{\Delta T}{\Delta X} + K_i A_i \frac{\Delta T_i}{t} \quad (\text{IV-13})$$

where ΔX is a infinitesimal length and $\Delta T/\Delta X$ is the temperature gradient along the conductor, J_o is operational current density in the conductor, $\rho(B)$ is the resistivity of the stabilizer, A is the conductor cross section ΔT_i is the temperature difference between neighboring layers, A_i is the insulation surface area in contact between adjacent turns, t is the thickness of insulation, and K_c and K_i are the thermal conductivity for conductor and insulation, respectively. The characteristics of q_h are shown in Figure IV-18.

Note that the above equation says that recovery heat flux contributions come from three sources and that recovery heat flux determined experimentally cannot be identified as q_h alone. This explains the fact that equivalent

$$Q_P = q_h P$$

$$Q_{COND} = K_c A \frac{T_h - T_b}{\Delta X}$$

$$K_I = K_i A_i \frac{\Delta T_i}{\tau}$$

TEMPERATURE PROFILE

$$J_0^2 \rho A = I^2 R$$

Figure IV-17. Distributions of Heat Sources and Heat Sinks

Figure V-18. Heat Transfer Characteristics of Boiling Helium

recovery heat flux as high as 2.2 W/cm^2 is possible. If mechanical structure and coil support are so well designed that the only conductor motion is a single turn slippage, then it is quite possible that the magnet could be cryostatically stable at a large heat flux. If a coil has less desirable coil structure so that a whole layer slide may occur, then Q_i must not be included. If a coil has rather poor mechanical structure, or too many other heating sources such as flux jumps, strain energy release or eddy current heating, etc., then this coil may not operate at full stabilization at as low a heat flux as 0.06 W/cm^2 , because the contribution from either pq_h or Q_{cond} cannot be fully depended on. Therefore, the nature of coil stability depends on many factors such as coil mechanical disturbance, the size and the nature of cooling passage, other sources of heating, whether there is vapor bonding or not, conductor insulation effect on heat transfer, etc. These factors all affect the recovery flux in an interdependent manner. For a given coil, precise analysis of these factors is not possible. Experimental simulations are essential. It is important to recognize that mechanical integrity of a coil design is an important factor to assure full stabilization. Also note that, for a given set of quantities for the right hand side of the above equation, the coil stability deteriorates as J_o^2 . Hence, lower current density will improve coil stability tremendously.

To illustrate further the complicated nature of coil stability, consider the typical heat transfer flux characteristics curve as shown in Figure IV-18. If a steady-state heat source with heat flux greater than $q_h = 0.7 \text{ W/cm}^2$ is present, the copper temperature will be suddenly raised to 50°K or so along $\overline{AB}$. Reduction of heat flux will follow the recovery path $\overline{BCD}$ with recovery flux equal to $q_f = 0.15 \text{ W/cm}^2$. Both q_h and q_f depend on the liquid channel length and the channel cross section. If the liquid channel has a gap $< 0.3 \text{ mm}$, then q_h and q_f will be proportional to the gap size.

Finally, steady-state considerations of coil stability will not necessarily guarantee that the coil will be cryostatically stable in a transient disturbance state. For instance, if the whole coil is undergoing a violent disturbance so that the steady-state cooling is not available, the coil shall completely quench without current sharing.

10. Conductor Design, Coil Structure and Coil Winding

As discussed in the preceding section, the full stability of the conductor will be assured if the conductor has good mechanical stability in a proposed coil structure. Careful investigation of all the forces exerted on the TF coil conductor reveals that there are at least four significant mechanical instabilities in a TF coil. These are as follows.

Out-of-Plane Load

The out-of-plane load, as shown in Figure IV-8, produces many local bending moments within each coil half and large over-turning moments between the two coil halves. Both the local moments and the over-turning moments are quite capable of generating shear stress between turns or between layers. If the TF coil is not shielded, these forces and shear stresses are cyclic in nature. Consequently, damage to the superconducting filaments, the copper stabilizer, insulator and coil structure must be carefully investigated.

The Centering Force

Conductors, insulators and structural materials in the TF straight segment at the inner leg will experience a large radial inward compression of about 8000 psi. This will likely generate layer to layer shear unless each layer is flat and firm enough to support its neighboring layer. The packing of these straight conductor segments must be quite tight to ensure force transfer. It is interesting to note that if these conductors are not straight, there is a vertical force attempting to straighten them out, thus, the relative generating motion and frictional heating.

Gravitational Load

The fact that the TF conductor in each coil weighs about 208 tons and is standing in a vertical plane could cause a problem in mechanical instability. The large static g-load and the large coil dimensions will require that the conductor in each layer must be set firmly on its inner neighboring layer and that the conductor must be tightly wound so that the coil will not become loose after it is standing in the vertical plane.

Other Bending Moments

Bending moments of a pure-tension TF coil may still exist if the coil shape is distorted because of imperfection in coil winding and the deflection of conductor and coil form due to g-load. Bending moments may also be developed if there is an in-plane coil displacement such as the vertical or radial displacement due to the imperfection of coil fabrication and assembly.

It is clear that a good TF conductor and coil design to overcome these mechanical instabilities is a challenging job. Since the bubble chamber magnets do not have the same mechanical instabilities that the TF coil does, the general wisdom is that the bubble chamber magnet type conductor and coil structure is probably inadequate to handle the TF coil forces. In general, pancake windings provide good support as far as the hoop force is concerned. However, the conventional pancake winding, consisting of many layers, will have poor mechanical integrity as far as the out-of-plane load is concerned. The solid pancake design,²² proposed by the University of Wisconsin group, is an attempt to improve the mechanical integrity of pancake winding against the out-of-plane load.

(a) Reference Conductor Design - Sheet Conductor

Based on the preceding design considerations, it is proposed that a wide sheet conductor be used in the TF coils if a field shield is provided. If each TF coil layer is made of a single sheet, the width of a sheet will be ~ 1 m. With reasonable sheet thickness (~ 1 cm), the operational current will exceed 100 kA. It is therefore proposed to divide each TF coil cross section into two subdivisions with a central partition rib as shown in Figure IV-10. This rib will serve several useful purposes: (1) it reinforces the strength of the coil form under g-load so that 1.25 cm thick coil forms will be adequate instead of 2.5 cm ones; and (2) the rib will also allow the jelly-roll winding in each subdivision to be done separately. Then the two side panels can be pressed against the winding and welded to enclose the coil form, thus confining the conductor firmly against the out-of-plane load.

With two subdivisions in each TF coil and one turn per layer in each subdivision, the conductor stabilizer width will vary from ~ 0.5 m for the innermost layer to ~ 0.3 m for the outermost layer. This gradual reduction in width will fit the trapezoidal cross section very well. The thickness of copper stabilizer varies according to the magnetoresistance and the

radiation induced resistivity. Reasonable thickness per layer is needed to increase the out-of-plane rigidity and to avoid troubles in handling wide and thin metal sheet. A current of 60 kA per turn was tentatively determined; typical dimensions for copper sheet, stainless steel reinforced sheet and the proper superconductor cross section in each field region were chosen. The superconductor cross section was sized using a J-H curve with a temperature 0.5°K above the intended operating temperature. For example, at 10 T - 3°K operation, the J-H curve used is the one with $T = 3.5^{\circ}\text{K}$. It turns out that this amount of superconductor will enable the TF coil to be operated at 8 T peak field at 4.2°K with the same ΔT allowance.

As an illustration, a set of typical sheet conductor specifications is listed in Table IV-2. The conductor is graded into nine field grades with 3 T as the lowest field grade and 10 T as the highest field grade. This is because the outermost layer conductor in the outer TF leg will see a field of 3 T rather than zero. Substantial cost saving can be made if conductor is graded in this manner.

The proposed sheet conductor, as shown in Figure IV-19, is made of a copper sheet and many superconducting composite wires stranded around it. This stranding is necessary to eliminate the self field instability which will be significant if the operational current is large. The composite with proper Cu/NbTi ratio (approximately 2) is pretinned first and then soft-soldered with tin-lead alloy to the copper sheet to assure good bonding. A rolling process is then applied to achieve good conductor dimensional tolerance. There are several important advantages of using stranded composite conductor:

- (1) Unlimited length conductor can be made easily;
- (2) If the joints of each individual wire are properly spaced, there is very little heat generation per unit length joint;
- (3) Grading the conductor with the proper NbTi fraction can easily be accomplished;
- (4) The short sample test of high current conductor can be a tricky job, whereas with stranded conductor, it is a simple matter to determine the I-H curve of each strand.

Figure IV-19. Reference Sheet Conductor (60 KA) for TFC and its Coil Cross Section (Jelly Roll Winding)

The electrical insulation and the liquid helium channel are provided by about 1 mm thick by 1 cm wide fiberglass wetted with epoxy. The fiberglass epoxy is spiral wrapped around the sheet conductor covering 50% of the exposed surface. This will provide an exposed surface area of about 50 cm^2 per cm conductor length. The equivalent recovery heat flux per cm^2 is about 0.3 W. The electrical insulation for 1 mm conductor spacing is about 200 V for the turn-to-turn insulation, which is adequate. The coil form is stainless steel and is insulated with a micarta or G-10 sheet of 1 cm thickness. These sheets are grooved to provide vertical liquid helium channels for venting.

A stainless steel sheet of variable thickness is wrapped along the conductor. As far as hoop stress is concerned, the multilayer stainless steel serves as a multishell stainless steel pressure vessel. For the out-of-plane load, the stainless steel sheet increases the rigidity of the coil form tremendously. Since the radius of curvature near a 10 T region is 1.98 m, the overall average hoop stress in stainless steel is 26,000 psi and that in copper stabilizer is 14,500 psi. These stress levels are fairly conservative at 4.2°K temperature.

The sheet conductor design unavoidably leaves several questions unanswered:

- (1) The stability of sheet conductor needs further study, especially the current sharing characteristics and the heat transfer in such a long and shallow channel;
- (2) The stranding pitch is on the order of 1 m, it is not clear what the level of self-field instability will be;
- (3) Handling, especially the coil winding of such a wide and rather thin conductor might have problems; and
- (4) The mechanical strength and the stress level in the composite conductor, especially the resistivity of copper after the stranding and rolling process, may increase by some amount.

(b) Cable Conductor

If no field shield is provided, monolithic conductor or the sheet conductor with stranded composite will have excessive ac losses, and a cable conductor must be used. However, since the twisting reversal is not yet shown

to be practical, the only alternative is to use a cable with many strands fully transposed. The filaments of NbTi must be arranged in a circular ring at equal distance from the strand conductor axis so that filaments are fully transposed. Then there is no longitudinal coupling or transverse coupling. Within each strand, a sufficient amount of stabilizing copper is incorporated to fulfill the cryostatic stabilization criterion. Two stainless steel reinforced plates are used as the members supporting hoop stress within the conductor. A typical cable is shown in Figure IV-20. A typical strand is also shown in this figure. Note that the orientation of the stainless steel and the cable is such that eddy current loss is minimized. A stainless steel band is used to bond braided cables and the stainless steel supports together. Wetted G-10 fiberglass epoxy is used to further bond the cable together. Spiral-wrapping the G-10 provides turn-to-turn insulation as well as nonvapor-locking liquid helium channels for a TF coil in a standing configuration. Additional cooling passages are also provided in the stainless steel support to permit liquid helium penetration into the cable. Cable conductor operational current per turn should not be too high, otherwise, the packing factor is reduced and the cable will be quite "mushy".

(c) Coil Winding

Figure IV-21 depicts a coil winding line for a TF coil with the described sheet conductor. The conductor, after passing through the tension device, a cleaning bath and a sheet conductor roller, is wrapped with fiberglass epoxy. The stainless steel wrapped along the conductor may have higher tension to prestress the copper in compression. A winding fixture with spring fingers must be provided to press the conductor in place as the conductor passes the corner of the smallest curvature into the straight segment. Additional pressing devices, automatic or semiautomatic, must be provided along the straight segment to ensure that the conductor is pressed tightly on the inner layer conductor as the winding progresses. As the outermost layer of the upper subcoil is wound, the straight panel must be tightly pressed onto the last layer of stainless steel sheet and welded in place. The side panel will be pressed from the side and welded in place. The coil winding for the second subdivision will then proceed. The two divided subcoils will be connected in series through the partition rib at any angular location between $\theta = 0^\circ$ and $\theta \cong 110^\circ$ where there is ample space between the helium vessel and the last layer of reinforced stainless steel.

Figure IV-20. 10 KA Cable Conductor and its Cryostat

Figure IV-21. A Conceptual Winding Line for TFC

11. The Effects of Fabrication Tolerances

The effects of TF coil fabrication tolerance have been investigated numerically. The fabrication tolerances include the imperfection of coil winding, the tolerances of coil forms, the static g-load deflection, the error in the assembly and the possible errors due to thermal contraction. The TF coil is so large and so heavy that it is unreasonable to specify the fabrication tolerance with length accuracy of ± 0.1 cm and angular accuracy of $\pm 0.05^\circ$. Perhaps the most uncontrollable error is the error induced when the TF coil is cooled to 4.2°K. For this study, it is assumed that at 4.2°K, the length error may be as large as 2 cm while the angular error may be as large as 0.5° .

Fabrication tolerance has been studied in five categories as follows (see Figure IV-22):

- (1) The TF coil shape may be distorted roughly uniformly above or below the true pure-tension shape by about 5 cm.
- (2) The TF winding may be displaced radially or vertically by about 2 cm during the winding process, the cool-down process or during the assembly.
- (3) The TF coil may be rotated by about 0.5° in the toroidal direction during the winding, cool-down or assembly.
- (4) The TF coil may be tilted about the horizontal axis by 0.5° .
- (5) The TF coil may be twisted about the vertical axis by 0.5° .

It is clear that the effects of the first two fabrication errors will alter the transverse force pattern and induce bending moments in the otherwise pure-tension TF coil. Therefore, an in-plane hoop tension analysis must be performed. To simulate the error in category (1), the pure-tension coil evolution for all 16 coils was intentionally interrupted so that the coil shape is approximately 5 cm from the final pure-tension shape. The hoop stress analysis is shown in Figure IV-4. On the other hand, the coil displacement is done for one coil only and the result of these errors induce nonuniform hoop tension as shown in Figure IV-23.

Figure IV-22. Five Possible Fabrication Tolerances

Figure IV-23. Tension Distortion of Coil Displacement and Coil Shape Deflection

If the fabrication error falls into the third, fourth and fifth categories, the dominant effect is the unbalanced inter-coil force in the toroidal direction. This force is expressed as the force per meter TF coil length. These force distributions are plotted in Figure IV-24. It is seen that tilting and twisting by 0.5° will generate an additional attractive force of order of 1×10^5 kg/m. The rotating error is much more serious than tilting or twisting. Rotating by 0.5° , the additional inter-coil force is about 4×10^5 kg/m.

Since small displacements and rotations can be considered virtual motions, the force is proportional to these errors. Therefore, the effects of any other fabrication errors can be directly calculated using these data. It is interesting to note that fabrication errors also modify the periodicity of the field ripple in the plasma, with unknown effect upon the energy confinement.

12. Coil Protection and Magnet Safety Analysis

(a) Energy Release and Over-Voltage Protection

The proposed TF coil protection scheme is a series protection circuit, as described in reference 3. The only relevant comment to be added here is that the protection circuit with 16 switches has the problem of activating the 16 switches simultaneously. The superconducting magnet group at the Rutherford Laboratory has addressed this problem extensively.²³ The worst case is when 7 or 8 adjacent switches open late. The maximum voltage with respect to ground will be about 4500 V instead of the 1000 V that occurs if all 16 switches open simultaneously. To improve this situation, the switches can be opened in a sequential order to minimize the terminal voltage.

In the present design, the stored energy is 30 GJ in 16 coils at 10 T. The operational current is 60 kA and the total inductance is 16.7 H. This operational current is too large to allow one pair of leads out from each TF coil. Instead, only two leads, one from the number 1 TF coil and the other from the number 16 TF coil, are allowed to feed through the cryostat. All other connections between adjacent coils are done through intercoil tubing connecting the two adjacent cryostats. Therefore, for this design, the energy dumping circuit is shown in Figure IV-25. This is a standard method of energy dumping. The dump resistor must always be connected to the

Figure IV-24. Out of Plane Load Due to Errors of Rotation, Twist and Tilt

Figure IV-25. Dumping Circuit

coil. Should a normal front develop in any coil, the magnet terminal voltage of that coil will surge and the signal is used to disconnect the power supply. The coil system energy will be discharged through the dump resistor. The terminal voltage of the magnet will be no greater than the voltage across the dump resistor. The discharge voltage will be 2000 V. The resistance of the dump resistor will be 0.033Ω and the coil current will decay in a time constant of 506 s, or 8-1/2 minutes. This is a reasonably fast discharge. A faster discharge can be designed if the coil and current leads are designed with higher insulation.

Arcing is always a serious threat to a large superconducting magnet, a large safety margin must be used in designing the coil insulation and the current leads insulation. For example, the 4000 V coil insulation must be provided in order to allow a 2000 V operating condition. In fact, the entire system should be tested with a high voltage and extreme low current output power supply to test the insulation strength of the coil with respect to ground under the liquid helium environment.

As shown in Figure IV-25, a fuse with a small current capacity is connected in the grounding loop to the discharge resistor to ensure that no large grounding current shall flow if any point in the coil winding should accidentally be grounded. The discharge resistor must be tested at incrementally increasing current levels during initial system checkout. Multiple parallel paths should be provided in case interruption may occur in the discharge resistor.

(b) Over-Current Protection

Current control is provided by a circuit that compares the current read by the transducer (Figure IV-25) to a reference voltage in the power supply.

(c) Coil Forces and Structure Stress

The most critical hazards are those in which the design forces in the conductor and forces in supporting structures are exceeded. Therefore, strain gauges are used to monitor these situations.

(d) Cryostat Protection

Each TF coil cryostat shall be equipped with pressure rupture discs to guard against pressure buildup in the helium vessel. The pressure rupture disc can also protect the magnet coil in the event that large resistive heating

occurs with rapid pressure rise. Then, at a preset pressure, the rupture disc breaks and the bulk of the liquid helium will be rapidly transferred from the dewar to an emergency dump tank. This will remove liquid helium quickly and the whole coil will become normal and coil energy will be uniformly dissipated throughout the winding.

(e) Short Checking

Shorting from turn to turn, from layer to layer or from coil to ground is a very serious matter. The short will act as a discharge resistor. Should the coil energy be discharged, large amounts of energy deposition will occur at the short, and the conductor around the short will be melted into a copper block. Techniques for testing the short must be developed. Potential taps for checking shorts after the coil is wound must be incorporated.

(f) Current Leads Protection

If the discharge time constant is 8-1/2 minutes, then the current leads must be designed to allow adiabatic heating in that time period without raising the lead temperature dangerously high. The current leads are cooled by counter-flow helium gas. In-line gas flowmeters must be installed to monitor the amount of counter-flow helium gas. Alternatively, voltage taps across the leads are provided to monitor the current lead temperature. Temperature sensors may also be attached to the connector of the current leads.

Current leads are a weak link in magnet systems. The magnetic field and forces on the lead must be carefully evaluated. The leads must be firmly supported and must have sufficient insulation.

(g) Connectors and Conductor Joints

Connectors and conductor joints are another weak link in the magnet system. The resistance of each joint and each connector must be carefully tested and voltage taps must be provided across the joints and connectors to monitor possible failures. All forces exerted on the connectors and joints must be carefully computed. The mechanical strength of joints and connectors must be thoroughly tested. In some instances, a redundant path should be provided.

(h) Coil Temperature and Thermal Stress

Many temperature sensors such as thermocouples, semiconductors, carbon resistors and/or platinum resistors are used to monitor the coil temperature at various locations. The coil temperature must be carefully monitored to limit the thermal stress during cooldown. A liquid helium level indicator and/or temperature sensor is installed at the uppermost position to ensure that this part of the coil will stay below the liquid helium level.

(i) Power Supply Protection

The power supplies must be protected from overheating in such elements as the power diode and transformer and from interruptions such as water flow.

(j) Vacuum Vessel Protection

The vacuum vessel must be protected against over-pressure. An over-pressure rupture disc must be provided. The vacuum vessel must also be protected against the transient forces between the eddy current and the decaying magnetic field.

(k) Field Shield Protection

If the magnet is discharged in 8-1/2 minutes, large eddy currents will be developed in each vacuum vessel and the field shield. The interaction between the eddy currents and the decaying field may generate a large and complex force and ruin the field shield or radiation shields or structural members.

(l) Bending Moments

When two neighboring coils carry different currents, the unbalanced inter-coil force is large. Furthermore, pure-tension TF coils will remain in pure tension only if all coils carry the same currents. If two coils carry different currents, large bending moments and peak stresses will occur in the coil and subsequent structure failure may occur. Therefore, it is exceedingly important to make sure the currents are the same. This probably can be guaranteed only through series operation under all circumstances.

(m) Vapor Locking

For certain orientations and channel size of liquid helium channels, gas locking may occur around the coil winding. This is another hazard that could burn out a section of conductor and lead to a disastrous arcing.

(n) Other Safety Considerations

The TF coils are subjected to the interactions from other systems, such as the plasma loop, the poloidal coils (OH and EF) and the neutral beams, etc. These interactions will have to be borne in mind in safety analysis. For example, the TF coil discharge rate should be reasonably slow so that no large eddy currents are generated in the shield. The TF coil would normally be discharged after all poloidal coils are shut down. This will avoid large interaction forces.

13. Summation of Dissipation Loss and the Refrigeration Requirements

Heat dissipation in the TF coil system is summarized in Table IV-4.

(a) Nuclear Heating

The nuclear heating is 91 W per coil or a total of 1.5 kW for 16 coils, at the nominal neutron wall load of $P_w = 0.5 \text{ MW/m}^2$. This nominal wall load is used to compute the nuclear heating for both the 8 T and 10 T operation

(b) AC Losses in Aluminum Shield

The ac losses in the 18°K shield are 8.53 kW per coil and 136.5 kW for all 16 TF coils. The 3°K shield has 298.6 W per TF coil and 4.78 kW for all 16 TF coils.

(c) The Cryostat Loss

The radiation loss between the 18°K field shield and the 3°K helium vessel is negligibly small. The radiation heat load from the 300°K vacuum wall to the 18°K shield is 64.5 W per TF coil or 1032 W for all 16 coils provided that multiple layer superinsulation is used. The heat conduction loss through the support system is small and there is no loss for the inter-coil structure. The 60 kA leads will dissipate a total of 120 W.

Therefore, the total loss in the 3°K helium vessel is 6.40 kW for all 16 coils. The total loss in the 18°K normal metal shield is 137.5 kW for

all 16 coils. If the TF coil is operated at 8 T, then the coils will be at 4.2°K. The ac loss in the 4.2°K helium vessel will be reduced by a factor of 0.64 relative to the 3°K use. The ac loss in the 18°K aluminum shield will also be reduced by a factor of 0.64. This is because the ac loss mainly is produced by the EF coil and the plasma current. Therefore, at 8 T operation, the total losses in the 4.2°K helium vessel are about 4.22 kW for all 16 coils. The total losses in the 18°K normal metal shield are 88 kW for all 16 coils. These losses, as well as the ac losses without field, are tabulated in Table IV-4.

For the 3°K or 4.2°K refrigeration by boiling, the coefficient of performance, C.O.P., is given by

$$\text{C.O.P.} = \frac{T_o - T}{T} \times \frac{1}{\eta} \quad (\text{IV-14})$$

Where η is the efficiency of the actual machine relative to Carnot efficiency, T_o is the ambient temperature and T is the refrigeration temperature. For large machines, η is about 20%. So the C.O.P. for the 3°K refrigerator is 495 and the C.O.P. for the 4.2°K refrigerator is 350.

Refrigeration of the 18°K aluminum shield takes place over a temperature range from low temperature T_L ($= 12.5^\circ\text{K}$) to high temperature T_H ($= 23.5^\circ\text{K}$). The C.O.P. is given by

$$\text{C.O.P.} = \left[\frac{T_o}{(T_H - T_L) / (\ln T_H / T_L)} - 1 \right] \times \frac{1}{\eta} = 81 \quad (\text{IV-15})$$

Therefore, at 10 T operation the total input power for the 16 TF coil refrigeration is about 14.27 MW of which 11.1 MW is used in the field shield refrigeration and 3.17 MW is used in the 3°K refrigeration. For 8 T operation, the total input power for the field shield refrigeration is 7.1 MW and the total input power for the 4.2°K refrigeration is 1.48 MW. Therefore, at 8 T operation, the total input power required is 8.58 MW for all 16 TF coils.

14. Refrigeration System Evaluation

(a) Aluminum Shield

The aluminum shield is refrigerated at an average temperature of 18°K. The refrigerant inlet temperature is 12.5°K and the outlet temperature is 23.5°K.

Table IV-4. TF Coil Heat Dissipation Summary

Heat Dissipation Items	With Aluminum Field Shield (Sheet Conductor)				Without Field Shield* (Cable Conductor with Twisting Reversal)			
	10 T (3°K)		8 T (4.2°K)		10 T (3°K)		8 T (4.2°K)	
	Per Coil (W)	16 Coils (kW)	Per Coil (W)	16 Coils (kW)	Per Coil (W)	16 Coils (kW)	Per Coil (W)	16 Coils (kW)
Nuclear Heating	91	1.5	91	1.5	91	1.5	91	1.5
3°K/4.2°K Al. Shield ac Losses	299	4.8	191	3.06	--	--	--	--
18°K Al. Shield ac Losses	8500	136	5500	88	--	--	--	--
Thermal Radiation Load on 18°K Shield	65	1.04	65	1.04	--	--	--	--
Thermal Radiation Load on Helium Vessel From 77°K shield	--	--	--	--	10	0.16	10	0.16
Cryostat Heat Conductor Loss on Helium Vessel	10	0.16	10	0.16	10	0.16	10	0.16
Current Leads Loss	--	0.12	--	0.12	--	--	--	--
Parallel Field Conductor Loss**	--	--	--	--	296	4.74	217	3.5
Perpendicular Field Con- ductor Loss**	--	--	--	--	130	2.08	84	1.34
AC Loss in Coil Form	--	--	--	--	631	10.1	404	6.46
Summation of Loss at 3°K/4°K	400	6.5	300	4.8	1168	18.7	816	13.1
Summation of Loss at 18°K	8565	137	5565	89	--	--	--	--
Refrigeration Power (Com- pressor Input power)	14.3 MW		8.9 MW		9.3 MW*		4.7 MW*	

* AC Loss in Conductor reinforcing structural material is not included.

** Because of complex nature of B in the plasma ramping and EF Coil ramping, 2 s rise time is used in this calculation. The conductor is a hypothetical conductor used in the ac losses calculation.

This range was chosen on the basis of the graph shown in Figure IV-26. This shows refrigeration input power (for a 11°K temperature rise), the resistivity ratio of a typical aluminum, and the product of the two as a function of temperature. This product is proportional to the power required to cool the aluminum eddy current shield, assuming the current is independent of temperature. To cool the TF coil shield, a temperature rise of 12.5°K to 23.5°K was chosen, which leaves a 11°K margin before the power required starts to rise.

(b) 3°K Refrigeration System Comparison

The large refrigeration requirement at low pressure prompted the investigation of the possibility of refrigerating the TF coils by circulating supercritical helium through condensers in the TF coil cryostat to maintain the required saturation pressure (0.237/atm) as shown in Figure IV-27. It was hoped this might result in smaller piping and cold box size, and reduce the total volume at subatmospheric pressure. A comparison was made between a range of "dense gas," single expander, 3°K refrigeration cycles and a reference cycle utilizing 3°K liquid. The results are shown in Figures IV-28, IV-29 and IV-30.

The 3°K liquid cycle was chosen to simulate ordinary 4°K liquifiers with pumped bath added. The assumptions are: 2 expanders, high-side pressure at 15 atm, expander discharge at 1 atm, 100% efficient expanders, and 100% efficient heat exchangers. The refrigeration available in the cold return from the 3°K liquid is recovered in the counterflow heat exchangers.

The "dense gas" cycles cover a range of low-side pressures. In each case, the smallest pressure ratio possible to produce a 2.2°K expander discharge with an expander inlet of 3°K or greater was used. Expander and exchanger efficiencies are taken as 100%.

The power input ratio is based on assumed 100% efficient isothermal compression.

Table IV-5 shows that a reduction in first stage compressor displacement is possible, but at a rather severe penalty in input power. Consequently, the present TF coil refrigeration system is based on operating with vacuum pumps to maintain the desired operating pressure.

Figure IV-26. Optimum Operating Temperature of Aluminum Shield

Figure IV-27. 3°K Refrigeration Schematics

Figure IV-28. Relative Compressor Power for 3°K Refrigeration

Figure IV-29. Relative First Stage Displacement for 3°K Refrigerator

Figure IV-30. Relative Mass Flow Rate for 3°K Refrigerators

Table IV-5. Pressure Range of Dense Gas Refrigerator

Condenser Pressure = P_{out} (atm)	Compressor Discharge Required = P_{in} (atm)
2.3	30
3.0	35
5.0	40
7.0	45
10.0	50
15.0	60
20.0	65
30.0	70

(c) Refrigeration System

A central refrigeration facility is provided for the EPR magnet system. This facility is schematically shown in Figure IV-31. The TF coil helium vessel as well as the 3°K or 4°K aluminum shield, shall be cooled by a helium liquifier/refrigerant with a v pumping system and heat exchangers. Three expanders are provided to allow the refrigeration of the 12.5°K to 23.5°K shield as well as the 4°K liquification. Also shown is the refrigeration loop for the poloidal coil.

The 60 kA or 80 kA current leads are refrigerated by counter-flow high pressure cold gas. Nb_3Sn tape with copper stabilizer is used to raise the temperature of the lower end of the current leads. This design arrangement will reduce the heat leak of the current leads.

15. Cryostat Design

(a) Summary

The TF coil cryostat consists of a stainless steel coil case that also comprises the liquid helium container, an aluminum field shield, and an aluminum outer shell. Cryostat insulation consists of a high vacuum between the aluminum shield and helium container and high vacuum with multilayer insulation between the aluminum shield and the room temperature outer shell. The general cryostat arrangement is illustrated in Figure IV-10.

Figure IV-31. Schematics of Central Refrigeration Facility
(~ 15 MW Refrigeration Plant)

(b) Inner Shell

The inner shell of the cryostat will consist of stainless steel plate. The arrangement shown, with a central divider between the two windings stiffens the cryostat walls enough to allow the use of 1/2" plate. Shear panels between the individual cryostats will be used to resist the torque produced by the out-of-plane loads on the TF coils (Figure IV-32).

(c) Aluminum Shield

The aluminum shield consists of two portions. The portion of the TF coil adjacent to the central support cylinder will be shielded by aluminum attached directly to the cryostat wall. The portion of the TF coil away from the central support cylinder will be insulated from the cryostat inner shell and will be loaded to an average temperature of 18°K. The shield must resist large loads (Figure IV-14); therefore, the shield will be of aluminum-stainless steel composite construction to limit the amount of strain the aluminum is subject to.

(d) Outer Shell

The cryostat outer shell will be constructed of aluminum alloy plate. A saving in both weight and material cost, relative to stainless steel, can be realized.

(e) Insulation

Heat transfer due to radiation between the 18°K aluminum shield and the 3°K cryostat inner shell will be negligible relative to other heat sources. Therefore, no multilayer insulation will be used in this space.

The heat transfer due to radiation between the outer vacuum jacket and the radiation shield can be estimated as:

$$Q/A = \sigma \frac{1}{\frac{1}{E_1} + \left(\frac{A_1}{A_2}\right)\left(\frac{1}{E_2} - 1\right)} (T_2^4 - T_1^4) \quad (\text{IV-16})$$

Where σ is the Stefan Boltzman constant = $5.67 \times 10^{-8} \text{ W/m}^2 \text{ } ^\circ\text{K}^4$, A_1 is the inner area, A_2 is the outer area, T_1 is the cold wall temperature = 18°K, T_2 is the warm wall temperature = 300°K, A_1/A_2 is 0.9, and $E_1 = E_2 = \text{Emissivity} = 0.02$.

Figure IV-32a. TF Magnet Structural Support

T.F. COIL SUPPORT
CONTINUOUS SHEAR PANEL

Figure IV-32b. TF Magnet Structural Support
(Cross-section View)

Then, $Q/A = 5.67 \times 10^{-8} [(1/0.02) + 0.9(1/0.02 - 1)]^{-1} (300^4 - 18^4) = 4.9 \text{ W/m}^2$.
 For multilayer insulation, an apparent thermal conductivity of $\bar{K} = 5 \times 10^{-7} \text{ W/cm}^2\text{K}$ can be used between 300°K and 20°K for estimation purposes. The heat transfer to the aluminum shield then becomes:

$$Q/A = 1/L \bar{K} \Delta T = 5.56 \times 10^{-5} \text{ W/cm}^2 = 0.556 \text{ W/m}^2$$

The surface area for one TF coil is about 116 m². Thus, the heat transfer can be reduced from $116 \times 4.9 = 568 \text{ W}$ to $116 \times 0.556 = 64.5 \text{ W}$ by the use of multilayer insulation.

Multilayer insulation must be applied with broken conductive paths so that eddy current heating cannot occur. Even very small heating rates could raise the insulation temperature, resulting in degradation of insulating performance.

16. TF Coil Support

(a) Support Cylinder

The support cylinder holds up the TF coils at the inside of the assembly, and resists the radial, inward forces generated during operation. The vertical loads are transmitted through the assembly supporting the inner OH and EF coils, as shown in Figure IV-32. The combined load is transmitted through a low thermal conductivity member to the building foundation. The combined load, assuming one-half of the TF coil weight is borne by the support cylinder, is 8.6×10^5 pounds.

A support ring with an allowable compressive strength of 10,000 psi would require an area of 86 square inches. The calculated heat input between 300°K and 3°K, for a 1 m length of glass reinforced epoxy would be 4.2 W. Thus, the heat transfer due to support of the static load can easily be made negligible, compared to the other heat loads.

(b) TF Coil Outer Support

The TF coils will be supported at the inside, on the support cylinder and at the outside by an individual compression support member. This support will incorporate a hinge to allow for radial-thermal contraction.

(c) TF Coil Intercoil Support

During operation, out-of-plane loads are produced on the TF coils that produce a torque on the assembly. This torque will be resisted by a shear panel between each coil and its neighbors. Access holes will be provided for access to the interior of the TF coil assembly. The shear panel will operate at the same temperature as the TF coil cryostat.

(d) Aluminum Shield Support

The aluminum shield will be supported as shown on Figure IV-32. Vertical loads will be taken at the inside by attachment to the support cylinder assembly and at the outside by the outer TF coil support. The shield will intersect the latter at the 18°K point so that no conduction heat transfer takes place between the shield and the cryostat inner shell.

Torque produced by the out-of-plane loads on the shield assemblies will be resisted by a shear panel between each TF coil shield. The shear panel will be in two layers, one on each side of the coil cryostat shear panel to provide thermal radiation shielding.

(e) Additional Magnet Structure Design

A detailed analysis of the magnet support system was performed for a preliminary magnet system design with a somewhat different set of forces than were described in this chapter. This analysis, which is described in Chapter XII and Appendix D, established the feasibility of the shear panel concept that was selected for the reference design, and also examined a more conventional torque-frame concept.

B. Poloidal Coil System

The poloidal coil system consists of the initiation-trimming (IT) coils, the ohmic-heating (OH) coils and the equilibrium-field (EF) coils. Both the OH coils and the EF coils are located outside the TF coils and are superconducting in order to avoid large I^2R losses. The IT coil is made of water-cooled copper because it is a small coil system and also because it is located near the first wall, where the radiation flux is high. The IT coil is designed to provide about 4 V-s over a 10 ms time interval to initiate the plasma discharge. The IT coil could also be connected to trim the plasma position. Therefore, the IT coil plays a role in both the plasma breakdown and the

plasma equilibrium by providing higher values of $\dot{B}$ than the OH coil and the EF coil can generate. Only a scoping study of the IT coil is given here, while a preliminary conceptual design is carried out for both the superconducting OH coil and the superconducting EF coil.

Although the OH coil functions as the primary transformer for producing the plasma current and the EF coil provides the tokamak plasma equilibrium, both coil systems have nearly identical problems insofar as the magnet design and magnet technology are concerned. Both magnet systems have large stored energy of the same order of magnitude and large operational current; both are fast-pulsed magnets; and both mainly are of a ring coil configuration with the exception of the long solenoid in the OH coil system. For these reasons, the conceptual design of the EPR OH coil and EF coil is carried out together as if they were one magnet system. The EF coils, consisting of eight pairs of ring coils, are designated with coil numbers from one to eight while the OH coils, consisting of five pairs of ring coils and a pair of long solenoids, are designated with coil numbers from nine to fourteen as shown in Figure IV-3.

The design requirements for the OH and EF coil systems were specified as a result of a detailed trade-off study. Burn cycle dynamics simulations of the plasma, the coupled OH and EF systems and the plasma heating systems were performed, as described in Chapter III. Free-boundary plasma MHD equilibrium calculations were utilized in the design of an EF coil system.

The IT coils, perhaps with auxiliary heating, provide voltage for the plasma initiation. The OH coils will heat the plasma to 1 keV and induce ~ 5 MA of plasma current; and the EF coils, besides providing the moderate- β circular plasma equilibrium, will induce the final ~ 2.6 MA of plasma current.

Poloidal flux must penetrate the meter-thick blanket and shield to establish the equilibrium field in the plasma. The blanket and shield are made mostly of conductive metals. If the blanket and shield are not laminated, flux penetration will have a large time delay and phase shift. Large eddy currents will arise, and large field distortion will be seen by the plasma. Reduction of these effects to tolerable levels was examined, and a criterion for blanket/shield segmentation was developed.

1. Superconducting OH Coil Design

The main function of the OH coil system is to induce and maintain the plasma current. The plasma heating is accomplished by the combined effort of

the OH coil system and the neutral beam injection system. The design requirements for the OH coil system were established in trade-off studies in which various current reversal times for the OH coils and turn-on times for the neutral beam were studied. Results of these studies are described in Chapter III. The poloidal coil design calculations were based on a design-basis burn cycle. The design-basis cycle is shown in Figure IV-7. This cycle corresponds to the startup mode of case 3 in Table III-11. A complete cycle time of 75 s, of which 60 s is the burn time, was chosen for the design-basis cycle.

The total volt-second requirement for the reference design is 135. The OH coils are capable of reversing from -5 T to $+5$ T, supplying 85 V-s, with an OH flux core radius of 1.7 m. The EF coil will supply the remaining 50 V-s.

The OH coils are located outside the TF coil system so that the winding can be arranged to minimize the ac superimposing field on the TF coil. Since the OH coil is at full negative value before the onset of plasma startup, the OH coil windings must also be distributed so that the field produced by the OH coils at the plasma is less than 10 G or so. This requirement must be fulfilled in order to guarantee a stable startup. Another consideration for the OH coil winding arrangement is that the OH coil winding location shall not obstruct the access required for neutral beam injectors, vacuum ducts, primary coolant pipes and remote maintenance equipment. With these considerations in mind, the OH coils are arranged as shown in Figure IV-3. The winding dimensions are shown in Table IV-6 and some magnet characteristics are listed in Table IV-7, where R_1 , R_2 , Z_1 and Z_2 represent the inner radius, the outer radius, the initial axial coordinates and the final axial coordinates, respectively.

At 5 T central field, the total ampere-turns are 67×10^6 , the total conductor length is 847.4×10^5 A-m, and the average current density used is 2640 A/cm². The maximum operational current is 80 kA at the end of the burn cycle. The maximum power transfer is 1620 MW. The self-flux energy stored in the OH coils with 5 T central field is 1534 MJ and the inductance is 0.48 H. The mutual inductance between the OH coil and the plasma ring is 0.57 mH. The coupling coefficient is 0.2422. The energy stored in the coupling field system of OH coils and plasma is 1621 MJ of which 347 MJ is the self-flux inductive energy stored in the plasma loop.

Table IV-6. Poloidal Coil Winding Configurations

	Coil No.	Z ₁	Z ₂	R ₁	R ₂	ΔZ^a	ΔR^b	j_{avg}	Coil Pair	Coil Pair
		(m)	(m)	(m)	(m)	(m)	(m)	(A/cm ²)	NI (MAT)	Conductor Length (40 kA-m)
Equilibrium Field	1	3.25	3.584	12.0	12.15	0.334	0.15	2946	2.95	5599
	2	4.15	4.417	11.55	11.70	0.267	0.15	2946	2.36	4309
	3	7.0	7.50	8.85	9.0	0.50	0.15	2946	4.42	6195
	4	7.8	8.30	7.10	7.25	0.50	0.15	2946	4.42	4980
	5	7.90	8.40	5.60	5.75	0.50	0.15	2946	4.42	3939
	6	7.16	7.44	2.25	2.40	0.28	0.15	-2946	- 2.48	904
	7	6.48	7.571	2.10	2.25	1.091	0.15	-2946	- 9.642	3294
	8	6.35	7.081	1.95	2.10	0.731	0.15	-2946	- 6.46	2055
Ohmic Heating	9	0	6.5	1.62	1.77	6.50	0.15	2640	51.48	13707
	10	6.50	6.9	1.70	1.85	0.40	0.15	2640	3.17	883
	11	6.90	7.3	1.75	1.90	0.40	0.15	2640	3.17	903
	12	7.30	7.6	1.80	1.95	0.30	0.15	2640	2.38	700
	13	7.80	8.6	3.80	3.95	0.80	0.15	2640	6.34	3857
	14	4.03	4.1	11.55	11.70	0.07	0.15	2640	0.55	1012

Table IV-7. OH/EF Coil Magnet Characteristics

	OH Coil	EF Coil
Superconductor/stabilizer	Nb-Ti/Cu	
Coil design	Single layer	
Conductor design	Fully transposed cable	
Stability	Cryostatic	
Cooling	Pool boiling	
Operating temperature (°K)	4.2	
Average current density (A/cm ²)	2640	2946
Magnetic field (T)		
in flux core	~ 5	
at plasma center		~ 0.46
Ampere-turns (MAT)	67	± 18.6
Total conductor length (MA-meters)	847	996
Maximum dB/dt in conductor (T/s)	6.7	~ 1
Stored energy in OH/EF/plasma field (MJ)	2262	
Maximum operational current (kA)	80	80
Number of turns	837	464
Self-inductance (H)	0.48	0.52
Mutual coupling	$K_{OHEF} = 0.0153$	
Power supply voltage (kV)	48	21
Volt-seconds to plasma (V-s)	85	50
Coupling coefficient to plasma ring	$K_{OHP} = -0.2422$	$K_{EFP} = -0.2566$

It is a difficult task to design an 80 kA cable with full transposition and good mechanical stability. The problem with high-current, fully-transposed cable is the sponginess of the cable. It is therefore proposed to achieve 80 kA operational current by two parallel conducting paths, with each path carrying 40 kA with 40 kA fully-transposed superconducting cable. The two parallel paths shall have identical impedance in order to have equal current sharing. The easiest way to do this is to make use of the obvious symmetry of OH coil winding with respect to the median plane.

2. Superconducting EF Coil Design

If the EF coils were placed inside the TF coils, there would be tremendous difficulties in coil assembly, disassembly, support, repair and maintenance, as well as in the assembly, disassembly and maintenance of the blanket and shield and the first wall. To avoid these problems, the EF coils are located outside the TF coils.

The OH coils would induce large voltages in the EF coils, if the EF coils were not decoupled from the OH coils. The decoupling ampere-turns should be positioned so that they produce small anti-vertical fields and small superimposing fields on the TF coils.

The EF coils were designed to produce 50 V-s to the plasma, thereby reducing the volt-second requirements for the OH coils.

The placement of the EF coils and the currents in them were determined as follows. A first iteration on the vertical field in the plasma was obtained from a fixed-boundary plasma MHD calculation in which a circular plasma cross section was specified. Then, a set of coils and currents was picked that would generate a vertical field pattern as close as possible to this vertical field (see Figure III-5). With the EF coils relatively far from the plasma and with the inclusion of the decoupling coils, which have currents in the wrong direction to provide equilibrium, it was not possible to exactly reproduce the desired vertical field. The EF coil currents were then modified slightly, with the coil positions fixed, to produce a more nearly circular shape using the free boundary equilibrium calculations described in Appendix C. The resulting coil configurations are shown in Table IV-6, the actual plasma

boundary is shown in Figure IV-33 and the flux surfaces for the entire equilibrium field and plasma are shown in Figure IV-34. The bulge in the plasma cross section is due to the decoupling coils. The plasma is sufficiently close to circular, however, that the free-boundary equilibrium quantities (β_p , β_t , q , I_p) calculated with the actual coil configuration are essentially the same as those calculated in Section III-A, with the boundary constrained to be exactly circular.

The magnet characteristics of the EF coils are shown in Table IV-7. Coils number 1 through number 5 are designed to produce the vertical field. These coils have positive ampere-turns. Coils number 6 through number 8 are used to decouple the EF coils from the OH coils. Complete decoupling requires a more precise coil arrangement. The achieved decoupling is measured by the coupling coefficient between the EF coils and the OH coils. This coefficient is 0.0158. Therefore, fairly good decoupling has been achieved.

The EF coils supply a total of 50 V-s to the plasma loop over the entire cycle; all of this is delivered to the plasma during startup. The total ampere-turns are zero, with positive and negative ampere-turns each equal to 18.575×10^6 . For the positive ampere-turn coils, the total conductor length is 996×10^6 A-m. The negative ampere-turn coils require a conductor length of 275×10^6 A-m. The 80 kA operational current is achieved by two parallel connection paths above and below the median plans as described in the OH coil section. A total of 928 turns of 40 kA cable is needed. The equivalent self-inductance is 0.52 H and the self-flux stored energy is 1663 MJ. The mutual inductance between the EF coils and the plasma is 0.628 mH and the coupling coefficients between them is 0.2566. The maximum power transfer is 389 MW. The maximum charging voltage of the EF coils is about 21 kV for 80 kA operational current. At the design field, the average current density is 2947 A/cm².

The problems of the cable design, the coil design, the cooling and ac losses, the magnet stability and protection, the mechanical forces and the cryostat design will be investigated in the subsequent sections.

Figure IV-33. Circular Plasma Boundary

Figure IV-34. Flux Contour

3. Plasma Initiation-Trimming (IT) Coil

In order to initiate the plasma discharge, a large toroidal electric field will be required over an interval of about 10 ms. As shown in Section III-C, about 4 V-s will be required. It will also be necessary to have a field-free region in the toroidal vacuum chamber so that the initial plasma will not be deflected by stray fields. The special requirements of this system are difficult to satisfy by means of the OH coil, so a separate system has been designed. These coils, which should also be useful for controlling the plasma position, are shown in Figure IV-35.

The IT coil system consists of four coils located near the first wall and two coils located on the outside of the blanket and shield above and below the neutral beam ports. All of these coils could be made of copper. The particular configuration shown uses currents of 400, 32 and 16 kA-turns to produce a total of 4 V-s when the coil system is discharged into a resistor. A magnetic field null is also produced by this coil system over a large area of the vacuum tank. The region where the magnetic field is less than 10 G is roughly elliptical in shape with a major axis (parallel to the major radius of the torus) of 3 m, and a minor axis of 1 m. Using this coil system, the plasma could be started at any position in the outside half of the vacuum chamber.

This system of coils should also be useful for control of plasma position. By connecting the coils in opposite polarities, horizontal and vertical fields can be produced in the torus with about 2 G/kA-turn in the coils. The requirements for field homogeneity in the control system have not yet been studied. It is possible that more coils may be needed if a more homogeneous field is required.

The circuit that controls the IT coil is also sketched in Figure IV-35. The coil is charged up by a power supply and discharged into a resistor to produce the required electric field. Charging up would take 0.3 s and discharge would take 10 ms. In order to vary the shape of the discharge wave form the discharge resistor should be variable, probably by shorting out sections of a fixed resistance. Since the IT coil will couple with the OH coil, it will be necessary to decouple them by positioning decoupling coils above and below the long solenoid of the OH coil. In Figure IV-35, bucking coils have been introduced to do this.

IV-88

After the IT coil has discharged, the plasma in the torus should be stably maintained by the change in the OH and EF coil currents, which should produce a driving voltage, V_{loop} , of about 25 volts. At this time, the IT coil could be reconnected to the trimming power supply, which will enable it to control plasma position.

4. Axial Forces, Hoop Stresses and Coil Interactions

The axial force on an infinitesimal circular current element, Idl , arises from its interaction with the radial field component, B_R . This force is given by

$$dF_Z = (Idl)B_R \quad (IV-17)$$

Cylindrical coordinates are used so that R is the radial coordinate of the element, Z is the axial position of the element and ϕ is the azimuthal coordinate. Substitute $B_R = -\partial A_\phi(R, Z)/\partial Z$ into the above expression and the axial pressure on the element is given by

$$dp_Z = \frac{dF_Z}{dZdR} = jB_R dZ = -j dZ \frac{\partial A_\phi}{\partial Z} \quad (IV-18)$$

Let Z_1 be the axial coordinate of the coil starting Z -plane and Z_2 be the axial coordinate of the coil final Z -plane. Then the axial pressure at a point specified by (R, Z) is given by

$$P_Z(R, Z) = \int_{Z_1}^Z dP_Z = \int_Z^{Z_2} dP_Z = -j[A_\phi(R, Z_2) - A_\phi(R, Z)] \quad (IV-19)$$

The total axial force on any coil plane Z is given by

$$F_Z(Z) = \int_Z^{Z_2} 2\pi R dR P_Z(R, Z) \quad (IV-20)$$

$$= 2\pi j \int [A_\phi(R, Z) - A_\phi(R, Z_2)] R dR.$$

The axial pressure and the axial forces were computed by a computer code "FORCE".²⁴

In the EPR poloidal coil system, since all of the fourteen poloidal coil pairs and the plasma ring contribute to the vector potential $A_\phi(R, Z)$ at any poloidal

coil winding, the axial force and pressure of each coil depends on excitation of all coils. Therefore, it is difficult to predict either the magnitude or the direction of the vertical forces. It is entirely possible that during complete cycle the direction of vertical force at any coil reverses and that the magnitude of the vertical force changes rapidly in disproportion to self-excitation of that coil. Figure IV-36 shows the vertical force per meter coil length during the burn cycle with 7.58 MA plasma current, 5 T central field in OH coils and full excitation in EF coils. Note that some coils are subjected to repulsive forces while other coils are subjected to attractive forces. Note that some adjacent ring coils are subjected to vertical forces with opposite direction. The long solenoid is expected to have the largest vertical force because it has the most ampere-turns of all. Yet it is surprising to find that coil number 8 has rather large repulsive forces.

To obtain full information on vertical forces, the vertical forces on each coil were computed at different times during the entire reference cycle (see Figure IV-7). The results are shown in Figure IV-37. Also investigated are the vertical forces on each coil when the plasma current suddenly quenches, a highly probable case for an EPR. It is interesting to note that, for all cases investigated, the vertical forces for coil numbers 2, 3, 4, 5, 6, 7, 11 and 13 will reverse their directions and coil supports for these coils must provide support in both directions.

The vertical force on the long solenoid is a body force and little structural support is needed. The vertical forces on coil numbers 6, 7, 8, 10, 11 and 12 will be transferred to the TF coil support cylinder with restraining rings for repulsive forces, as shown in Figure IV-32. Fortunately, the remaining poloidal coils (namely, coil numbers 1, 2, 3, 4, 5, 13 and 14) have rather weak vertical forces (no greater than 83×10^3 kg/m). Support for these coils and their cryostat conceptual design is discussed in Section IV-B.

Although the axial compressive stress for an ordinary solenoid is generally small in comparison with hoop stress, the axial pressure on the poloidal coils may be quite high because these axial forces result from many coil interactions. Computations of axial compressive pressure distributions for all poloidal coils are shown in Figure IV-38. It is seen that most poloidal coils have small compressive pressures (less than ~ 100 kG/cm² or 1420 psi) with the

Figure IV-36. Vertical Force per Unit Length During Burn Phase

Figure IV-37. Axial Force per Unit Coil Length (10^3 kg/m)

Figure IV-38a. Axial Pressure, P (kg/cm²) and Vertical Force, F (tons).

Figure IV-38b. Axial Pressure, P (kg/cm²) and Vertical Force, F (tons).

exception of coil numbers 7, 8 and 9. The maximum compressive stress in coil numbers 7, 8 and 9 are 240 kg/cm², 301 kg/cm², and 592 kg/cm², respectively. These do not present mechanical problems.

The hoop force, or radial force on a current element, $Id\ell$, arises from its interaction with the axial field component, B_z . This force is given by

$$dF_R = (Id\ell)B_z = (jdRdZd\ell)B_z . \quad (IV-21)$$

The circumferential tension on the current element is given by

$$dT = (dF_R/d\ell) \cdot R = (jdRdZ)R B_z , \quad (IV-22)$$

where R is the radius of curvature of the current element. The tensile stress, or hoop stress, is given by

$$\sigma_t(R, Z) = \frac{dT}{dRdZ} = jRB_z . \quad (IV-23)$$

Computation of hoop stresses in a multilayer solenoid is a complicated problem because the hoop forces on any given turn will depend not only on the jRB_z of that turn but also on the interaction body forces resulting from the other turns pushing on it. The exact solution is further complicated by the inhomogeneous coil structure. For example, it is difficult to know exactly the behavior of insulators mixed among the copper and structural reinforced material such as stainless steel.

It is proposed (see Section IV-B-6) that the poloidal coils in the EPR shall have one layer to avoid the high voltage insulation problem. Therefore, the hoop stress in any turn is independent of all other turns. The average hoop stress is given by

$$\bar{\sigma}_r = j_{avg} \bar{R} \bar{B}_z , \quad (IV-24)$$

where $\bar{R}$ is the mean radius of the turn and $\bar{B}_z$ is the axial field component averaged over the turn cross section.

The averaged hoop pressure or magnetic pressure, assuming no body force interaction from neighboring turns among successive layers, can easily be shown to be

$$\bar{\sigma}_r = j_{avg} t \bar{B}_z, \quad (IV-25)$$

where t is the radial thickness of the turn.

Maximum values of σ_t and σ_r occurs near the maximum B_z region. The computed $\bar{\sigma}_t$, $\bar{\sigma}_r$, $\sigma_{t \max}$ and $\sigma_{r \max}$ for the poloidal coils are tabulated in Table IV-8. It is interesting to see that coil numbers 6, 7, 10, 11 and 12 have hoop compression rather than hoop tension. Also note that coil numbers 5, 6, 8, 13 and 14 have high-averaged hoop stress and must have a substantial amount of reinforcement materials in the winding. The radial magnetic pressures for all coils are not high and they present no problems at all.

5. Coil Stability and Magnet Protection

The superconducting poloidal coils have a large stored energy; thus, their design must be based on full stabilization. It is expected that the magnet can be reliably designed and that the magnet can be charged beyond short sample performance by undergoing current-sharing phenomena. However, it is important to realize that since the superconducting poloidal coils will be charged and discharged in 2 to 6 s, the problems associated with these coils' quench are much smaller than those that would arise if the TF coils quenched. Therefore, one may argue that for an EPR, the poloidal coils may be designed with less stringent requirements on coil stability than the TF coils. For a commercial reactor, however, the high reliability required of a generating plant will demand that the poloidal coils shall not quench once in several years.

The ac magnet cryostatic stability will be more complicated than the dc magnet cryostatic stability. In addition to those considerations discussed in Section IV-A-9, achieving ac magnet cryostatic stability depends on accurate knowledge of ac losses in superconductor, matrix, structural materials and in the helium vessel. It also requires accurate knowledge of strain energy released if the stress in any material exceeds its elastic limit. The constant venting of helium gas, the detailed motion of gas bubbling and the effect on

Table IV-8. Hoop Stress and Radial Pressure of Poloidal Coils During Burn Phase

Coil No.	j (A/cm ²)	t (m)	$\bar{R}$ (m)	$\bar{B}_Z$ (T)	$\bar{\sigma}_t$ (kpsi)	$\bar{\sigma}_r$ (kpsi)	B_Z max (T)	$\bar{\sigma}_t$ max [*] (kpsi)	$\bar{\sigma}_r$ max [*] (kpsi)
1	2946	0.15	12.07	0.012	0.62	0.007	2.1	108	1.3
2	2946	0.15	11.63	0.29	14.3	0.19	2.3	114	1.5
3	2946	0.15	8.93	0.13	4.9	0.08	2.4	92	1.5
4	2946	0.15	7.18	0.39	12.0	0.25	2.66	81	1.7
5	2946	0.15	5.68	0.83	19.8	0.53	3.1	75	2.0
6	-2946	0.15	2.33	2.32	-22.8	-1.48	4.14	- 41	-2.6
7	-2946	0.15	2.18	0.12	- 1.13	-0.08	-4.1	37	2.6
8	-2946	0.15	2.03	-2.83	24.1	1.81	-5.6	48	3.6
9	2640	0.15	1.70	1.41	9.1	0.8	4.8	31	2.7
10	2640	0.15	1.78	-2.11	-14.2	-1.2	-5.01	- 33.6	-2.7
11	2640	0.15	1.83	-1.20	- 8.2	-0.68	-4.32	- 29.5	-2.5
12	2640	0.15	1.88	-1.24	- 8.8	-0.71	-3.94	- 28	-2.2
13	2640	0.15	3.88	1.38	20.3	0.79	3.62	53	2.1
14	2640	0.15	11.63	0.37	16.7	0.21	1.69	74.5	1.0

*Based on Noncooperative single turn model, so these values will have large errors.

the heat transfer flux must also be fully understood. The coil forces and stresses are of a dynamic nature. The mechanical disturbances will be quite different from that in a dc magnet. The problems of material fatigue and fracture and their effect on the superconductor current density are all important.

The OH coils are proposed to oscillate with a homopolar generator, and the EF coils are to be driven by a bridge power supply. Since both the OH coils and the EF coils will be charged or discharged in 2 to 6 s, energy will be returned to the power supply system. Coil protection with dumping resistors is not necessary. If the OH coils and EF coils are designed with full stabilization or nearly full stabilization, the velocity of normal front propagation will be negative, zero, or in the worst case, a tiny positive value of 1 m/s at most. The energy dissipation within the coil, in a time interval of 2 to 6 s, will be negligibly small.

Perhaps the most critical hazards for the poloidal coils are the coupling effects between the poloidal coils and plasma, and the coupling effects among themselves. In the event of a plasma quench, large voltage, currents and forces will be induced. The voltage might cause insulation breakdown. The currents might exceed the poloidal coil critical currents by a large amount and the coil current might run away with a large velocity of propagation. The power supply may not be capable of handling the large power transfer. These complicated coupling effects should be fully analyzed.

Other items of magnet protection and safety analysis, as discussed in Section IV-A-12, are equally applicable to the poloidal coils protection and safety precautions.

6. Conductor Design and Coil Structure

The operational current for both the OH coils and EF coils is 80 kA, supplied by two parallel 40 kA cables with fully transposed strands. The OH coils will have 837 turns in each parallel path. The OH coils will be cycled by a homopolar generator with peak voltage of 48 kV across the OH coil terminals if the OH coils are charged from - 5 T to + 5 T in 2 s. The EF coils will have 464 turns in each parallel path. The charging voltage will be around 21 kV across the EF coil terminals. Hence, turn-to-turn voltage will be about 60 V for the OH coil and about 50 V for the EF coil. The

breakdown electric field in a helium gas environment is about 260 V/mm. Therefore, minimum separation between two adjacent turns in the same layer is about 1.3 mm for the OH coils and 1.17 mm for the IF coils.

It is clear that multilayer coils will require large gaps between layers. This presents an awkward problem for coil design. It is especially true in the long coils such as the long solenoid of the OH coils. Although good dielectric materials such as G-10 fiberglass may be used to fill the gaps between layers, the magnet designer will still face problems because it is very likely that pinholes or cracks may exist in these materials when the magnet is energized. Therefore, the proposed OH coils and IF coils will be wound with a single layer conductor. The width of the conductor is 15 cm. Based on the information of axial compressive stress, this width will be adequate.

The IF coils and OH coils will be cooled by pool boiling at 4.2°K and 1 atm pressure. Pool boiling is simple, inexpensive, reliable and easy to control. Above all, it will be shown in the next section that a rather small heat transfer flux is adequate to remove the conductor ac losses. Under this circumstance, the heat transfer flux ceases to be an important factor in determining the coil stability. The coil stability will depend on the conductor current density level, the amount of liquid helium surrounding the conductor and the extent of coil disturbances. However, it is important to recognize that pool boiling will work for an ac magnet only if the helium bubbles, which are generated at a constant rate, can be properly vented to avoid bubble accumulation within the winding. This requirement further justifies the decision for a single-layer coil.

The 40 kA cable can be achieved by cabling a basic strand of 0.3 mm diameter as shown in Figure IV-39. In the basic strand component, the copper to NbTi superconductor ratio is 2 to 1, with the NbTi cross-sectional area $2.3 \times 10^{-4} \text{ cm}^2$, or 1171 filaments with 5 μ filament diameter. For a poloidal coil with a 5 T peak field, the conservative value of NbTi critical current density is about 117 kA/cm². Therefore, the basic strand shall carry 27 A. As shown in Figure IV-39, to assure current sharing with a higher Cu/NbTi ratio, 20 pure copper wires of 0.3 mm diameter were transposed along the 10 composite strands. High resistivity soft solder is used to solder these wires.

Figure IV-39. 40 KA Cable for Poloidal Coils

together around a central structural-reinforcing element. This raises the overall Cu/NbTi ratio of the basic cable to 11 to 1. This design offers good coil stability and good mechanical bonding for each strand with low ac losses in mind. The composite with a single-component stabilizer rather than a cupronickel and Cu two-component stabilizer was chosen because of the rather poor conductor stability if cupronickel is incorporated. Therefore, as long as the ac loss is not too great, a conductor with a single-component stabilizer will be preferred. The basic cable shall carry 270 A at 5 T.

The central element, as illustrated, could be a stainless steel wire of 1.2 mm diameter if the hoop stress is large. For a small hoop stress, the element may be made of cold-worked copper. The material and size of the central element shall depend on the coil forces.

The 40 kA cable was finally achieved by transposing 150 basic cables into a height of 2 basic cables (~ 4 mm) by a width of 75 basic cables (~ 150 mm). The basic cable must be insulated with nomex paper before transposition. Otherwise, very large coupling eddy currents and ac losses will arise in the wide cable. Since the stability of the basic cable is built into the stability of the 40 kA cable, the fact that the basic cable is insulated probably will have no great effect on the stability of the 40 kA cable.

Finally, the 150 basic cables are banded together with spiral wrapping wetted fiberglass of 2 mm in thickness.

As shown in Figure IV-40, the poloidal coil is wound with a single layer wide cable of 40 kA.

The spiral wrapping fiberglass of 2 mm in thickness will provide a turn-to-turn separation of at least 4 mm for adequate insulation. The width of the fiberglass is 2 cm and the wrapping pitch is 3 cm so that the liquid helium channel of 1 cm wide by 0.2 cm deep by 15 cm long will be provided on both faces of the cable. The bottom supporting plate is machined to have an inclined surface in the radial direction so that gas venting will move radially outward. An 0.1 mm nomex paper is used between turns to prevent the bubbles from rising through the turns. These measures should provide good bubble venting, separate the gas-liquid flow and allow each turn to see only the bubbles generated by itself.

Figure IV-40. Poloidal Coil Structure and its Cryostat Configuration

The wrapped fiberglass is expected to be strong enough to transfer the hoop tension to the external rings, which are made to assist the hoop tension of the cable. The inner rings are made to overcome the hoop compression as well as to serve as the bobbin for winding. Many 2 mm wide slits are cut in these rings to allow gas venting and liquid circulation.

The axial compressive or repulsive forces of every 10 to 15 turns are supported by a 2-cm thick G-10 or micarta plate. This arrangement shall reduce the accumulation of axial compressive or repulsive pressures exerting onto the cable, which otherwise might short out the basic cables and deteriorate the coil stability.

7. AC Loss Estimation and Refrigeration Requirements

In estimating the ac losses in the poloidal coils, the poloidal coil numbers 1 through 5 will be classified as Group I because they have about the same peak field of 3 T and same dB/dt of about 0.5 T/s. The poloidal coils numbered 6 through 8 will be classified as Group II because they have a peak field of 5.5 T and dB/dt of about 1 T/s. The remaining poloidal coils (coil numbered 9 through 14) are OH coils and will be called Group III, with peak field of about 5 T and dB/dt of 5 T/s.

The total conductor length of Group I is 996×10^6 A-m and the superconductor current density is 1.8×10^5 A/cm². Therefore, the amount of superconductor volume is 0.565 m³. The total conductor length of Group II is 275×10^6 A-m and the J_c is 1.0×10^5 A/cm². The NbTi volume is 0.23 m³. The Group III conductor length is 847×10^6 A-m and J_c is about 1.2×10^5 A/cm². The NbTi volume is 0.87 m³. Assume the 3 group coils all have 5 μ filaments, and recall that Group III superconductor will be subjected to a cycle of $-5 \text{ T} \rightarrow +5 \text{ T} \rightarrow -5 \text{ T}$ while the Group I and Group II will have a cycle of $0 \rightarrow B_{\max} \rightarrow 0$. The hysteresis losses per cycle will be

$$\text{Group I, } E_f = 14,980 \text{ J}$$

$$\text{Group II, } E_f = 7,870 \text{ J}$$

$$\text{Group III, } E_f = 61,920 \text{ J}$$

For a cycle time of 75 s, the time-averaged hysteresis dissipation is 1130 W of which 826 W are lost to the OH coils. Therefore, the OH coils may require even smaller superconducting filaments than 5 μ .

To estimate the eddy current losses in the matrix, it is easy to see that the eddy current losses in the basic cable will dominate the eddy current losses in the composite strand. Therefore, we replace the filament twisting pitch by the strand pitch, which is assumed to be 8 mm. The amount of matrix material involved in eddy current losses must include the volume of stabilizer copper wires. The central stainless steel element will slightly affect the coupling because its resistivity is 5×10^{-7} Ω -m, probably a factor of 100 larger than effective transverse resistivity. Let us assume that the high resistivity solder will affect the transverse resistivity, ρ_m , so that $\rho_m = 5 \times 10^{-9}$ Ω -m; if this is not so, other means such as a cupronickel barrier must be used to reduce the transverse resistivity. Recall that the Group I and Group II conductors will experience a linear field rise and fall, namely, $0 \rightarrow B_{\max} \rightarrow 0$ in a complete cycle, while the Group III conductors will experience a cycle of $-5 \text{ T} \rightarrow +5 \text{ T} \rightarrow -5 \text{ T}$ with $dB/dt = 5 \text{ T/s}$. The matrix losses per cycle are given by

$$\text{Group I, } E_m = 3,187 \text{ J}$$

$$\text{Group II, } E_m = 4,361 \text{ J}$$

$$\text{Group III, } E_m = 177,235 \text{ J}$$

Clearly the matrix losses for the OH coils is large. The cable proposed in the preceding section should be modified to reduce the matrix transverse resistivity or changed to a smaller basic cable so that the stranding pitch length can be reduced. Based on these ac loss estimations, the averaged power dissipation in the matrix is 2464 W.

The total ac losses averaged over a cycle of 75 s are 3594 W at 4.2°K, requiring an input power of 1.17 MW for the liquid helium facility of the poloidal coil system.

As discussed in the next section, the poloidal coil cryostats are of non-metallic material with a thin metal barrier (~ 5 mills) for preventing helium permeation. The structural heat conduction loss for the axial force support was computed to be 331 W for all poloidal coils. The radiation heat load is

about 80 W. Therefore, the grand total refrigeration requirement is 4005 W at 4.2°K or 1.31 MW at room temperature.

Finally, let us examine briefly the cryostatic stability of the OH coils under the severe ac losses. The total surface area of the OH coil basic cable is 986 m². Assume that only 5% of its surface is exposed to liquid helium; then the surface area is 49.3 m² or 493,000 cm². The total ac loss per cycle is 239,155 J. The amount of ac loss is evenly generated in a 4-s pulsing time so that the heat transfer per second is

$$h = \frac{59,789 \text{ W}}{493,000 \text{ cm}^2} = 0.12 \text{ W/cm}^2.$$

It is also easy to show that each basic cable with 20 copper wires is cryostatically stable according to the classic full stabilization criterion.

8. Poloidal Coil Cryostat Design

(a) Inner Shell

Figure IV-40 illustrates a cryostat configuration for a poloidal coil. To reduce eddy current losses to a minimum, an essentially non-conductive inner shell is required. The illustrated design uses glass-reinforced epoxy, with an 0.013 cm layer of stainless steel for a helium permeation barrier. The construction technique consists of fabricating the shell by filament winding or braiding around the coil as shown in Figure IV-41. The stainless steel barrier is installed after enough layers are built up to support it. Then fabrication continues until the required thickness is obtained.

(b) Outer Vacuum Jacket

The outer vacuum jacket is constructed of aluminum alloy.

(c) Insulation

The cryostat is insulated with high vacuum, plus a liquid-nitrogen-cooled radiation shield of laminated construction to minimize eddy current heating in the nitrogen shield. Multilayer insulation is used between the outer vacuum jacket and the nitrogen shield. The multilayer insulation is installed with the reflective coating in segments to avoid formation of eddy currents in the insulation layers.

Figure IV-41. Fiberglass braiding machine for nonmetallic poloidal coil cryostat
(courtesy of McDonnell-Douglas Astronautics Company)

(d) Inner Supports

The individual coils are supported internally as illustrated in Figure IV-40 with low thermal conductivity members. These supports are designed to take loads in both directions. The high loads per unit length of coil are distributed between supports by a stiff cryostat inner shell.

(e) External Supports

Support for the magnetic loads on poloidal coil numbers 1, 2, 3, 4, 5, 13 and 14 are provided by a support "tree" at the top and support columns at the bottom (see Figure XII-2). Sixteen radial beams are used between the inside central column and the outermost or coil support column.

(f) Refrigeration

Refrigeration of the poloidal coil system is by boiling liquid helium at a nominal one atmosphere pressure as shown in Figure IV-31. The system will be operated as a refrigerator to recover the refrigeration available in the helium vapor return.

9. Equilibrium Field Flux Penetration on the Blanket and Shield

The magnetic field from the EF coils must penetrate the blanket and shield to act on the plasma. The blanket and shield contain much electrically conductive material, particularly stainless steel. Eddy currents in this material would distort the EF and delay its penetration if the blanket and shield were not sufficiently segmented. As will be shown below, the blanket and shield design of 16 segments, each made of 43 blocks, imposes very little field distortion and delay of penetration.

Table IV-9 summarizes the results for calculations of the field induced at the center of the plasma due to the eddy currents in the blanket and shield. Results are expressed as the ratio of the induced field to the applied field and as a phase and time delay.

The equations are developed in Appendix F, and the assumptions of the calculations are discussed there.

(a) Unsegmented Blanket and Shield

For the unsegmented blanket and shield, the field can be found by Equation (IV-26).

Table IV-9. Induced Field in the Plasma Region Due to Eddy Currents in the Segmented or Subdivided Blanket and Shield

<u>Toroidal Segments</u>	<u>Blocks per Segment</u>	<u>Maximum Induced Field B_{in}/B_o</u>	<u>Phase Delay ϕ</u>	<u>Time Delay t_o</u>
1	1	167% ^a	-	-
8	1	42% ^a	-	-
16	1	9.3%	10.5°	117 ms
32	1	2.3%	2.6°	29 ms
48	1	1.0%	1.1°	12 ms
64	1	0.6%	0.7°	8 ms
16 ^b	36	1.5%	1.8°	20 ms

a) Assumptions of model not valid for these cases.

b) The 43 blocks per segment in the reference design are represented by 36 blocks per segment in the computational model.

$$B_{in} = \frac{-\mu_o}{2} h a \dot{B}_o / \rho, \quad (IV-26)$$

where h = the blanket and shield thickness = 1.30 m

a = the blanket and shield mean radius = 3.05 m

ρ = the electrical resistivity; for stainless steel = $0.74 \times 10^{-6} \Omega\text{-m}$

$\dot{B}_o$ = the time derivative of the applied field, and is taken to be $B_o/2 \text{ s}$

Using these values yields

$$B_{in} = 1.67 B_o.$$

This result, that the induced field is larger than the applied field, is clearly non-physical; in fact the induced fields would induce additional currents that in turn would produce fields, adding the net induced field to a value

sufficient to cancel the applied field. However the result does show that eddy current effects are too large, and the blanket and shield must be segmented.

(b) Blanket and Shield Segmented Toroidally

Equation (F-18) of Appendix F gives the field on axis from a segment of the blanket and shield as:

$$B_{in} = \frac{\mu_0 \sigma h a}{8} \dot{B}_0 \left[\frac{y(a^2 + y_0^2 - l^2)}{a^2 \sqrt{a^2 + y^2}} - \ln(y + \sqrt{a^2 + y^2}) \right]_{y = -l - y_0}^{y = l - y_0}, \quad (IV-27)$$

where $2l$ is the length of the segment and y_0 is the distance along the axis from the center of the segment to the field point. With $2l$ equal to the circumference divided by the number of segments, Equation (IV-27) was used to find the field from each segment of a blanket and shield divided into 8 and into 16 segments. The results are shown in Table IV-9. With 8 segments, the induced field is found to be large, so large in fact that the assumptions of the calculation do not hold. Even with 16 segments, the resulting induced field (9.3%) seems uncomfortably large. Such a change in vertical field would lead to a change in the plasma major radius and would adversely affect other plasma parameters such as current and temperature; it could increase both the major and minor radii of the plasma enough to bring the plasma onto the first wall.

Although it is conceivable that these eddy currents in the blanket and shield could actually be used to help stabilize the plasma or that they could be compensated for by phase shifting the input current to the EF coils, it seems safest to include adequate segmentation so that these currents are negligible. Since the blanket/shield region will have to be broken into modular units for assembly purposes anyway, the concept of insulated segmentation is compatible with a practical assembly scenario.

In each case in Table IV-9, the induced field is expressed as a percentage of the applied field, along with the phase delay ϕ and time delay, t_0 for penetration, calculated by the two following equations:

$$\phi = \tan^{-1} 2 \dot{B}_{in} / E_0 \quad (IV-28)$$

and

$$t_0 = \phi \tau / \pi, \quad (IV-29)$$

where τ is the applicable rise time of 2 s.

(c) Subdivided Blanket and Shield Represented by a Brick Model

One way of further subdividing is to increase the number of segments to 32, 48 or 64. The results of such segmentation is shown in Table IV-9. Another way, and the one that has been chosen for the design, is to subdivide each segment radially and poloidally into blocks. The blanket and shield design, shown in Figures IX-1 and IX-24, consists of 16 segments, each made up of 43 blocks. For the purpose of calculating eddy current effects, these 43 blocks are represented by 36 rectangular bricks, aligned with coordinate axes. The model is described in Appendix F; Eq. (IV-30) gives the field at a point (x_o, y_o, z_o) due to eddy current in a brick of dimensions $2a$ by $2b$ by $2c$.

$$\begin{aligned}
 B_{in} = & -\frac{\mu_o}{4\pi} \frac{\sigma}{a^2 + b^2} \dot{B}_o \left[\tan^{-1} \frac{uv}{wr} \left\{ 1/2 w^2 (x_o^2 - a^2 + y_o^2 - b^2) - w^4/6 \right\} + \right. \\
 & + \tan^{-1} \frac{uw}{vr} \left\{ vy_o (a^2 - x_o^2) + 1/2 v^2 (a^2 - x_o^2 + y_o^2 - b^2) + v^3 y_o + 1/2 v^4 \right\} + \\
 & + \tan^{-1} \frac{vw}{ur} \left\{ ux_o (b^2 - y_o^2) + 1/2 u^2 (b^2 - y_o^2 + x_o^2 - a^2) + u^3 x_o + 1/2 u^4 \right\} + \\
 & + \ln(r + u) \left\{ wv(b^2 - y_o^2 - 2v^2/3) - wy_o (a^2 - x_o^2 + 3v^2/2) - 1/2 w^3 y_o \right\} + \\
 & + \ln(r + v) \left\{ wu(a^2 - x_o^2 - 2u^2/3) - wx_o (b^2 - y_o^2 + 3u^2/2) - 1/2 w^3 x_o \right\} + \\
 & + \ln(r + w) \left\{ uy_o (u^2 + x_o^2 - a^2) + vx_o (v^2 + y_o^2 - b^2) + 2x_o y_o (u^2 + v^2) \right\} + \\
 & + r \left\{ uvw/6 + 1/2 x_o vw + 1/2 y_o uw + 2x_o y_o w \right\} \Bigg], \quad (IV-30)
 \end{aligned}$$

where σ is the electrical conductivity, $r = (u^2 + v^2 + w^2)^{1/2}$; $u = x - x_o$, $v = y - y_o$, $w = z - z_o$; and u , v and w are evaluated at their upper and lower limits $\pm a - x_o$, $\pm b - y_o$, and $\pm c - z_o$, respectively.

An array of $16 \times 36 = 576$ rectangular bricks modeled the blanket and shield, and the field from them was calculated by Equation (IV-30) and summed. The induced field on the plasma axis varies between 1.46% and 1.59% of the applied field; this variation is thought to be due to the rectangular-brick model used for the calculation rather than to the subdivided blanket and shield itself.

An estimate of how the field distortion affects the plasma equilibrium can be made based on the "type B" adiabatic compression algorithms.²⁵ A 5% decrease in vertical field would lead to a 2.5% increase in the major radius. A 10% change in the vertical field would increase both the major and minor plasma radius sufficiently to put the plasma on the outer wall of the vacuum chamber, in the absence of a limiter.

The induced field level, phase delay and time delay for the subdivided blanket and shield are felt to be safely small from plasma-physics considerations.

The problem of torques arising from the toroidal field acting on the eddy currents is discussed in Chapter X.

C. Research and Development Requirements

Many elements of the extensive research and development program that will be required to support the engineering effort on EPR superconducting magnet systems have been identified.

1. Toroidal-Field Coil System

(a) Magnet Stability Study and Testing Program

In the EPR, there is sufficient winding space to build fully stabilized coils. However, the stability of even fully stabilized coils is not completely understood at this time. Cooling along the conductor and between the adjacent turns has never been seriously considered in the analysis of stability. Data on normal front propagation velocity versus the conductor current density level, the magnet field and the associated cooling provisions are generally lacking or unreliable. An analytical study and a good experimental testing program on conductor stability and coil stability must be done before the multimillion dollar TF coil system is built.

(b) AC Loss and Pulsed-Field Shielding Study

No matter how one designs them, the vertical field and poloidal field will interact with the TF coil. They will produce a large ac loss dissipation in the TF coil system. A technique using normal metal shielding or superconducting shielding must be developed to provide the basis for a credible shielding design.

(c) Conductor Development Program

If complete shielding cannot be achieved, the matrix loss and the filament loss of fully stabilized conductors of conventional design are significant. To reduce these losses, the conductor may have to be made of a cable consisting of transposed multiple strands. Such a construction would require a development effort. Aluminum-stabilized conductors must also be evaluated, as it may be worthwhile to develop such a conductor.

(d) Coil-Forming Modeling

Using copper as the dummy conductor, the coil-forming technique and winding facility must be developed in preparation for coil fabrication.

(e) Radiation-Damage Testing

Data on radiation-damage effects to the superconductor, stabilizer, insulation material and structural material at low temperatures must be available before an EPR magnet system is designed and built.

(f) High-Field Magnet Program

In order to obtain high (≥ 10 T) toroidal fields, two approaches need to be explored. (1) NbTi can operate at fields of about 10 T with a useful current density if it is cooled to $\sim 3^\circ\text{K}$. NbTi is a strong ductile material, easily drawn into multifilamentary form together with Cu or Cu + CuNi stabilizers. It can operate at moderate strain levels ($\sim 2\%$). Practical refrigeration schemes and an understanding of heat transfer must be developed for the temperature region around 3°K . (2) Nb_3Sn can, in principle, operate with useful current densities at higher fields (10 to 16 T) and higher temperatures. Nb_3Sn cannot be drawn into multifilamentary form but recently has been put into this form by drawing Nb filaments in Cu or bronze stabilizers, then diffusing Sn into the Nb to form Nb_3Sn filaments. In this form, the matrix-averaged current density is comparable to NbTi at 3°K . The problem of brittleness remains, and determination of suitable strain levels for useful conductors must still be made. A modest level of effort to explore Nb_3Sn for post-1985 applications is appropriate.

2. Ohmic-Heating Coils and Equilibrium-Field Coils

An EPR will have a better chance to produce net electrical power if these coils are superconducting. Development of both coil systems are very similar.

These coils store a large amount of energy and must be cycled rapidly. This will require extending the present state-of-the-art of pulsed-superconducting magnet technology by a considerable amount. This should be started as early as possible. Modeling will be a useful approach. Large and fast charge and discharge magnets, having stored energies of the order of 100 MJ, need to be built to study the problems associated with large ac superconducting magnets as soon as possible. A parallel development program on the energy storage and power supply systems for these magnets must be started as soon as possible. The requirements upon the power supply and upon the pulsed superconducting coils are so interrelated that the development program for the two must be closely integrated.

(a) AC Losses of Superconductor

Analytical calculations and experimental measurements of ac losses in superconductors must be started as soon as possible to gain the knowledge and collect the data required for ac magnet design. These should include hysteresis losses, eddy current losses and self-field losses.

(b) AC Conductor Development

A low-loss cryogenically stable conductor must be developed because of the large stored energy in these coils. Industry must be involved in this effort.

(c) High-Current AC Cabling and Testing

Industry must be involved in this effort. It requires cable-making facilities and an experimental program to test ac cable stability and energy dissipation. A cable must be achieved that has a low self-field loss, good heat transfer, good packing factor and adequate mechanical support.

(d) Low AC Loss Cryostats

Low ac loss cryostats must be developed. Preferably these will be non-metallic with low He permeation.

(e) Heat Transfer Study

Heat transfer must be evaluated for the forced laminar flow and turbulent flow regimes and for the pool boiling regimes.

(f) AC Cryogenic Magnet Stability Study

The results for the previous five areas must be incorporated into an overall design of the pulsed field coils.

(g) Engineering Model Coil

A model coil should be built on a sufficient scale (~ 100 MJ) to test and evaluate the design concepts evolved. It should be built so that it can incorporate and test new coil concepts as they are developed.

3. Structural Material Development

There is a general lack of reliable cryogenic data on some of the materials used in superconducting magnets. In particular, the measurements of mechanical properties, such as stress and strain under cyclic loading, must be done. Data on thermal, electrical and mechanical properties of all structural materials must be available before we can design and build an EPR magnet system.

-
1. K. P. Jüngst, "Rate Dependent Magnetization of Superconducting Magnet Conductors in Transverse and Longitudinal Fields," Proceedings of Fifth International Magnet Technology Conference, Italy (May 1975).
 2. J. N. Luton, Jr., "Periodic Reversal of Twist Hardness for Reduction of Longitudinal Pulsed Field Effects," ORNL-TM-5019, Oak Ridge National Laboratory, (1975, pp 126-136.
 3. W. M. Stacey, Jr., "Tokamak Experimental Power Reactor Studies," ANL/CTR-75-2, Argonne National Laboratory (June 1975).
 4. E. L. Wilson, "Solid Sap, a Static Analysis Program for Three-Dimensional Solid Elements," a report to Denver Mining Research Center, U.S. Dept. of the Interior Bureau of Mines (1971).
 5. J. File, R. G. Mills and G. V. Sheffield, "Large Superconducting Magnet Design for Fusion Reactors," IEEE Transaction on Nuclear Science NS-18, (1971), pp. 277-282.
 6. S. S. Shen and J. R. Miller, "Transient Poloidal Field Effects in Twisted Multifilamentary Superconductor for Toroidal Field Windings of Tokamaks," Proceedings of the Sixth Symposium on Engineering Problems of Fusion Research, (1975), pp. 145-151.
 7. W. J. Carr, Jr., "Parallel Field Losses in Twisted Multifilament Superconductors," Proceedings of the Sixth Symposium on Engineering Problems of Fusion Research, (1975), pp. 152-155.

8. M. S. Walker, G. R. Wagner and S. S. Shen, "Losses in Twisted Multi-filament Superconductors in Simulated Poloidal (Parallel) Fields," Proceedings of the Sixth Symposium on Engineering Problems of Fusion Research, (1975), pp. 156-160.
9. F. R. Fickett, "Resistivity of Polycrystalline Aluminum and Copper in High Magnetic Fields: The Effect of Temperature and Purity," *Appl. Phys. Lett.* 17, (1975), p. 525.
10. M. G. Benz, "Magnetoresistance of Copper at 4.2°K in Transverse Fields up to 100 KG," *Appl. Phys.*, 40, (1969), p. 2003.
11. J. de Lounay, R. L. Dolecek and R. T. Webber, "Magnetoresistance of Copper," *J. Phys. Chem. Solids*, 11, 37 (1959).
12. F. R. Fickett, "Magnetoresistance of Very Pure Polycrystalline Aluminum," *Phys. Rev. B*, 3 (1971), p. 1941.
13. J. P. Purcell and R. B. Jacobs, "Transverse Magnetoresistance of High Purity Aluminum," *Cryogenics*, 3, (1963), p. 109.
14. R. Stevenson, "Resistance of Transverse Magnetoresistance of High Purity Aluminum," *Canadian J. of Phys.*, 45, (1967), p. 4115.
15. A. Maimoni, "Electrical Resistance of Aluminum of Low Temperatures," *Cryogenics*, 3, (June 1962), p. 217.
16. F. Pawlek and D. Rogalla, "The Electrical Resistivity of Silver, Copper, Aluminum, and Zinc as a Function of Purity in the Range 4-298°K," *Cryogenics*, 6, (1966), p. 14.
17. V. D. Arp, M. B. Kasen and R. P. Reed, "Magnetic Energy Storage and Cryogenic Aluminum Magnets," NBS-Report AFAPL-TR-68-87 (1969).
18. R. W. Boom, private communication.
19. R. Bohn and G. Komarek, "Julich Argus II Race Track Magnets," Joint U.S.-U.S.S.R. Status Report, Division 4: Superconducting Magnets, edited by D. Bruce Montgomery, December 1975, Section III-F. (Recovery flux less than 0.06 W/cm².)
20. Z. J. J. Stekly, "The Performance of a Large MHD-Type Stable Superconducting Magnet," Presented at the International Conference on High Magnetic Fields, Grenoble, France, September 1966. (Recovery heat flux for single layer coil is 2.2 W/cm², attempted heat recovery heat flux is 0.88 W/cm² for full coils.)
21. B. J. Maddock, C. N. Carter and C. G. Lewis, "The Effect of Interburn Heat Flow on the Recovery Currents of a Superconducting Magnet: A Comparison of Calculated and Measured Values," *Cryogenics*, 13, (1973), p. 337.
22. G. L. Kulcinski, et al., "Wisconsin Tokamak Reactor Design," UW-FDM-68, 1, Chapter VII, (1974).

23. P. T. M. Clee and C. W. Trowbridge, "Design Study of a Tokamak Fusion Device with Superconducting B_0 Coils," RL-73-067, (1973), p. 37.
24. Written by H. Desportes, France, Saclay.
25. H. D. Furth and S. Yoshikawa, "Adiabatic Compression of Tokamak Discharges," *Phys. Fluids*, 13, (1970), p. 10.

CHAPTER V

PLASMA HEATING SYSTEMS

Supplemental heating, in addition to the ohmic heating, will be required to heat the EPR plasma to ignition temperatures. Current experience dictates that neutral beam heating be the first choice for this supplement. Extrapolation of present D^+ sources to higher currents and higher particle energies needed to penetrate the EPR plasma poses many challenges, primarily because the $D^+ \rightarrow D^0$ cross section decreases rapidly with energy. Efficient energy recovery is essential for utilization of these sources, and improvements in ion composition and grid transparency would have profound impact on the performance of the system and the EPR energy balance. The sizes and requirements of components loom large in the overall view of the reactor. Development of D^- sources of good gas efficiency will greatly ameliorate this situation and lead the way to higher beam energies and greater efficiencies, with dramatically reduced pumping requirements. Radio-frequency (rf) wave heating appears as an attractive alternative from the technological point of view since efficient power sources exist for several heating modes and blanket presentation problems are reduced. On the other hand, wave heating experiments have not shown the same success as neutral beam heating.

In this chapter, three neutral beam systems are designed. The first, chosen as the reference design, is based on minor extrapolations beyond presently achieved results with D^+ sources, the second is based on improved D^+ sources and the third is based on direct-extraction D^- sources. All employ energy recovery, and the D^+ injectors select only the $D^+ \rightarrow D^0$ component. Beam transport and neutralizing systems are designed and pumping requirements are explored in detail.

Radio-frequency sources and launching systems are designed for ion cyclotron and lower hybrid resonance heating. Because of the lack of adequate power sources and the uncertainties of high power transmission, electron cyclotron heating systems have not been designed.

A. Neutral Beam Injector Design

A reference design for an EPR neutral beam injector system was presented in last year's scoping and project definition study.¹ This design was based on a

power flow model containing two lumped, effective transport efficiencies (τ_1 and τ_2) which can account, after the fact, for losses beyond the exit of the accelerator without specifying the nature of the losses or where they occur.² The reference designs considered here are based on a new power flow model which accounts explicitly for all losses in terms of line densities of gas (along paths traversed by ions and neutrals) and cross sections for dissociation and charge-changing collisions.³ A somewhat simplified version of this model was used in the TETF scoping study.⁴

Three reference designs will be considered. Reference designs 1 and 2 are based on the use of positive deuterium ion sources and assume minor and major extrapolations, respectively, beyond the present state of the CTR ion source art. Reference Design 3 is based on the use of direct-extraction D^- ion sources with dc current densities and gas efficiencies comparable to those assumed for Reference Designs 1 and 2. Development of a D^- source with these characteristics in time to be of use for EPR would represent a major breakthrough. Reference Design 3 is presented here, somewhat prematurely in view of the present state-of-the-art, merely to show the potential advantages of direct extraction D^- sources.

Reference Designs 1 and 2 both call for twelve injectors, with two beam lines per injector, to provide 60 Mw of 180 keV D^0 beam. Each beam line will have a positive ion source and accelerator, followed by a double-focusing bending magnet, a neutralizer for the atomic ions and a second bending magnet to remove atomic ions from the beam emerging from the neutralizer. Direct energy convertors will be used to recover energy from the molecular and atomic ions removed by the first and second separator magnets. Thermal energy recovery will also be used to increase the overall injector efficiency. The two neutral beams from each injector will converge toward a 0.75 m diameter aperture in the first wall of the torus at angles of 10° above and 10° below the horizontal median plane of the torus.

D^- ions can be injected directly into neutralizers with 100% efficiency. They can also be injected after going through a bending magnet to eliminate line-of-sight neutron bombardment of the ion source; in this case, there will be a loss of several per cent due to premature neutralization even with pumping speeds several times higher than in the case of straight-line injection.

Table V-1. Ion Source Design Characteristics

	<u>Reference Design 1</u>	<u>Reference Design 2</u>	<u>Reference Design 3</u>
Type of Source	D^+	D^+	D^-
D^+ fraction of i_+ , Γ_1	0.75	0.95	-
D_2^+ fraction of i_+ , Γ_2	0.18	0.02	-
D_3^+ fraction of i_+ , Γ_3	0.07	0.03	-
D^- fraction of i_- , Γ_1^-	-	-	0.95
e^- fraction of i_- , Γ_e	-	-	0.05
Shape of Plasma Sheath	convex	convex	planar
Type of Grid	multiaperture	multiaperture	multiaperture
First Grid Transparency, τ_g	0.45	0.50	0.45
Number of Grids	4 or 5	4 or 5	4 or 5
Ion Beam Current Density, $j_+ = (R_1/R_n)^2 j_0$ (A/cm ²)	0.135*	0.175*	0.135
Plasma Ion Emission Current Density, j_0 (A/cm ²)	~ 0.300	~ 0.350	0.300
Beam Voltage, V_+ (MV)	0.18	0.18	0.18
Gas Efficiency, η_g	0.45	0.5	0.45
Pulse Length	dc	dc	dc

* R_1 and R_n are, respectively, the radii of curvature of the first and last of n grids.

We shall consider only straight-line injection at this time. A bending magnet and a direct convertor will be used after the neutralizer.

1. Ion Sources

The design characteristics of the ion sources for the three reference designs are given in Table V-1. The assumed positive ion beam composition for Reference Design 1 is typical of present-day LBL ion sources.⁵ Water-cooled multiaperture grids capable of dc operation will be of the type developed at ORNL.⁶ For the positive ion sources, circular apertures will be used in preference to slots so that the horizontal and vertical emittances will be identical. This will simplify the design of the double focusing magnet used to remove molecular ions from the beam entering the neutralizer and to focus the atomic ion and neutral beams. As a matter of convenience, circular apertures will also be assumed for the D^- sources.

For the D^- sources of Reference Design 3, only the simplest case of a planar plasma sheath and axially aligned apertures will be considered at this time. For the positive ion sources of Reference Designs 1 and 2, curved grids will be used in the source and accelerator to produce a convex plasma sheath and a diverging ion beam. The first, double-focusing bending magnet will convert the D^+ component of this diverging beam into a converging beam which goes through a waist before it reaches the first wall of the torus. The location of the waist and the beam size at the first wall are determined by the initial beam divergence.

To minimize the neutralizer gas load, the neutralizer will be tapered to follow the converging positive and neutral beams. Some results of a study³ of the effects of initial beam size and divergence on the beam size at the neutralizer entrance and exit and at the first wall of the torus will be presented in the next section. The results apply to Reference Design 1.

Neutral and ion beam current requirements per source are given by

$$i_o = I_o / N_s = P_B / V_+ N_s \quad \text{equiv. A} \quad (V-1)$$

$$i_+ = I_+ / N_s = \frac{P_B / V_+ N_s}{\epsilon_R \Gamma_1 f_{10} e^{-\pi + \sigma_{10}} e^{-\pi_0 \sigma_{01}}} \quad A \quad (V-2)$$

$$i_- = I_-/N_s = \frac{P_B/V_- N_s}{\epsilon_R \Gamma_1 f_{10}^- e^{-\pi \sigma_{10}^-} e^{-\pi \sigma_{01}^-}} \quad A \quad (V-3)$$

where I_0 , I_+ and I_- are total beam currents; N_s is the number of ion sources; P_B is the total neutral beam power; V_+ and V_- are ion beam voltages; ϵ_R is the reactor capture efficiency for neutral beam; Γ_1 is the atomic fraction of i_+ ; f_{10}^+ and f_{10}^- are the power efficiencies for $D^+ \rightarrow D^0$ and $D^- \rightarrow D^0$, respectively; $e^{-\pi \sigma_{10}^+}$ is the fraction of the D^+ beam that reaches the neutralizer, the remainder $(1 - e^{-\pi \sigma_{10}^+})$ being lost due to premature neutralization along a curved path while traversing a gas target of thickness π_+ atoms/cm²; $e^{-\pi \sigma_{01}^-}$ is the fraction of the D^0 beam that reaches the reactor plasma, the remainder being lost due to reionization in a gas target of thickness π_0 atoms/cm²; σ_{10} is the electron capture cross section (cm²/atom) and σ_{01} is the electron loss cross section (cm²/atom). The definitions for $e^{-\pi \sigma_{10}^-}$ and σ_{10}^- are analogous to those for $e^{-\pi \sigma_{10}^+}$ and σ_{10}^+ . For the straight-line injection case of Reference Design 3, $e^{-\pi \sigma_{10}^-} = 1.00$. In terms of average pressure, $\bar{p}$ (torr), path length, ℓ (m), and gas temperature, T (°K), the thickness of a gas target is given by

$$\pi = 1.94 \times 10^{21} \bar{p} \ell / T \quad \text{atoms/cm}^2 \quad (V-4)$$

2. Atomic Ion and Neutral Beam Transport for Reference Designs 1 and 2

A preliminary beam line design* is shown in Figure V-1. M-1 is a double focusing magnet for D^+ , with field index $n = (R/B)dB/dR = 1/2$, bending radius $R_0 = 1.5$ m and bending angle $\theta = 90^\circ$. For a 180 keV D^+ beam, the gap field is $B_0 = 575$ gauss; field variation with R is given by $B = B_0 (R/R_0)^{-1/2}$.

The diverging ion beam from the accelerator is assumed to be axially symmetric. Beam transport calculations for motions of atomic ions and neutrals in the transverse phase space $(x, x'; y, y')$ have been made for several values of emittance $\epsilon = \epsilon_x = \epsilon_y$. ϵ is determined by initial beam size $2 r_{0 \text{ max}}$ and by the

* We are indebted to T. K. Khoe of the Accelerator Research Facilities Division, ANL, for the design of M-1 and the formulation of the beam transport equations.

	$R_0(\text{m})$	$\theta(\text{deg})$
D_1^+	1.5	90.0
D_2^+	2.12	73.0
D_3^+	2.6	64.9

Figure V-1. Preliminary Beam Line Design. R_0 is the Bending Radius, θ is the Bending Angle in M-1. For 180 keV D^+ , the Gap Field $B_0 = 575$ Gauss in M-1.

maximum initial beamlet half-angle divergence ω_o , which is assumed to be the same for all beamlets. R_n is the radius of curvature of the final (nth) grid of the accelerator (see Figure V-2).

The boundary curve for the transverse phase space occupied by the beam is approximated by an ellipse to facilitate calculations. The area of the ellipse at the exit of the accelerator, where the initial conditions occur, is given by

$$\epsilon = \frac{\text{area}}{\pi} = r_o \max \omega_o \quad (V-5)$$

which gives the relationship between ϵ , $r_o \max$ and ω_o .

Preliminary calculations indicated that $e^{-\pi + \sigma_1 0} = 0.97$ and $e^{-\pi 0 \sigma_1 1} = 0.885$ are reasonable values. Using these values (and $V_+ = 0.18$ MV, $\epsilon_R = 0.95$, $\Gamma_1 = 0.75$, $f_{10} = 0.20$), Equation V-2 and the design value $j_+ = 0.135$ A/cm² give

$$i_+ = 45.4 P_B / N_S \quad \text{A} \quad (V-6)$$

$$a_g = i_+ / j_+ = 336 P_B / N_S \quad \text{cm}^2 \quad (V-7)$$

$$2 r_o \max = \sqrt{(4/\pi) a_g} = 20.7 \sqrt{P_B / N_S} \quad \text{cm} \quad (V-8)$$

where a_g is the area of the accelerator exit grid.

Beam transport calculations for atomic ions and neutrals have been made for several values of P_B / N_S and for three cases that differ in the manner in which $\Omega_o \max = r_o \max / R_n$ is chosen.³ The cases are

$$\text{Case I: } \Omega_o \max = 0.060 \text{ rad, } R_n = (r_o \max / 0.06 \text{ m})$$

$$\text{Case II: } R_n = 1.60 \text{ m, } \Omega_o \max = (r_o \max / 1.60) \text{ rad}$$

$$\text{Case III: } R_n = 2.225 \text{ m, } \Omega_o \max = (r_o \max / 2.225) \text{ rad}$$

In every case, ω_o was held constant and equal to 0.020 rad. Results of calculations for Case I are shown in Table V-2. The calculations were not completed because the trend was not favorable for the larger values of $2 r_o \max$. Results of calculations for Case II are shown in Table V-3. Gas loads were

Figure V-2. Schematic of Ion Source and Accelerator for a Diverging Ion Beam. For the initial version of Reference Design 1, $\omega_0 = 0.020$ Rad., $\Omega_0 \text{ max} = 0.0735$ Rad., $R_n = 2.225$ m, $n = 4$ or 5 .

Table V-2. Results of Calculations for D⁺ and D^o Beam Transport and Neutralizer Gas Loads.
Case I. $\omega_o = 0.020$ rad; $\omega_{max} = 0.060$ rad; R_n Variable (Reference Design I)

P_{B/N_s} (MW)	i_+ (A)	$2r_{\omega_{max}}$ (m)	$\omega_{\omega_{max}}$ (rad)	R_n (m)	S_w (m)	$2r_w$ (m)	$2r_{0.3}$ (m)	$2r_{4.3}$ (m)	$2r_{10.7}$ (m)	q_N (Torr-ℓ/s)	αq_N (Torr-ℓ/s)	$(1-\alpha)q_N$ (Torr-ℓ/s)
40/48	37.9	.189	.060	1.580	6.10	.182	Not Calculated		.268	Not Calculated Be-		
40/32 (60/48)	56.8	.231	.060	1.925	4.91	.147	Because of Un-		.393	cause of Unfavorable Trend in		
40/24 (80/48)	75.7	.267	.060	2.225	4.24	.137	favorable		.521	$2r_{10.7}$		
60/24 (80/32)	114	.327	.060	2.725	3.52	.127	Trend in		.751			
80/24	151	.378	.060	3.150	3.12	.122	$2r_{10.7}$		.951			

N_s = number of sources

i_+ = ion beam per source

$2r_{\omega_{max}}$ = initial beam size

$\omega_{\omega_{max}}$ = initial 1/2-angle beam divergence

ω_o = maximum initial 1/2-angle beamlet divergence

R_n = radius of curvature of nth (exit) grid

S_w = distance, magnet exit to beam waist

$2r_w$ = beam size at waist

$2r_{0.3}$ = beam size at $s = 0.3$ m (neutralizer entrance)

$2r_{4.3}$ = beam size at $s = 4.3$ m (neutralizer exit)

$2r_{10.7}$ = beam size at $s = 10.7$ m (1st wall of torus)

q_N = gas load per neutralizer

αq_N = fraction of q_N to Chamber I

$(1-\alpha)q_N$ = fraction of q_N to Chamber II

Table V-3. Results of Calculations for D⁺ and D⁰ Beam Transport and Neutralizer Gas Loads
Case II. $\omega_0 = 0.020$ rad; $R_n = 1.60$ m; ω_{max} Variable (Reference Design I)

P_B/N_s (MW)	i_+ (A)	$2r_{0,max}$ (m)	ω_{max} (rad)	R_n (m)	S_w (m)	$2r_w$ (m)	$2r_{0.3}$ (m)	$2r_{4.3}$ (m)	$2r_{10.7}$ (m)	q_N (Torr-l/s)	αq_N (Torr-l/s)	$(1-\alpha)q_N$ (Torr-l/s)
40/48	37.9	.189	.0591	1.60	5.96	.163	.309	.194	.274	Not Calculated Because of Unfavorable Trend in $2r_{0.3}$		
40/32 (60/48)	56.8	.231	.0722	1.60	6.46	.168	.378	.206	.288			
40/24 (80/48)	75.7	.267	.0834	1.60	6.74	.188	.438	.233	.330			
60/24 (80/32)	114	.327	.1020	1.60	7.01	.166	.537	.268	.329			
80/24	151	.378	.1180	1.60	7.59	.176	.621	.304	.353			

N_s = number of sources

i_+ = ion beam per source

$2r_{0,max}$ = initial beam size

ω_{max} = initial 1/2-angle beam divergence

ω_0 = maximum initial 1/2-angle beamlet divergence

R_n = radius of curvature of nth (exit) grid

S_w = distance, magnet exit to beam waist

$2r_w$ = beam size at waist

$2r_{0.3}$ = beam size at $s = 0.3$ m (neutralizer entrance)

$2r_{4.3}$ = beam size at $s = 4.3$ m (neutralizer exit)

$2r_{10.7}$ = beam size at $s = 10.7$ m (1st wall of torus)

q_N = gas load per neutralizer

αq_N = fraction of q_N to Chamber 1

$(1-\alpha)q_N$ = fraction of q_N to Chamber 11

not calculated because the neutralizer sizes were too large to be of interest. For Case III (Table V-4), neutralizer sizes and gas loads are reasonable but not necessarily optimized.

To pressure distribution in the tapered neutralizer,³ the length, L divided into ten equal segments of length $\Delta l = 0.4$ m (see Figure V-5). For each segment, an average conductance was calculated by using the value of wall radius at the midsection of the segment in the expression for the conductance of a tube of radius, r (m), and length, l (m),

$$c = 2.609 \times 10^6 \frac{r^3}{l} \text{ l/s} \quad (\text{V-9})$$

The pressures p_n were then calculated in terms of α and q_N (Figure V-3) by using the expressions

$$p_n/q_N = \frac{\alpha \Delta l}{2.609 \times 10^6} \left[\frac{1}{r^3(0.2)} + \frac{1}{r^3(0.6)} + \dots + \frac{1}{r^3(0.4n-0.2)} \right], \quad n \leq 5 \quad (\text{V-10})$$

$$p_n/q_N = \frac{(1-\alpha) \Delta l}{2.609 \times 10^6} \left[\frac{1}{r^3(3.8)} + \frac{1}{r^3(3.4)} + \dots + \frac{1}{r^3(0.4L+0.2)} \right], \quad n \geq 5 \quad (\text{V-11})$$

For $n = 5$, the two expressions for p_n/q_N may be equated and solved for α .

For 180 keV D^+ , the neutralizer target thickness at 293°K for a power efficiency $f_{10} = 0.95 F_{0\infty}$ ($F_{0\infty}$ is the equilibrium neutral fraction) is $\pi_N = 4.3 \times 10^{16}$ atoms/cm². With this value of π , Equation V-4 gives a neutralizer line density of 6.5×10^{-3} Torr-m. Numerical integration using the values of p_n/q_N given by Equations V-10 and V-11, with a known value of α , gives

$$q_N \int_0^L \left(\frac{p}{q_N} \right) dl = q_N \times \text{const} = 6.5 \times 10^{-3} \text{ Torr-l}$$

or

$$q_N = \frac{6.5 \times 10^{-3}}{\int_0^L (p/q_N) dl} \text{ Torr-l/s} \quad (\text{V-12})$$

Case III, Table V-4, with $P_B = 60$ MW and $N_s = 24$ ion sources (in twelve injectors) was chosen as the initial Reference Design 1. The final design, obtained by iteration, will be discussed in Section V-6.

Table V-4. Results of Calculations for D^+ and D^0 Beam Transport and Neutralizer Gas Loads
Case III. $\omega_o = 0.020$ rad; $R_n = 2.225$ m; ω_{max} Variable (Reference Design 1)

P_B/N_s (MW)	i_+ (A)	$2r_{o_{max}}$ (m)	ω_{max} (rad)	R_n (m)	S_w (m)	$2r_w$ (m)	$2r_{0.3}$ (m)	$2r_{4.3}$ (m)	$2r_{10.7}$ (m)	q_N (Torr-c/s)	αq_N (Torr-c/s)	$(1-\alpha)q_N$ (Torr-c/s)
40/48	37.9	.189	.0425	2.225	3.70	.132	.236	.136	.423	8.34	5.65	2.69
40/32 (60/48)	56.8	.231	.0519	2.225	4.05	.135	.290	.136	.474	10.7	7.96	2.76
40/24 (80/48)	75.7	.267	.0600	2.225	4.24	.137	.337	.137	.501	14.7	11.5	3.20
60/24 (80/32)	114	.327	.0735	2.225	4.44	.139	.414	.140	.605	23.3	19.3	4.00
80/24	151	.378	.0849	2.225	4.49	.152	.479	.153	.691	34.5	29.0	5.50

V-12

N_s = number of sources

i_+ = ion beam per source

$2r_{o_{max}}$ = initial beam size

ω_{max} = initial 1/2-angle beam divergence

ω_o = maximum initial 1/2-angle beamlet divergence

R_n = radius of curvature of nth (exit) grid

S_w = distance, magnet exit to beam waist

$2r_w$ = beam size at waist

$2r_{0.3}$ = beam size at $s = 0.3$ m (neutralizer entrance)

$2r_{4.3}$ = beam size at $s = 4.3$ m (neutralizer exit)

$2r_{10.7}$ = beam size at $s = 10.7$ m (1st wall of torus)

q_N = gas load per neutralizer

αq_N = fraction of q_N to Chamber 1

$(1-\alpha)q_N$ = fraction of q_N to Chamber 11

$$\begin{aligned}
 p_0 &= p_{10} = 0 \\
 \Delta l &= 0.4 \text{ m} \\
 r_n &= r_0 + r' n \Delta l \\
 r' &= [(r_{10} - r_0) / 4] + .001
 \end{aligned}$$

Figure V-3. Division of the neutralizer into segments for numerical integration. The second term in the expression for r' is an allowance for beam scattering (Ref. 7).

3. Molecular Beam Transport

For the purposes of this discussion, the plane defined by the x-axis and the beam axis will be called the horizontal plane (plane of the paper in Figure V-1) and the plane defined by the y-axis and the beam axis will be called the vertical plane. For the molecular ion beams, the bending angles are not equal to the magnet angle. Therefore, the molecular beams experience vertical defocusing and horizontal focusing at the downstream edge of the magnet. Results of transport calculations for D_2^+ and D_3^+ are given in Table V-5 for the preliminary version of Reference Design 1. S is the distance from the downstream magnet edge; initial conditions are those of Table V-4 with $P_B = 60$ MW, $N_S = 24$ sources.

Table V-5. Results of D_2^+ , D_3^+ Beam Transport Calculations for the Beam Line of Figure V-1

D_2^+	horizontal beam size at $S = 2$ m*	0.204
D_2^+	vertical beam size at $S = 2$ m	0.728
D_3^+	horizontal beam size at $S = 1.85$ m	0.485
D_3^+	vertical beam size at $S = 1.85$ m	0.914

* S is the distance from the M-1 exit face to the direct energy converter. Initial conditions are those given in Table V-4 for $P_B = 60$ MW, $N_S = 24$ ion sources.

4. EPR Neutral Injector Power Flow Model for Reference Designs 1 and 2

The injector power flow diagram³ for EPR is shown in Figure V-4 for the positive ion source case. Ion beam from the accelerator enters magnetic separator M-1, which removes molecular ions from the beam before it enters the neutralizer. The molecular ions, with attenuation due to charge-changing collisions and dissociation, go to a direct energy converter^{8,9} where their energy is recovered with an efficiency $\eta_1 \lesssim 0.85$.

Figure V-4. EPR injector power flow diagram. π is a gas target thickness (atoms/cm²) for charge-changing collisions or dissociation. η_2 and η_1 are electrical energy recovery efficiencies (≤ 0.85). η_3 is the thermal energy recovery efficiency (≈ 0.30). $f_{10} = 0.95 F_{0\infty} - F_{1\infty}$ = power efficiency for $D^+ \rightarrow D^0$.

A number of cross sections for charge-changing collisions and dissociation appear in Figure V-4. The summations may be written out for 90 keV hydrogen particles and then used for the corresponding 180 keV deuterium particles:

$$\Sigma (\sigma_{\text{loss}})_2 = \sigma_{\text{H}_2} + 1/2 (\sigma_{\text{H}} + \sigma_{\text{H}^+}) \quad (\text{V-13})$$

$$\Sigma (\sigma_{\text{loss}})_3 = 1/3 (\sigma_{\text{H}} + \sigma_{\text{H}^+}) + 2/3 (\sigma_{\text{H}_2} + \sigma_{\text{H}_2^+}). \quad (\text{V-14})$$

Values for all of the cross sections, measured for 90 keV hydrogen particles, are given in Table V-6. The values of σ_{10} and σ_{01} in the table are averages of several values compiled by S. K. Allison and M. Garcia Munoz.¹⁰

Table V-6. Cross Sections for Charge Changing Collisions and Dissociation for 90 keV Hydrogen and 180 keV Deuterium Particles

Particle	Cross Sections	Reference
90 keV H^+ or H^0 or equivalent (cm^2/atom)	$\sigma_{10} = 1.5 \times 10^{-17}$	10
	$\sigma_{01} = 5.7 \times 10^{-17}$	10
90 keV H^- or equivalent (cm^2/atom)	$\sigma_{T0} = 2.3 \times 10^{-16}$	11
	$\sigma_{11} = 1.6 \times 10^{-17}$	12
90 keV H_2^+ or equivalent ($\text{cm}^2/\text{molecule}$)	$\sigma_{\text{H}} = 4.1 \times 10^{-16}$	13
	$\sigma_{\text{H}_2} = 6.0 \times 10^{-17}$	13
	$\sigma_{\text{H}_2^+} = 2.15 \times 10^{-16}$	13
90 keV H^+ or equivalent ($\text{cm}^2/\text{molecule}$)	$\sigma_{\text{H}} = 7.3 \times 10^{-16}$	13
	$\sigma_{\text{H}_2} = 2.4 \times 10^{-16}$	13
	$\sigma_{\text{H}^+} = 2.25 \times 10^{-16}$	13
	$\sigma_{\text{H}_2^+} = 1.0 \times 10^{-16}$	13

5. Injector Gas Flow Diagram

The gas flow diagram³ for EPR is shown in Figure V-5. The various gas loads are defined in Table V-7. Neutral injector gas loads per injector (2 beam lines) for the preliminary reference design of Table V-4 with $P_B = 60$ MW and $N = 24$ ion sources are given in Table V-8. The fractional beam losses, $1 - e^{-\frac{S_p}{\sigma_1 0}}$ and $1 - e^{-\pi 0 \sigma 01}$, depend on the pressure distributions in Chambers I and II of Figure V-5. The pumping speed in the region of M-1 will be conductance-limited to a value that will not permit arbitrary reduction in pressure and beam loss for a given gas load.

A crude estimate of the conductance of M-1 can be obtained by noting that all of the gas from the accelerator, q_{out} , and the neutralizer, αq_N , enters a volume bounded by the exit plane of the accelerator and the neutralizer, the surfaces defined by the polepieces, extended to meet the exit planes, and the surface defined by the curved magnet edges, extended to meet the exit planes. The gas leaves this volume through the unobscured fraction of the bounding surface, which is maximized by using an open "C" type magnet rather than a window frame type. The area of this fraction is estimated to be roughly 5.0 m^2 for the M-1 design of Figure V-1. For deuterium, the room temperature conductance of this area considered as an aperture is $C = 1.6 \times 10^6 \text{ l/s}$. For a Chamber I pumping speed of $S_p \text{ l/s}$, the effective, conductance-limited pumping speed, S , in the M-1 region is given by

$$\frac{1}{S} \approx \frac{1}{C} + \frac{1}{S_p} \quad (V-15)$$

Values for the effective speed, S , and the cryopanel area, A_I , required to give a pumping speed, S_p , are given in Table V-9 for several values of S_p . Fractional losses of D^+ , D_2^+ and D_3^+ are also given. The pressures p_I and p_M , the average Chamber I pressure and the average pressure in the M-1 region, are given by

$$p_I = q_I / S_p = 101.4 / S_p \quad (V-16)$$

and

$$p_M = (q_{out} + \alpha q_N) / S + p_I = 36.8 / S + p_I \quad (V-17)$$

where the numerical values are from Table V-8.

Table V-7. Gas Loads to Pump Chamber I, II and the Torus

Nature of Load	Amount of Load (torr-c/s)	Pump Chamber
Gas flow into a source	$q_{in} = 0.095 (i_+ / e_+) \cdot 10^4$	--
Gas flow out of source	$q_{out} = (1 - \eta_N) q_{in}$	I
Premature Neutralization of D^+	$q_{1+} = 0.095 (i_+ / e_+) (1 - e^{-\tau_1})$	I
Recombination of D^+ on collecting surfaces	$q_1 = 0.095 (i_+ / e_+) \times 2 \cdot 10^4$	I
Recombination of D_1^+ on collecting surfaces	$q_1 = 0.095 (i_+ / e_+) \times 3 \cdot 10^4$	I
Gas Load to Chamber I from neutralizer ^a	q_N	I
Gas load to Chamber II from neutralizer ^a	$(1 - \alpha) q_N$	I
Recombination of D^+ on collecting surfaces	$q_{1+}^* = 0.95 (i_+ / e_+) (1 - 0.95 F_{1+} - F_{1+}^* \times e^{-\tau_1} + 1)$	II
Relonization of p^0	$q_{01} = 0.95 (i_+ / e_+) (1 - e^{-\tau_1} + 1) \times (1 - e^{-\tau_1})$	II
Recombination of D^- on collecting surfaces	$q_- = (i_+ / e_+) (F_{1+}^* e^{-\tau_1} + 1)$	II
Neutral beam	$q_0 = 0.095 P_B / V + q_{R_N}$	Torus
Slow gas flux to torus per injector (2 beam lines) ^b	$q_D = C_{duct} (P_{injector} - P_{torus})$	Torus

a. See equations V-10 through V-12

b. For a non-circular duct of uniform cross sectional area, A, length, l , and perimeter, H, (dimensions in cm) the conductance is given by¹⁴

$$C_{duct} = \frac{1}{1 + 3/16 H^2/A} \text{ l/s}$$

Table V-8. Neutral Injector Gas Loads (per Injector) for the Design of
Table V-4 with $P_B = 60$ MW and $N_S = 24$ Ion Sources (in Twelve
Injectors

Chamber I Gas Loads (Torr-l/s)		Chamber II Gas Loads (Torr-l/s)	
$2q_{in}^a$	63.5	$2q_{10}^*$	12.6
$2q_{out}$	34.9	$2q_{01}$	0.362
$2q_{10}$	0.487	$2q_-$	~ 0.0
$2q_2$	17.3	$2(1-\alpha)q_N$	8.0
$2q_3$	10.1	q_{II} (total load)	21.1
$2\alpha q_N$	38.6		
q_I (total load)	101.4		

^a $2q_{in}$ is not part of the Chamber I gas load but is included here because it is used to calculate $2q_{in}$ (see Table V-7).

Figure V-5. Gas flow diagram for EPR. The various gas loads are defined in Table V-7.

TABLE V-9. Chamber I Pumping Speeds and Pressures and Fractional Losses of D_1^+ , D_2^+ and D_3^+ for the Design of Table V-4 with $P_b = 60$ MW and $N_s = 24$ Ion Sources

A_I (m^2)	S_p^* (l/s)	S (l/s)	P_I (Torr)	P_M (Torr)	$1 - e^{-\pi \frac{S}{A_I} 10}$	$1 - e^{-\pi \frac{S}{A_I} (\sigma_{loss})_2}$	$1 - e^{-\pi \frac{S}{A_I} (\sigma_{loss})_3}$
20	1×10^6	6.2×10^5	1.0×10^{-4}	1.6×10^{-4}	0.047	0.64	0.80
40	2×10^6	8.9×10^5	5.0×10^{-5}	9.1×10^{-5}	0.027	0.43	0.59
60	3×10^6	1.0×10^6	3.3×10^{-5}	7.0×10^{-5}	0.021	0.34	0.49
80	4×10^6	1.1×10^6	2.5×10^{-5}	5.8×10^{-5}	0.017	0.29	0.42
100	5×10^6	1.2×10^6	2.0×10^{-5}	5.1×10^{-5}	0.015	0.25	0.38
∞	∞	1.6×10^6	0.0	2.3×10^{-5}	0.0069	0.10	0.17

* A pumping speed of 5×10^4 l/s per m^2 of cryopanel is assumed.

The line densities used to calculate the fractional losses of Table V-9 are given by

$$\int (p \, dl)_{D_j^+} = \ell_{jM} p_M + \ell_{jI} p_I, \quad j = 1, 2, 3 \quad (V-18)$$

where ℓ_{jM} is the path length for D_j^+ through the region where the average pressure is p_M and ℓ_{jI} is the path length through a region where the average pressure is p_I . For the design of Figure V-1, $\ell_{1M} = 3.00$ m, $\ell_{2M} = 3.63$ m, $\ell_{3M} = 4.47$ m, $\ell_{1I} = 0.0$ m, $\ell_{2I} = 2.0$ m and $\ell_{3I} = 1.85$ m.

Values of cryopanel area A_{II} required to give a pumping speed S and a pressure p_{II} in Chamber II are given in Table V-10. Approximate fractional losses of D^0 due to reionization and of D^+ due to neutralization in M-2 or in the region between M-2 and the direct converter are also given. For the design of Figure V-6, the line density used to calculate the fractional loss of D^0 is given by

$$\int (p \, dl)_{D^0} = 2.0 \, p_{II} + 4.4 \, (\overline{p}_{M-2} + \overline{p}_{DUCT}) + 0.3 \, p_{torus} \quad (V-19)$$

We approximate the average pressure over the path through M-2 and the duct by

$$\overline{p}_{M-2} + \overline{p}_{DUCT} \sim 1/2 \, (p_{II} + p_{torus}) \quad (V-20)$$

to obtain

$$\int (p \, dl)_{D^0} \sim 4.2 \, p_{II} + 2.5 \, p_{torus} \lesssim 4.2 \times 21/S_p + 5 \times 10^{-6}, \quad (V-21)$$

where use has been made of Table V-8 and the fact that $p_{torus} \lesssim 2 \times 10^{-6}$ Torr (see Section VII-A).

Beam transport calculations for D^+ through M-2 to the direct converter (Figure V-6) have not yet been done but the line density of the gas over this path is not expected to exceed (or reach) $6 \, p_{II}$ Torr-m and this value has been used to calculate the values of $1 - e^{-\pi \times 10^{-10}}$ shown in Table V-10.

Figure V-6. Neutralizer--Plasma Portions of the Beam lines for Reference Designs 1 and 2 and Preliminary Layouts of the Direct Convertors and Magnetic Separators in Chamber II.

6. Final Reference Design 1

For reasonable values of A_I (60 m²) and A_{II} (40 m²), the calculated losses $1 - e^{-\pi + \sigma_{10}}$ and $1 - e^{-\pi_{0\sigma_{01}}}$, of Tables V-9 and V-10, are less than the values assumed to obtain the starting conditions of Equations V-6, V-7 and V-8. A new set of starting conditions was obtained by using the values $e^{-\pi + \sigma_{10}} = e^{-\pi_{0\sigma_{01}}} = 0.98$, $P_B = 60$ MW and $N_s = 24$ ion sources in Equation V-2, with all other factors as before. The final version of Reference Design 1 is summarized in Table V-11, which also summarizes two other designs to be described later.

TABLE V-10. Chamber II Pumping Speeds and Pressures and Fractional Losses of D^0 and D^+ for the Design of Table V-4 with $P_B = 60$ MW and $N_s = 24$ Ion Sources.

A_{II} (m ²)	S_p^* (l/s)	P_{II} (Torr)	$1 - e^{-\pi_{0\sigma_{01}}}$	$1 - e^{-\pi + \sigma_{10}^*}$
20	1×10^6	2.1×10^{-5}	0.035	< 0.012
40	2×10^6	1.05×10^{-5}	0.018	< 0.0062
60	3×10^6	7.0×10^{-6}	0.013	< 0.0042
80	4×10^6	5.3×10^{-6}	0.010	< 0.0032
100	5×10^6	4.2×10^{-6}	0.0087	< 0.0025

* A pumping speed of 5×10^4 l/s per m² of cryopanel is assumed.

7. Reference Design 2

Reference Design 1 is based on minor extrapolations beyond the present state of the art for ion sources. It may be expected that significant advances will have been made before the freeze date for the EPR neutral injector design. Reference Design 2 is based on the assumption that major advances beyond the present state of the art will result in a source with the design characteristics listed in Table V-1. R & D efforts aimed at achieving characteristics similar to those of Table V-1 are currently under way at a number of CTR ion source laboratories.

Table V-11. Neutral Beam Reference Design Summaries

[illegible]

Table V-11. Neutral Beam Reference Design Summaries (cont'd)

Chamber I was lost due to Neutralizers, $2 \times P_{N_1}$ (Watts)	44.0	47.2	44.0	50.0
Total Chamber I was lost due to P_{N_1} (Watts)	44.0	47.2	44.0	50.0
Chamber I crosshatched Area, A_{N_1} (cm ²)	40	40	40.0	40.0
Chamber I Pumping Speed, S_{N_1} (l/s)	1.0×10^7	1.0×10^7	1.0×10^7	1.0×10^7
Chamber I Pressure, P_{N_1} (Torr)	1.0×10^{-7}	1.0×10^{-7}	1.0×10^{-7}	1.0×10^{-7}
Chamber I Pressure in the Wall section, P_{N_1} (Torr)	1.0×10^{-7}	1.0×10^{-7}	--	--
P_{N_1} (Watts) in Chamber I (Watts)	1.0×10^{-7}	1.0×10^{-7}	--	--
P_{N_1} (Watts) in Chamber I (Watts)	1.0×10^{-7}	1.0×10^{-7}	--	--
P_{N_1} (Watts) in Chamber I (Watts)	1.0×10^{-7}	1.0×10^{-7}	--	--
P_{N_1} Fractional Loss in Chamber I, P_{N_1} (Watts)	0.019	0.01	--	--
P_{N_1} Fractional Loss in Chamber I, P_{N_1} (Watts)	0.01	0.01	--	--
P_{N_1} Fractional Loss in Chamber I, P_{N_1} (Watts)	0.01	0.01	--	--
Chamber II was lost due to P_{N_2} (Watts)	11.4	11.4	--	--
Chamber II was lost due to P_{N_2} (Watts)	11.4	11.4	--	--
Chamber II was lost due to P_{N_2} (Watts)	11.4	11.4	--	--
Chamber II was lost due to P_{N_2} (Watts)	11.4	11.4	--	--
Chamber II was lost due to Neutralizers, $2 \times P_{N_2}$ (Watts)	11.4	11.4	--	--
Total Chamber II was lost due to P_{N_2} (Watts)	11.4	11.4	--	--
Chamber II crosshatched Area, A_{N_2} (cm ²)	40.0	40.0	40.0	40.0
Chamber II Pumping Speed, S_{N_2} (l/s)	1.0×10^7	1.0×10^7	1.0×10^7	1.0×10^7
Chamber II Pressure (Torr)	1.0×10^{-7}	1.0×10^{-7}	1.0×10^{-7}	1.0×10^{-7}
P_{N_2} (Watts) in Chamber II (Watts)	1.0×10^{-7}	1.0×10^{-7}	1.0×10^{-7}	1.0×10^{-7}
P_{N_2} (Watts) in Chamber II (Watts)	1.0×10^{-7}	1.0×10^{-7}	--	--
P_{N_2} (Watts) in Chamber II (Watts)	--	--	1.0×10^{-7}	1.0×10^{-7}
P_{N_2} Fractional Loss in Chamber II, P_{N_2} (Watts)	0.017	0.017	--	--
P_{N_2} Fractional Loss in Chamber II, P_{N_2} (Watts)	1.0×10^{-7}	1.0×10^{-7}	--	--
P_{N_2} Fractional Loss in Chamber II, P_{N_2} (Watts)	--	--	1.0×10^{-7}	1.0×10^{-7}
Ion Beam Power, P_{N_2} (Watts)	44.0	44.0	44.0	44.0
Assumed a Value	1.1	1.1	1.1	1.1
Power Required to Produce the Ion Beam, P_{N_2} (Watts)	44.0	44.0	44.0	44.0
Assumed Direct Converter Efficiency, η_{N_2}	0.85	0.85	0.85	0.85
Assumed Direct Converter Efficiency, η_{N_2}	0.85	0.85	0.85	0.85
Assumed Direct Converter Efficiency, η_{N_2}	0.85	0.85	0.85	0.85
Total Power Recovered Electrically, P_{N_2} (Watts)	44.0	44.0	44.0	44.0
Total Power Recovered Electrically, P_{N_2} (Watts)	44.0	44.0	44.0	44.0
Total Recovered Power, $P_{N_2} = P_{N_2} + P_{N_2}$ (Watts)	44.0	44.0	44.0	44.0
Total Power Input From Ion Beam Power Supply, $P_{N_2} = P_{N_2} + P_{N_2}$ (Watts)	44.0	44.0	44.0	44.0
Electrical Power Generated in ECR Ion Gun, $P_{N_2} = P_{N_2} + P_{N_2}$ (Watts)	0.44	0.44	0.44	0.44
Overall Neutral Ion Beam Power, $P_{N_2} = P_{N_2} + P_{N_2} + P_{N_2}$ (Watts)	0.44	0.44	0.44	0.44

Reference Design 2 is summarized in Table V-11.

8. Overall Injector Efficiency

The overall neutral injector efficiency is given by

$$\epsilon_{NI} = P_B / (k P_+ - P_R) \quad (V-22)$$

or

$$\epsilon_{NI} = \frac{P_B/P_+}{k - P_R/P_+} \quad (V-23)$$

where P_+ is power of the ion beam emerging from the accelerator, $k P_+$ is the power required to produce and accelerate the ion beam and $P_R = P_{R1} + P_{R2} + P_{R3}$ is the total power recovered (electrical and thermal). Given the ion beam voltage and composition (Table V-1), and the calculated values $0.95 F_{0.0} \approx 0.2$, $F_{1.0} \approx 0.0$, and the assumed values $k = 1.1$ and $\epsilon_R = 0.95$, and the fractional losses of Tables V-9 and V-10 (or similar ones for other designs), P_R/P_+ can be read off of Figure V-4.

Values of the components of fractional recovered power are given in Table V-12 for Reference Designs 1 and 2 as functions of η_1 , η_2 and η_3 , the energy recovery efficiencies. For EPR, reasonable values for the efficiencies are $\eta_1 = \eta_2 = 0.85$ and $\eta_3 = 0.30$. With these values, the results shown in Table V-13 are obtained for the ion beam power recovery efficiencies, P_R/P_+ , and the overall neutral injector efficiencies, ϵ_{NI} . A breakdown into power recovered electrically and power recovered thermally is given in Table V-14. Also given are $k P_+$, P_{IPS} and ϵ_{IPS} ; P_{IPS} is the external power furnished by the injector power supply (IPS) and ϵ_{IPS} is the efficiency with which P_{IPS} is converted into neutral beam power.

9. Reference Design 3

The potential advantages of direct-extraction D^- ion sources were discussed in last year's scoping and project-definition study.¹ As was noted in Reference 1, a direct-extraction D^- ion source suitable for use in a neutral beam injector does not presently exist. In view of the present state of the art, Reference Design 3 is based on long-term optimism rather than short-term expectations.

Table V-12. Values of the Components of Fractional Recovered Power in the Power Flow Diagram of Figure V-4 for Reference Designs 1 and 2 with $V_1 = 0.18 \text{ MV}^{\text{H}}$

Component of Fractional Recovered Power	Value of Component		
	Reference Design 1 (Preliminary)	Reference Design 1 (Final)	Reference Design 2
$P_{R_1}/P_+ = \left(\sum_2^3 \Gamma_j e^{-\pi_j \Sigma_j} \right)$	$0.153 \eta_1$	$0.160 \eta_1$	$0.037 \eta_1$
$P_{R_2}/P_+ = (0.8 \Gamma_1 e^{-\pi_+^{\sigma} 10})$	$0.570 \eta_2$	$0.585 \eta_2$	$0.752 \eta_2$
$k - 1$	$0.100 \eta_3$	$0.100 \eta_3$	$0.100 \eta_3$
$1 - \left(\sum_1^3 \Gamma_j e^{-\pi_j \Sigma_j} \right)$	$0.119 \eta_3$	$0.104 \eta_3$	$0.0221 \eta_3$
$[0.8 \Gamma_1 e^{-\pi_+^{\sigma} 10} (1 - e^{-\pi_+^{\sigma} 10})]$	$0.012 \eta_3$	$0.0059 \eta_3$	negligible
$[0.19 \Gamma_1 (1 - e^{-\pi_0^{\sigma} 01}) e^{-\pi_+^{\sigma} 10}]$	$0.016 \eta_3$	$0.0025 \eta_3$	$0.0018 \eta_3$
P_{R_3}/P_+	$0.247 \eta_3$	$0.212 \eta_3$	$0.124 \eta_3$

a. Use has been made of the calculated values $0.95 F_{0\omega} = 0.20$ and $F_{1\omega} \sim 0.0$. It is assumed that $k = 1.1$ and $\epsilon_R = 0.95$. Σ_j in $e^{-\pi_j \Sigma_j}$ is an abbreviation for $\Sigma(\sigma_{\text{loss}})_j$; for $j = 1$, $\Sigma_j = \sigma_{10}$.

Table V-13. Calculated Values of Ion Beam Power, P_+ , Ion Beam Power Recovery Efficiency, P_R/P_+ , and Overall Neutral Injector Efficiency, ϵ_{NI} , for Reference Designs 1 and 2 with $P_B = 60$ MW, $\eta_1 = \eta_2 = 0.85$, $\eta_3 = 0.3$ and $k = 1.1$.

<u>Reference Design</u>	<u>P_R/P_+</u>	<u>P_+ (MW)</u>	<u>ϵ_{NI}</u>
1, Preliminary	0.69	493	0.30
1, Final	0.70	441	0.34
2, Final	0.71	338	0.45

Table V-14. Values of P_+ , $k P_+$, $P_{R1,2}$, P_{R3} , P_{IPS} and ϵ_{IPS} for Reference Designs 1 and 2^a

<u>Reference Design</u>	<u>P_+ (MW)</u>	<u>$k P_+$ (MW)</u>	<u>$P_{R1,2}$ (MW)</u>	<u>P_{R3} (MW)</u>	<u>P_{IPS} (MW)</u>	<u>ϵ_{IPS}</u>
1, Preliminary	493	542	303	36.50	239	0.25
1, Final	441	485	279	28.05	206	0.29
2, Final	338	372	227	12.60	145	0.41

^a $P_{R1,2}$ is the total power recovered electrically; $P_{IPS} = k P_+ - P_{R1,2}$ is the external power furnished by the injector power supply (IPS); $\epsilon_{IPS} = P_B/P_{IPS}$ is the efficiency with which P_{IPS} is converted into neutral beam power; $\eta_1 = \eta_2 = 0.85$; $\eta_3 = 0.30$ and $k = 1.1$.

The power flow diagram for Reference Design 3, shown in Figure V-7, is an obvious extension of the flow diagram of Figure V-4. The required ion beam per source is given by Equation V-3, with $e^{-\pi-\sigma_{10}} = 1.00$ for the straight-line injection case of Reference Design 3; for this case,

$$i_- = \frac{P_B/V-N_s}{\epsilon_R \Gamma_1 f_{10} e^{-\pi-\sigma_{01}}} \quad (V-24)$$

The power efficiency f_{10} for $D^- \rightarrow D^0$ goes through a maximum as the target thickness is varied through an optimum value.^{15,16} D'Yackhov¹⁶ gives an expression for $(f_{10})_{\max}$:

$$(f_{10})_{\max} = \left(\frac{\sigma_{10}}{\sigma_{01}} \right) \left(\frac{\sigma_{01}}{\sigma_{10} + \sigma_{11}} \right) \frac{\sigma_{10} + \sigma_{11}}{\sigma_{10} + \sigma_{11} - \sigma_{01}} \quad (V-25)$$

The maximum occurs for an optimum target thickness

$$\pi_{\text{opt}} = \frac{1}{\sigma_{10} + \sigma_{11} - \sigma_{01}} \ln (\sigma_{10} + \sigma_{11}) / \sigma_{01} \quad (V-26)$$

Values of σ_{01} , σ_{10} and σ_{11} are given in Table V-6. As noted earlier, σ_{01} was obtained by averaging tabular data compiled in Reference 10. The value of σ_{10} is an extrapolation of graphical data compiled in Reference 11. The value of σ_{11} is a graphical extrapolation by Tawara and Russek of data they have compiled in Reference 12. With these values for the cross sections, Equations V-25 and V-26, give $(f_{10})_{\max} \sim 0.60$ and $\pi_{\text{opt}} \sim 7.7 \times 10^{15}$ atoms/cm². This value of π in Equation V-4 gives a neutralizer line density of 1.2×10^{-3} Torr-m at 293°K.

The value $(f_{10})_{\max} \sim 0.60$ is for a gas target. For 100 to 400 keV H^- (equivalent to 200 to 800 keV D^-), the measured value of $(f_{10})_{\max}$ in a lithium plasma target is ~ 0.82 ; at lower energies (down to at least 100 keV for H^- , or 200 keV for D^-) theoretical considerations suggest that the value should be slightly higher.¹⁷

A brief study³ has been made of two versions of Reference Design 3. Both versions (3a and 3b) start with a direct-extraction D^- ion source whose assumed

Figure V-7. Power Flow Diagram for Reference Design 3.

design characteristics are given in Table V-1. In design 3a, carried through in some detail, neutralization occurs in a gas target, with an efficiency of $\sim 60\%$. In design 3b, neutralization occurs in a lithium plasma target with an efficiency of $\sim 82\%$; the plasma target has not yet been considered in detail but, obviously, it should be located at the waist of an initially converging beam. To avoid straight-forward but tedious calculations for which there will be ample time while direct-extraction D^- sources suitable for use in neutral beam injectors are being developed, beam focusing has not been considered for these preliminary designs, which may therefore be considered as worst-case designs.

A preliminary D^- beam line design is shown in Figure V-8. The accelerator and the D_2 gas neutralizer are separated by a 1.5 m pumping gap to reduce the pressure in the accelerating gaps and thus to minimize beam energy spread due to premature neutralization and grid loading due to ionization of gas in the accelerator. To minimize the neutralizer gas load, the neutralizer is tapered to follow the beam, which is diverging, due to finite emittance, from a waist located at the exit of the accelerator. The neutralizer pressure distribution and gas load were obtained by numerical integration as in the case of Reference Design 1 but with specified, finite entrance and exit pressures of 1×10^{-4} and 1×10^{-5} Torr, respectively.

Reference Designs 3a and 3b are summarized in Table V-11. The choice of six injectors with two ion sources per injector represents an attempt to minimize the number of injectors while holding the pumping requirements per injector within reasonable bounds.

10. Overall Injector Efficiency as a Function of Beam Energy

Neutral beam injector designs for EPR call for beam energies of 180 keV. Higher beam energies may be required to insure adequate penetration. For optimum target thickness, the neutral fraction and, hence, the power efficiency associated with the neutralization process $D^+ \rightarrow D^0$ in D_2 gas decreases from $\sim 20\%$ at 200 keV to less than 1% at 600 keV (see Figure V-9).¹⁸ Over the same energy range and for optimum target thickness, the efficiency associated with the stripping process $D^- \rightarrow D^0$ is essentially constant with a value of $\sim 60\%$ for a D_2 gas target; for a lithium vapor target, the efficiency is $\sim 65\%$ and for a lithium plasma target it is $> 80\%$. Thus, there is a strong

Figure V-8. Preliminary layout of D^- beam line for Reference Design 3-a.

Figure V-9. Maximum Power Efficiencies for the Neutralization Processes $D^- \rightarrow D^0$ and $D_j^+ \rightarrow D^0$, $j = 1, 2, 3$ as Functions of Energy per Deuteron. Solid Curves are from Reference 18. The Dashed Curve is a Conservative Extrapolation from Data Measured at Higher Equivalent Deuteron Energies for $H \rightarrow H^0$ (Reference 17).

incentive for the development of direct-extraction D^- sources with dc current densities and gas efficiencies comparable to those obtained with present-day D^+ sources.

Reference Designs 1, 2, 3a and 3b are summarized in Table V-11. The beam energy is limited to 180 keV in each case. For Reference Designs 1 and 2, this choice was influenced by the rapid decrease in neutralization efficiency and the consequent loss in overall injector efficiency with increasing beam energy. For the preliminary designs 3a and 3b, the decision to use the same energy as for the other designs was arbitrary. Given the virtual constancy of the neutralization efficiency with energy for $D^- \rightarrow D^0$, a higher energy (for a given beam power) means a lower ion beam current, a smaller beam diameter and a smaller neutralizer bore; smaller gas loads, lower pressures, smaller beam losses and a higher overall neutral injector efficiency follow.

11. Injector Mechanical Design Description

Several layouts were prepared to utilize the limited space available for coupling the neutral beam injector apparatus with the reactor. Limited space between the TF coils as well as the requirement for sufficient shielding to protect the coils from plasma neutrons severely limit the number of acceptable arrangements for the neutral beam injectors (see Figure V-10). In addition, the rather large volume of the injector apparatus and its associated vacuum pumping systems was one of the prime factors influencing design of the reactor facility as well as of the injector itself. Twelve neutral beam injectors are shown in Figure V-10 in four groups of three injectors. Alternate groups are aimed clockwise or counterclockwise to conserve overall system momentum. Two additional neutral beam injectors may be added later in place of two radio frequency units if desired.

The neutral beam injector is shown in Figure V-11. Two beam lines mounted in the same vertical plane aim at a 0.75 m diameter first wall aperture. The included angle between the beam lines is 20° . Each beam line consists of the ion source, M-1 magnet and neutralizer. There is a common M-2 magnet for both lines. Each magnet has a collector converter for energy recovery. Both beam line components are mounted in a single vacuum chamber 9 m L x 7 m H x 4 m W. The inner wall of the chamber is lined completely with cryosorption panels ($\sim 250 \text{ m}^2$ total cryosorption surface). These panels can supply 125 m^2 of continuous pumping while the remaining 125 m^2 are being regenerated. The injector chamber is joined to the reactor vacuum chamber by a $\sim 3.75 \text{ m}$ long duct. The

Figure V-10. ANL Experimental Power Reactor Plan View

Figure V-11. ANL EPR Neutral Beam Injector

duct is fitted with a double ported isolation valve, a bellows section to accommodate differential expansion and two remote disconnect flanges. The inner surface of the duct is designed in a similar fashion as the first wall and requires separately cooled low Z coated panels. Special shielding completely surrounds the duct and extends over portions of the injector housing (described in Chapter IX). Three specially formed pieces of shielding are located within the injector to reduce some of the direct plasma radiation streaming. Liquid nitrogen cooled chevron baffles are used as radiation shields to further reduce the heat load on the liquid helium cooled cryosorption panels.

The neutral beam injector design features a series of remotely adjustable alignment mechanisms for positioning the large chamber and aiming the beams. Each of the twelve units weighs between 150 and 250 tonnes and is equipped to be remotely handled.

B. Radio Frequency Heating of EPR

The plasma startup studies, Chapter III-B, have concluded that from 40 to 60 MW of supplementary heating power will be required to reach ignition operating temperature in EPR. Presently, the most favored candidate to supply the supplementary heating is high energy neutral beam injection, and radio frequency (rf) wave heating is considered as a backup option. Neutral beam injection theoretically and experimentally has been shown to be a very effective supplementary heating method. However, a great deal of development and extrapolation of existing sources is required to reach the level required for efficient heating in EPR. In contrast, rf heating has been what may be called moderately successful both theoretically and experimentally.²⁰⁻²⁴ However very little development and extrapolation of existing or planned sources is required to provide the rf power needed to reach ignition in EPR. In addition, rf heating has the advantage that by varying the frequency or mode, the radial heat deposition profile in the plasma can be controlled. RF heating may also be used to enhance neutral beam injection by tuning the rf to resonate with the injected neutral beam particles.²⁵

The proper role of rf heating, and the optimum mixture of rf and neutral beam heating in EPR awaits the results of experiments planned on present and future large devices such as PLT, PDX, TFTR, JET and T-20. EPR is being planned to take advantage of advances, as they occur, in the state of the

art of rf plasma heating. The neutral beam injection ports are designed without compromising their performance so as to function alternately as rf input ports. In addition, four sectors (sections between TF coils) that are not used for neutral beam ports are designed incorporating rf heating ports. They allow the installation of 25 MW of rf heating power. The use of six additional ports will provide the capability of delivering 60 MW to the plasma.

Currently, the most promising modes for rf heating are ion cyclotron resonance heating (ICRH), first harmonic ICRH, lower-hybrid resonance heating (LHRH) and electron cyclotron resonance heating (ECRH). Of the modes described above, ICRH and LHRH are most favored at this time because the high power tubes and modulators exist to build the rf power system required for EPR at conversion efficiencies of from 50 to 75%.

A program exists to develop a high power, 200 kW C.W. tube at 120 GHz for ECRH in EBT-II.²⁶ However, the research and development program does not promise to deliver a tube until about 1980. The overall dc to rf power efficiency of the system will be about 25%, considering an estimated conversion efficiency for the tube of 30% and 0.2 db/m as the attenuation coefficient of the oversized transmission waveguide. A more realistic value is about 20%. ICRH is considered only briefly in this section of the report because it requires breakthroughs in both tube technology and rf power transmission systems to be competitive. It presently remains a future promising mode of rf supplemental heating. Present estimates are that it will not be as efficient as the others from the point of view of conversion efficiency. If successful, however, it promises the capability of supplying the heating power through a number of small openings in the first wall. A total aperture opening in the first wall of only a few square meters is required for 60 MW of supplemental heating.²⁷ The power can be beamed-in much the same as in the neutral beam injection technique with the added advantage that it can make sharp bends.

Of the three modes described above, LHRH is most favorable for EPR because it will be possible to launch the electromagnetic heating wave into the plasma through the opening of a waveguide, a "Grill" waveguide structure,²⁸ or some yet to be determined antenna system. The LHRH wave launcher will simply be inserted into the port from the outside. It will not be

necessary to make any electrical connection inside the first wall. This will greatly facilitate the installation and maintenance of the launcher since it will not be necessary to work inside the first wall with its high residual radiation level. In contrast for the case of ICRH, a loop will probably be required inside the first wall. The loop must make electrical and cooling water connections to the first wall far removed from the injection port. Consideration of the expected residual radiation levels requires that this be accomplished remotely.

1. Lower Hybrid Resonance Heating

A number of authors^{29,30} have shown that the lower-hybrid resonance layer is accessible only for waves with a sufficiently large parallel refractive index, $n_{||}$, i.e. parallel to magnetic field, given by

$$n_{||} = k_{||}c/\omega \geq (1 + \omega_{pe}^2/\omega_{ce}^2)^{1/2}$$

where ω_{pe} and ω_{ce} are respectively the electron plasma and cyclotron frequencies. More recently, Troyan and Perkins,³¹ and Bers³² have shown the existence of an upper bound on $n_{||}$ of about two for the EPR conditions. The above relationships demonstrate that a rather narrow spectrum of waves must be generated by the wave launcher to achieve penetration. A method of launching a slow wave is required. A possible method for launching such a narrow spectrum is an array of phased waveguides, the "Grill", first proposed by Brambilla.²⁸

The "Grill" structure has been selected as the launcher for LHRH heating for the EPR reference design. It is the most advanced theoretically^{28,33} and experimentally.³⁴ The launcher is shown in Figure V-12. It consists of 16 narrow-height WR1150 waveguides forming a rectangular array, 8 waveguides wide by 2 waveguides high. The narrow dimension is in the direction of the toroidal magnetic field. The array is 0.638 m high by 0.585 m wide, including a water-cooling jacket. As it leaves the blanket and shield area it makes a right angle bend downward. See Figure V-13 for details of the H-plane bend. The "Grill" is made of stainless steel and coated with beryllium to reduce the waveguide attenuation and impurity level due to sputtering. In order to reduce the probability of electrical breakdown in the waveguide feed section and physically separate the high power klystron feed section from the "Grill", a ceramic dielectric window is placed in the bend, as shown in Figure V-14, between the high-power klystron and the "Grill".

Figure V-13. Details of Right Angle H-Plane Bend and "Grill"

Ceramic windows (Al_2O_3 , BeO or some "other" radiation-hardened dielectric material) are placed in the shadow of the reactor shielding to prevent as much as possible the large fluxes of neutrons, gamma rays and ions expected in EPR from reaching the window. The limited data available indicates that radiation seriously degrades the electrical insulating properties of ceramics (see Appendix E). The experiments performed to date are for low radiation fluences and do not indicate what permanent changes in the electrical properties may occur at high damage levels. The thermophysical properties of ceramics, such as thermal conductivity and specific heat, can be significantly degraded by radiation (see Appendix E). Thermal gradients and the associated stresses are likely to increase as these properties change with irradiation. Indeed, the limited data available indicates that further research is required in this area. Insulators that will be able to withstand the particle fluxes in EPR and larger devices will likely be developed in the future.

In addition, in order to further reduce the probability of breakdown in the "Grill", a large vacuum box is built around the waveguides in the vertical leg of the bend, Figure V-13. In this area, the waveguides are physically separated to allow efficient vacuum pumping and routing of the waveguides to individual power sources located below the reactor floor, see Figure V-14. With 30 half-inch diameter holes along the narrow dimension of each of the waveguides, a pressure of 10^{-6} Torr is achieved at the window with one 10,000 l/s pump at "turn-on" of the rf, for details refer to Chapter VII-C.

2. LHRH RF Power Source

A high-power C.W. klystron has been selected as the source for LHRH. The tube will be similar in design to the X3070 and the X3075 klystrons produced by Varian Associates. The above tubes are 500 kW C.W. tubes at 2.4 and 2.1 GHz respectively with conversion efficiency of about 50% and gain of about 58 db. High power C.W. tubes with conversion efficiencies as high as 60% have been developed. Because the LHRH frequency of EPR is different from the above tubes, a program is envisioned for developed of a klystron for EPR. Furthermore, in order to avoid a resonance inside the plasma and achieve optimum coupling, a pump frequency, f_0 , 1.4 times the lower-hybrid frequency, has been selected for the reference design.³⁵ The tube would be 500 kW C.W. with a conversion efficiency of 60% and gain of 58 db at either 1120 MHz at 8 T maximum TF coil field and 1190 MHz at 10 T.

Each waveguide of the array is fed by a separate 500 kW C.W. klystron located below the reactor floor. Figure V-15 contains a block diagram of the high power source and low power drive train for a "Grill". Because it is necessary to adjust the phase of the waveguides relative to each other, each klystron has a separate drive train amplifier and phase shifter. Each of the klystrons and individual drive trains is excited from a common low power signal generator or source. The design of the drive train and signal source is completely conventional and will present no problem in EPR.

The system will be fully integrated so that in case of the detection of high VSWR or arc in the waveguide the rf drive and the high voltage dc modulator will be disabled within a few μ s to protect the klystrons and waveguide/"Grill" windows. In this connection, the drive train contains directional couplers for monitoring VSWR, photo-type arc detectors looking at the high power klystron window and rf pin-diode modulators to turn off the rf within a few μ s in case of a fault.

The high voltage dc power supply for the klystrons is of conventional design. It will be located far removed from the klystrons. A design has been selected in which a set of four adjacent klystrons are fed from a common 64.5 kV, 51 amp, 3.3 MW dc power supply. The power supply is described in Chapter VI-E. The primary power feed is from the superconducting energy storage inductor system as is the case with neutral beams. The power supply voltage will be adjustable from 8 kV to 64.5 kV. The power supply will be fully protected for safety of operating personnel. A crowbar circuit is also included in the design to protect the klystron in case of an electrical fault. A total of four power supply units (13 MW) is required for each Grill. In the event of failure of one of the units, the system will operate at 75% of capacity. This will allow operation of the reactor while or until the unit is repaired.

The dc voltage will be fed to the klystron through either a switch tube modulator or floating deck type modulator. The modulator will allow control of the rf duty cycle of the klystrons. The "on" time of the klystron and rf heating depends, of course, on the energy required for ignition and the installed rf heating power. The EPR design could require as much as 324 MJ. Assuming that a minimum of four "Grills" (25 MW) are installed in EPR, the

94-A

Figure V-15. A block diagram of the high power source and low power drive train for a "Grill"

heating pulse duration will be 12.9 s. If ten units were installed, 60 MW, the pulse duration would be about 5.4 s. The rf heating duty cycle is 17.2% for 25 MW and 7.2% for 60 MW using 75 s as the reference cycle.

The overall dc to rf power efficiency of LHRH is 48%, without thermal recovery, assuming a transmission efficiency of the "Grill" of 90% (95% has been achieved in a four-across waveguide "Grill" by Bernabei²⁴), 60% as the conversion efficiency of the klystron and 0.5 db as the insertion loss of a waveguide transmission system. The use of four "Grills" will allow the transmission of 25 MW to the plasma. If all ports were used for LHRH, 100 MW of rf heating could be supplied to the plasma. The system is designed to be A.C. so that, if required, power could be supplied continuously to the plasma during the burn.

3. Ion Cyclotron Resonance Heating (ICRH)

ICRH is considered to be an option to LHRH in EPR. The high-power tube modulators and transmission system exist to supply the supplemental heating requirements of EPR. However, the rf heating wave launcher is not very attractive since it will probably require a loop, a section of a loop or a coil inside the first wall surrounding the plasma. As such, it will function as a limiter and must be designed to take the first wall loading of EPR. In addition, it may adversely effect confinement because of impurity sputtering. The loop will probably need to be surrounded by a Faraday cage to reduce electrostatic field levels. It may also require an insulator between it and the first wall and plasma to prevent electrical breakdown. However, if a solution to the materials problem exists, then fast-wave ICRH, at twice ICR frequency, promises to be an efficient heating method of EPR. An overall efficiency of about 64% is expected, i.e., rf power into the plasma from the loop divided by the total dc power input to the final amplifier. If the materials problem can be solved, it may be possible to launch the ICRH from a dielectric loaded waveguide array.

Two quarter section loops (1/4 turn) have been selected as the launcher for ICRH for the EPR reference design. Similar designs have been successfully used on the ST tokamak and have been proposed for PLT. Figure V-16

Figure V-16. Details of the loops and power feed system of ICRH for $\bar{a}$ port

shows details of the loops and power feed system. The loops are water-cooled and make both electrical and cooling water connection to the first wall at the top and bottom of the torus. The loop is located midway between first wall and minor radius of the plasma. In this design the loops are made of stainless steel and coated with beryllium to reduce high-Z sputtering. They are fed from below through an rf dielectric window and bend arrangement by two 30.5 cm diameter water-cooled coaxial cables each carrying 3.1 MW of power. If four ports are implemented with ICRH, it will be possible to transmit 25 MW to the plasma.

The final amplifiers for each port of ICRH in EPR are conventional. Two Eimax X2170 ceramic/metal, water cooled, power tetrode with 650 kW anode dissipation ratings are used in a push-pull common grid arrangement. Two amplifiers are required per port. Using this design a conversion efficiency of 75% is achieved with a band width of 10%. The band width is required in case it is necessary to track changes in the toroidal-plasma cavity resonant modes with time for optimum coupling. A fast feedback system is incorporated in the design to track and lock-on to the resonant mode.

The remainder of the system is also conventional. Each of the final amplifiers is fed from a regulated 18 kV, 271 amp, 4.8 MW dc power supply Chapter VI-E. The power supply is fully protected. The low level and intermediate power amplifier is conventional and will present no problems in the EPR design. The monitor and control system, i.e., VSWR, arcs, is similar to the design discussed for LHRH.

In summary, systems can be built using current technology to deliver the supplemental heating power to EPR. A summary of the relevant rf input system parameters is presented in Table V-15. The systems promise to deliver the required power at high conversion efficiency ranging from 48% for LHRH to 64% for ICRH. The systems are "small" in size compared to neutral beam heating and moderate in cost. They will be comparatively easy to operate and maintain in the EPR environment. However, plasma heating must be demonstrated in large size devices in order for rf to be completely acceptable as the primary source of supplemental heating.

Table V-15. RF Heating Parameters

	ICRH (fast wave)	LHRH
Pump frequency (MHz) (fast compressional mode)	54 at 8 T max TF coil field 68.6 at 10 T max TF coil field	1120 at 8 T 1190 at 10 T
Output power (MW)	25 using 4 ports 60 using 10 ports	25 using 4 ports 60 using 10 ports
DC to rf power efficiency from source to port (%)*	64	48
Heating Pulse duration, (seconds)	12.9 for 25 MW 5.4 for 60 MW	12.9 for 25 MW 5.4 for 60 MW
Duty cycle (%)	17.2 for 25 MW 7.2 for 60 MW	17.2 for 25 MW 7.2 for 60 MW
Launcher	$\frac{1}{4}$ turn loops	"Grill" waveguide 8 across by 2 high
Transmission Scheme	Coaxial cables	Waveguides
High Power Source	tetrode amplifier	Klystron

* Takes into consideration conversion efficiency of final amplifier, transmission line loss and reflection loss from launcher.

C. Research and Development Requirements

1. Neutral Beam Injectors

Much of the research and development on neutral beam injector systems presently directed towards nearer-term fusion programs is directly related to EPR. However, the EPR presents demands on neutral beam injectors that cannot easily be met by present goals.

As is clearly indicated by Figure V-9, the neutralization efficiency of $D^+ \rightarrow D^0$ rapidly decreases with increasing primary beam energy. Neutral beam energies for EPR must be at least 180 keV and more likely much higher if the full potential of higher magnetic fields and higher plasma densities is to be realized. D^- sources offer good neutralization efficiency at high voltage, since stripping probability remains high. The development of D^- sources would greatly benefit the EPR design. Two approaches appear possible. Double charge exchange of D^+ with energy recovery and post acceleration is one possible solution.

Direct D^- extraction offers another alternative. The magnetron source currently in development also has poor gas efficiency and, at the present time, does not appear to be able to develop large current emission at the levels needed for EPR. A second type of direct extraction source is a surface emission source activated by H and Ce bombardment. This type of source appears to offer very good gas efficiency and energy efficiency. The most serious question is whether sufficiently high current densities can be achieved.

Energy recovery systems should also be developed. If the neutral beam injection energy can be kept sufficiently low, the implementation of an energy recovery system might make D^+ sources viable for EPR. Even though energy recovery systems are not absolutely vital to operation of a D^- injector, they will provide a sufficient improvement in overall operating efficiency to warrant their development.

2. RF Heating Systems

Radio frequency heating is the most attractive alternative to neutral beam heating. The most pressing questions concerning rf heating are related to plasma interaction. Many experiments designed to investigate the interaction of rf waves with plasma are in progress or planned. If the results of these

experiments prove encouraging, then implementation of wave heating on EPR would be possible with a minimum amount of technology development; at least in the ICR and LHR modes, since adequate power sources are commercially available.

The primary technology concern of implementing ICR heating is the coupling device. Aperture coupling is not feasible because of the low frequency, and so some form of loop or antenna is required. The loop must survive in the same hostile climate as the first wall of the reactor and, in the process, must not contaminate the plasma.

At LHR frequencies, aperture coupling is possible. However, the actual design of the coupling array, the power handling capability of the array, and waveguide vacuum windows are not fully possible without further development.

At ECR heating frequencies, the technology of generating sufficiently high powers and efficiently transporting the high frequency power does not exist. Substantial research and development is necessary on all phases of ECR equipment before a design can be implemented.

-
1. W. M. Stacey, Jr., et al., "Tokamak Experimental Power Reactor Studies," ANL/CTR-75-2, Argonne National Laboratory (June, 1975).
 2. J. A. Fasolo, Argonne National Laboratory (1975) (unpublished).
 3. J. A. Fasolo, Argonne National Laboratory (1976) (unpublished).
 4. W. M. Stacey, Jr., et al., "Tokamak Engineering Technology Facility Scoping Study," ANL/CTR-76-1, Argonne National Laboratory (March, 1976).
 5. K. W. Ehlers, et al., "Design and Operation of an Intense Neutral Beam Source," *J. of Vac. Sci. and Tech.* 10, 922 (1973).
 6. H. H. Haselton, et al., "Extraction Systems for Neutral Beam Heating of PLT and Doublet-III Tokamaks," Presented at the Sixth IEEE Symposium on Engineering Problems of Fusion Research, San Diego, California (November, 1975).
 7. A. B. Wittkower, et al., "Small-Angle Scattering Observed in the Formation of Neutral Atoms from 10- to 55-keV Positive Ion Beams," *Phys. Rev.* 136, A 1254 (1964).
 8. R. W. Moir, "A Review of Direct Energy Conversion of Ion Beams: Experimental Results and Reactor Applications," Proc. of First Topical Meeting on the Technology of Controlled Nuclear Fusion, San Diego, California, 1, 432 (April, 1974).

9. D. J. Bender, W. L. Barr and R. W. Moir, "Performance Analysis of In-line Direct Converters for Neutral Beam Sources," Presented at the Sixth IEEE Symposium on Engineering Problems of Fusion Research, San Diego, California (November, 1975).
10. S. K. Allison and M. Garcia-Munoz, Atomic and Nuclear Processes, Edited by D. R. Bates, Academic Press (1962).
11. C. F. Barnett, J. A. Ray and J. C. Thompson, "Atomic and Molecular Collision Cross Sections of Interest in Controlled Thermonuclear Research," ORNL-3113, Revised, Oak Ridge National Laboratory, p. 60 (1964).
12. H. Tawara and A. Russek, "Charge Changing Processes in Hydrogen Beams," Rev. Mod. Phys. 45, 178 (1973).
13. G. W. McClure, "Charge Exchange and Dissociation of H^+ , H_2^+ and H_3^+ Ions Incident on H_2 Gas," Phys. Rev., 130, 1852 (1963)
14. S. Dushman, Scientific Foundations of Vacuum Techniques, Edited by J. M. Lafferty, John Wiley & Sons, Inc. (1962).
15. S. K. Allison, "Experimental Results on Charge-Changing Collisions by Hydrogen and Helium Atoms and Ions at Kinetic Energies Above 0.2 keV," Rev. Mod. Phys. 30, 1137 (1958).
16. B. A. D'yachkov, "Production of High-Energy Neutral Atoms by Conversion of H^+ , H_2^+ and H_3^+ Ions in a Supersonic Lithium-Vapor Jet," Sov. Phys. - Tech. Phys. 13, 1036 (1969).
17. G. I. Dimov and G. V. Roslyakov, "Conversion of a Beam of Negative Hydrogen Ions to Atomic Hydrogen in a Plasma Target at Energies Between 0.5 and 1 MeV," Nucl. Fusion 15, 551 (1975).
18. K. H. Berkner, R. V. Pyle and J. W. Stearns, "Mixed Species in Intense Neutral Beams," Proc. of First Topical Meeting on the Technology of Controlled Nuclear Fusion, April 16-18, 1974, San Diego, California, 1, 392 (1975).
19. Symposium on Plasma Heating in Toroidal Devices, International School of Plasma Physics, September 4-17, 1974, Varenna, Italy.
20. A. M. Messiah and P. E. Vandenplas, "Comprehensive Theory of rf Energy Absorption by a Hot Ion-Electron Plasma Cylinder Excited by an Arbitrary Electromagnetic Field," Plasma Phys. 15, 505-533 (1973).
21. J. Adam, et al., "Wave Generation and Heating in the ST Tokamak at the Fundamental and Harmonic Ion Cyclotron Frequencies," Fifth International Conference on Plasma Physics and Controlled Fusion Research, IAEA-CN-33/A3-2, Tokyo, Japan (1974).

22. J. Hosea, Princeton Plasma Physics Laboratory, private communication of the latest results of ICRH in the ATC (June, 1976).
23. S. Bernabei, Princeton Plasma Physics Laboratory, private communication of the latest results of LHRH in the ATC (June, 1976).
24. T. H. Stix, "Fast-Wave Heating of a Two-Component Plasma," Symposium on Plasma Heating in Toroidal Devices, September 4-17, 1974, Varenna, Italy, p. 105.
25. R. A. Dandl, "Research Program for Plasma Confinement and Heating in Elmo Bumpy Torus Devices," ORNL-TM-4941, Oak Ridge National Laboratory (June, 1975).
26. "The Experimental Thermonuclear Device, Tokamak-20," ERDA-TR-58, Vols. 1 and 2, Moscow (1975)
27. M. Brambilla, "Slow-Wave Launching of the Lower Hybrid Frequency Using A Phased Waveguide Array," Nucl. Fusion 15, 47 (1976).
28. T. H. Stix, The Theory of Plasma Waves, McGraw-Hill Publ. Co. New York, p. 65 (1962).
29. V. E. Golant, "Plasma Penetration Near the Lower Hybrid Frequency," Sov. Phys. - Tech. Phys. 16(12) (June, 1972).
30. F. Troyan and F. W. Perkins, "Lower-Hybrid Heating in A Large Tokamak," Proc. of Second Topical Conference of RF Plasma Heating, B-4, Lubbock, Texas (1974).
31. A. Bers, "Ion Heating in Tokamaks by Wave Penetration and Parametric Down-conversion of RF Power," Proc. of the US-Australian Workshop on Plasma Waves, paper 5, Ed., R. L. Cross (1975).
32. P. Lallia, "A LHR Slow Wave Launching Structure Suited for Large Toroidal Experiments," Proc. of the Second Topical Conference on RF Plasma Heating, C-3, Lubbock, Texas (1974).
33. S. Bernabei, et al., "Penetration of Slow Waves into a Dense Plasma Using a Phased-Guide Array," Phys. Rev. Lett. 34(14), 866 (April 7, 1975).
34. A. Rogister and G. Hasselberg, "Theory of Parametric Heating of the Plasma in the Lower Hybrid Frequency Range," The Physics of Fluids, 19, No. 1, 108 (January 1976).

CHAPTER VI

ENERGY STORAGE AND TRANSFER

Because the EPR design employs superconducting poloidal field magnets, the design of the matching power supplies must include the ability to store, transfer and recover gigajoules of energy efficiently at repetition rates of about once per minute. Conventional power supplies are not well suited to meeting the EPR design goals because they tend to be too costly and inefficient in this mode of operation. Therefore, the designs of the poloidal field power supplies do not incorporate existent and proven technologies.

The OH coil power supply is based upon energy storage in the coil itself. This energy is transferred in and out of the coil through an inertial energy storage device in the form of radially stacked, drum type homopolar generators to obtain a half-sinusoid voltage waveform. The length of the half sinusoid is controlled by adjusting the effective capacitance of the homopolar generators to match the inductance of the OH coil. The start and stop action is controlled by operation of a switch across the OH coil. Energy losses, including power supply losses and plasma heating are resupplied by an auxiliary rectifier inverter operating out of the central energy storage inductor. This rectifier/inverter also provides voltage to maintain plasma current during the burn phase.

The EF coil power supply is designed to have an actively-controlled rate of energy change using a three-phase inductor-converter (I-C) SCR bridge between the EF coil and a central superconducting energy storage inductor.

The central energy storage inductor is also used to smooth the power demand from the electrical grid for the neutral beam and rf heating systems. Energy is added to the central storage inductor at almost constant rate throughout the whole fusion reactor cycle. Energy is extracted from the central storage inductor at much higher rates than the average input rate during the heating periods. The power grid only sees the relatively low, average power.

The power supplies for the neutral beam and rf heating systems also employ technology which is beyond the current state-of-the-art, in order to avoid excessive power bumps on the electrical grid and, in the case of the neutral

beam injector supply, to also take advantage of the superior qualities of a new technology. The design of the energy transfer network for the neutral beam injector uses an I-C to drive a high frequency polyphase rectifier. The injector is protected against spark damage by using a saturated time-delay transformer (STDT) to limit surge currents at the outset and to phase back on the high frequency polyphase rectifiers within the first 100 μ s for longer surge periods. The design of the rf heating power supply uses a five-phase I-C so that voltage ripple on the power amplifier can be minimized.

A. Toroidal-Field Coil System

The inductance of the TF coil is 16.7 H, and the operating current is 60,000 A. A 6 V, 60,000 A dc power supply charges the TF coils to full current in 48 h. A conventional dc power supply is used in this design. Special design effort is required to protect against voltage surges on the supply during unexpected plasma dumps.

B. Ohmic-Heating Coil System

The OH coil current is reversed at the start and end of the burn cycle. This is accomplished by matching the capacitive value of a homopolar generator to the inductance of the OH coil so that a free oscillation will take place at the required frequency.

The reversal period is planned to be two seconds or longer. The maximum energy transfer called for in the power supply reference case is 1060 MJ, but for design purposes, 1200 MJ is chosen in order to handle the more demanding conditions of high plasma resistivity or high plasma impurity which might be expected during some operations of an experimental device (see Table III-11).

The OH coil inductance is 0.4794 H, which means that 67,745 A must be switched by the homopolar generators to meet the 1200 MJ energy transfer requirements. A sinusoidal reversal in 2 s requires 51.1 kV (48 kV for PS reference case), a value which is substantially higher than is typical in ordinary homopolar generators. In order to match the high voltage and relatively low current, by homopolar generator standards, a radially stacked drum-type homopolar generator, referred to as the counter cyclonic generator (CCG),^{1,2} has been designed.

An axial cross section of the CCG is shown in Figure VI-1. Several cylindrical shells are aligned about a central axis. The innermost cylinder is made of an insulating material. The following cylinder is conducting material. The next cylinder is again made of insulating material, etc. The last cylinder is made of insulating material. The insulating cylinders are aligned and supported by insulating spokes emanating from the central core. The conducting cylinders are free to rotate inside the stationary insulating cylinders. The conducting cylinders are supported by a forced air flow system routed through the insulating cylinders.

Brushes are located uniformly around the edges of the conducting cylinders. Electrical connections between cylinders are shown in Figure VI-1. The current in adjacent cylinders flows axially in opposite directions, forcing adjacent conducting cylinders to rotate in opposite directions.

A radially directed, azimuthally uniform magnetic flux crosses the cylinders and is directed axially outwards through the end regions. The individual cylinders are drum-type homopolar generators that are subjected to tangential forces as a result of the radially directed magnetic field and axially directed currents. The force, F , is

$$F = i \int_0^l B_r dl = i \langle B_l \rangle \quad (\text{VI-1})$$

where B_r is the radial flux, i is the axial current and dl is the incremental length along the axis. The rotation of the cylinder in a magnetic field induces a voltage, V , across the length of cylinder which is given by

$$V = v \langle B_l \rangle \quad (\text{VI-2})$$

where v is the surface velocity of the cylinder. The time rate of change of voltage as a function of current, i , is

$$\dot{V} = \frac{i r^2 \langle B_l \rangle^2}{J} \quad (\text{VI-3})$$

where r is the radius of the cylinder and J is the moment of inertia. The electrical analogue of the drum-type homopolar generator is a capacitor, the value of which is

$$C = \frac{J}{\langle B_l \rangle^2 r^2} \quad (\text{VI-4})$$

Figure VI-1. Cross section of the region of insulating cylinders and conducting drums for a typical CCG.

The moment of inertia of a thin shell cylinder is approximately

$$J = 2\pi \rho l r^3 \Delta r \quad (\text{VI-5})$$

where ρ is the density of the drum material, l is the drum length and Δr is the radial thickness of the shell.

The drums are electrically connected in series to operate at the highest possible voltage per unit length. Each cylinder is designed to operate at the highest possible surface velocity consistent with the structural strength of the cylinder. In the case of thin shell cylinders, the stress is proportional to the square of the surface velocity. Therefore, the optimum design is one for which the effective capacitance of each shell is chosen to give a voltage distribution in the generator which forces each shell to rotate at the same surface velocity. For a given sinusoidal current, $I \cos \omega t$, the resulting voltage is

$$V = \frac{I}{\omega C} \sin \omega t \quad (\text{VI-6})$$

The drum thickness that produces the required capacitance is

$$\Delta r = \frac{I \langle B l \rangle}{2\pi^2 \rho l r v} \quad (\text{VI-7})$$

The basic energy storage unit for the OH coil is envisioned to be several series-connected, CCG-type homopolar machines. A sketch showing the coils, rotating drums and iron core and shield layout is presented in Figure VI-2. A flux map is shown in Figure VI-3. The free space available for conducting and insulating drums extends from a radius of 0.55 m to about 0.85 m. The dimensional and operational parameters for the CCG module based on the yoke design shown in Figure VI-3 are presented in Table VI-1. The design uses 17-4 stainless steel drums which are 0.55 m long and fiberglass insulating cylinders which have a radial thickness of 0.01905 m. The values of induced voltage and stored energy are based on the stainless steel drums reaching surface speeds of 236 m/s. At these speeds, the stress levels reach 414 MN/m² and the drums expand radially by as much as 0.0016 m. An air gap of 0.003 m per drum is provided in the CCG design to allow for this expansion.

The stainless steel drums are capable of withstanding this stress and expansion without having a serious fatigue problem. The line on the endurance

Figure VI-2. Sketch of CCG Geometry

Figure VI-3. Magnetic Field Plot For CCG

TABLE VI-1. Parameters for a Single CCG Module

Cylinder Number	Inner Radius m	<b1> T-m	Radial Thickness m	Induced Voltage V	Effective Capacitance f	Stored Energy MJ
Inner Insulator	0.55	-	0.019	-	-	-
1	0.569	2.4	0.0286	566	72.96	11.7
2	0.6196	2.49	0.0272	588	70.33	12.1
3	0.6689	2.60	0.0264	614	67.35	12.7
4	0.7174	2.72	0.0257	642	64.38	13.3
5	0.7651	2.86	0.0253	675	61.23	13.9
6	0.8125	3.03	0.0253	715	57.79	14.8
Outer Insulator	0.8408	-	0.009	-	-	-
Total Unit	-	-	-	3800	10.88	78.5

chart shown in Figure VI-4 represents a safe limit for fatigue. The point shown on the chart indicates the design value for the 17-4 stainless steel cylinders. This design point is a safe operating value and should lead to a virtually infinite fatigue life.

The forced air flow system is designed to support the conducting cylinders in the presence of the radial expansion of the drums. The axes of the CCG generators are oriented in the vertical direction to make use of the gravitational force. The bottom edges of the conducting cylinders are tapered and the forced air flow outlets are on matching tapers in the insulating drums. As the drums expand, the conducting cylinder slides downward on the tapered surface, maintaining the same air gap as defined by the air pressure and flow. The upper ends of the cylinders are supported by teflon buttons which have several air holes connecting an air chamber in the insulating drum and the button face in contact with the rotating cylinders. Air pressure from the rear side of the button pushes the button towards the cylinder, but the air flow released through the holes in the button develops a back pressure that restrains the buttons from making direct contact with the rotating cylinders.

Brushes for the CCG module are based on the type used by R. A. Marshall.³ Each cylinder requires 60 brushes, 0.01 m² on each side. The distance between brush centers is 0.06 m. Each brush handles 1130 A. The voltage drop on a Marshall brush operated under these conditions is about 0.4 V per half cylinder, or 4.8 V per CCG module. The energy loss due to brushes per CCG module per fusion reactor cycle is

$$U_v = 4 \int_0^{\frac{\pi}{2\omega}} V_b I_m |\cos \omega t| dt$$

$$\approx \frac{4 V_b I_m}{\omega} \quad (VI-8)$$

where U_v is the energy loss per fusion reactor cycle due to voltage drop on brushes, and V_b is the voltage drop per CCG module. About 1.05% of the energy transferred is dissipated due to voltage drops across the brushes.

The Marshall brushes are operated with a contact pressure of 110 kN/m² (16 lb/in.²). The coefficient of friction for these brushes is 0.18.

Figure VI-4. Goodman Endurance Chart for CCG 17-4 stainless steel cylinders.

Therefore, the energy loss per CCG module per fusion reactor cycle due to brush friction is given by

$$U_F = d p a a \quad (\text{VI-9})$$

where U_F is the energy loss due to brush friction loss per CCG module in a fusion reactor cycle, d is the distance travelled by brush surfaces in a total fusion reactor cycle, p is the pressure on the brushes, a is the total contact area of brushes per CCG module, and a is the coefficient of friction. Approximately 1.09% of the energy transferred is lost per fusion reactor cycle due to brush friction. The resistance of each drum is about $3.6 \mu\Omega$. I^2R losses in the 96 drums are 0.36% of the energy transferred per fusion reactor cycle.

The series connection of 16 CCG modules described in Table VI-1 will more than satisfy the demands of the OH coil. The series-connected modules can store 1256 MJ at a voltage of 60,794 V. The minimum value of capacitance is 0.68 F.

The minimum field reversal time in the OH coil circuit, neglecting lead inductance and other strays, is 1.8 s. Any reversal time longer than 1.8 second can be achieved by simply reducing the B field on the CCG modules. For instance, suppose the desired reversal time is τ , where $\tau > 1.8$ s, then

$$\tau = \pi / \omega \quad (\text{VI-10})$$

where

$$\omega = 1/\sqrt{LC} ,$$

or

$$\pi^2 / \tau^2 = 1/LC$$

and

$$C = \tau^2 / \pi^2 L . \quad (\text{VI-11})$$

The value of capacitance, C , is also given by Equation IV-4. Since J divided by r^2 is simply the mass of the cylinder, m , the value of magnetic field for an arbitrary period, B_τ , is

$$B_\tau = \sqrt{\frac{m \pi^2 L}{\tau^2 \tau^2}} . \quad (\text{VI-12})$$

Equation VI-12 results from setting the value of C from Equation VI-11 equal to that of Equation VI-4. The peak voltage that can be generated with field,

B_τ , is $V_\tau = B_\tau \cdot \ell \cdot v$

or

$$V_{\tau} = (B_m \times v) \left(\frac{\tau_{\min}}{\tau} \right) \quad (\text{VI-13})$$

where B_m is the maximum CCG field corresponding to the shortest value of τ , $\tau_{\min}$. The voltage required to force field reversal in a time τ is,

$$\begin{aligned} V_{\tau} &= \omega L I_0, \\ &= \frac{\pi}{\tau} L I_0, \\ &= V_0 \left(\frac{\tau_{\min}}{\tau} \right) \end{aligned} \quad (\text{VI-14})$$

In conclusion, any oscillating period equal to or longer than the period corresponding to the maximum CCG field can be matched by simply reducing the magnetic field.

The OH coil circuit diagram is shown in Figure VI-5 and the OH coil operation cycle is shown in Figures III-21 and III-23. As can be seen in Figure III-21, the OH coil current is reversed at the start of the burn cycle and again at the end of the burn cycle. During the burn cycle, a small voltage and di/dt is required to maintain the flow of plasma current. Roughly, 5 V-s is needed during the burn phase for a 1 minute burn. Towards the end of the cycle, the current in the OH coil is returned to the same value as at the start of the burn when the OH coil current went through its first reversal.

A controllable power supply is required to satisfy the burn cycle demands. The design of this power supply is a smaller version of the energy storage and transfer system described in the EF coil section. The system consists of a central superconducting energy storage coil which stores the energy needed for adding and subtracting ~ 5 V-s to the OH coil and an inductor-converter (I-C) SCR-capacitor bridge for transferring the energy. The central energy storage inductor used for this supply is also used for the EF coil power supply, the neutral beam power supply, and the rf heating power supply. The inductor-converter bridge is described in detail in the following section on the EF coil. The I-C must be able to transfer a peak power of 66 MW at a peak voltage of 800 V in order to remove the burn Volt-seconds from the OH coil at the end of the burn cycle. Since the energy removal sets the requirement for the rectifier, the system will handle longer burn periods as well, until the OH coil reaches its maximum magnetic field. Losses in this system are about 20 MJ per fusion reactor cycle.

VI-13

Figure VI-5. Circuit Representation for Ohmic Heating Coil System

The shorting switch, which is connected across the OH coil and in series with the I-C circuit, must be capable of carrying the full OH coil current of 80,000 A and must be capable of interrupting this current under "no voltage" conditions. The switch must be able to isolate the peak OH coil voltage of 61,000 V. These requirements are satisfied by rapid-acting mechanical switches in parallel with vacuum interrupters.

C. Equilibrium-Field Coil System

The EF coil power and energy cycle is shown in Figure III-24. The power supply for the EF coil must be an actively controlled supply because the demand on field level varies with plasma current and temperature. The design of the power supply is based on a superconducting magnetic energy storage (MES) and inductor-converter (I-C) energy transfer scheme suggested by Peterson, et al.⁴

A basic circuit diagram is shown in Figure VI-6. The superconducting magnetic energy storage unit is shown on the left-hand side of the circuit diagram and denoted by L_S . The EF coil is shown on the right-hand side of the circuit diagram and denoted by L_E . The energy flow in the network is reversible; and if L_S is equal to L_E , nearly 100% of the energy can be transferred into and out of each coil. However, in the present design, L_S is a central energy storage inductor used for the EF coil, OH coil, neutral beams and rf heating system. The capacitors in the circuit are able to store only a small fraction of the magnetic energy at any instant of time without suffering damage.

The I-C network can be actively controlled in two ways; either by controlling the frequency at which the two SCR bridge circuits operate, or the relative phasing between the LH and RH bridges. The network behaves differently as the relative balance between L_S and L_E changes. In order to describe the network behavior, L_S and L_E are assumed to be equal. The basic SCR cyclic switching sequence followed is $SX1 SX5 \rightarrow SX1 SX6 \rightarrow SX2 SX6 \rightarrow SX2 SX4 \rightarrow SX3 SX4 \rightarrow SX3 SX5 \rightarrow SX1 SX5$, etc., where X is either L or R. The time increment between switching events is constant for relatively long intervals, if there are no short-term changes required during the interval.

Figure VI-6. Inductor-Converter Bridge Circuit Diagram

Relative phase between the LH and RH bridges is measured relative to the time of cycle when identical switching events take place in the two bridges.

When $I_S \gg I_E$, or $I_E \simeq 0$, the voltage waveforms of V_{c1} , V_{c2} , V_{c3} , V_A , V_B and V_C are completely dominated by I_S . The voltage waveforms for V_{c1} , V_{c2} , V_{c3} , V_{LS} , and V_{LE} are shown in Figure VI-7. The voltage waveform for V_{LE} corresponds to a phase angle between LHB and RHB which provides the maximum integrated average value across L_E over a switching cycle. L_E and L_S act as a constant current generator relative to the behavior of the capacitors, since they are designed to remove only small amounts of energy from the inductors in a switching sequence. Therefore, the voltage, V_{cx} , on any capacitor, C_x , is given by

$$V_{cx} = \frac{I_{cx} \Delta t}{C_x} + V_{IN} \quad (VI-15)$$

where I_{cx} is the value of current entering C_x on the dotted side, Δt is the length of time increment between switch intervals, and V_{IN} is the voltage on C_x at the start of switch interval.

For the case where $I_E \simeq 0$, the values of I_{cx} are only $+I_S$, $-I_S$ and 0. The value V_0 shown in Figure VI-7 is controlled by I_S , Δt and C . Thus, for a given circuit design where I_S and C are chosen, V_0 is varied by varying the frequency of the switching sequence. The integrated average value of V_{LE} can also be altered by changing the relative phase between LHB and RHB; in which case, the wave shape of V_{LE} is altered so that the size of ripple in the voltage is substantially increased.

The voltage across L_S varies back and forth between V_0 and $-V_0$. This is a natural result of having all the current in one inductor. The capacitor can be rapidly charged from L_S , but takes an extremely long time to discharge into L_E . The switching scheme alternately discharges the capacitors into L_S and then recharges them from L_S .

Figure VI-8 shows the voltage waveforms when $I_S = I_E$ and the relative phase angle is adjusted to give a symmetrical voltage waveform. The wave shape of the capacitor voltage is affected by the current I_E , as would be

Figure VI-7. Inductor-Converter Voltage Wave Forms when $I_s \gg I_E$

Figure VI-8. Inductor-Converter Voltage Wave forms when $I_s = I_E$.

the case for any condition where I_S is not infinitely larger than I_E . Figure VI-9 shows voltage waveforms when $I_S \ll I_E$.

The timing chart for switching the two bridges under symmetrical waveform operation is shown in Figure VI-10.

Actual EF coil power supply design is based on typical SCR parameters for currently available power devices. The SCR design parameters are listed in Table VI-2. The lower limit on the size of capacitance is determined, as indicated by Equation VI-13, from the SCR turn-off time, the value of storage inductor current, and the lowest voltage required in the EF coil. The lowest EF coil voltage is about 900 V recorded at about 0.1 s after startup. The energy storage inductor is almost at peak current, and the EF coil current is almost at zero. Allowing an SCR conduction time of approximately twice the turnoff time, or about 250 μ s between SCR switching steps, the highest switching frequency is 1330 Hz. The design value of the capacitors is 35 mF, which allows operating in the mode depicted in Figure VI-7 with a value of V_0 equal to 570 V. Allowance is made for a 100 V SCR voltage drop. The highest voltage demand of 20,280 V occurs at 1.25 s into startup. At this point in the cycle, the average integrated coil voltage is approximately 1.875 times the capacitor voltage. A switching frequency of 120 cps will lead to the correct average coil voltage. A design value of 15 kV provides adequate safety margin, in the event shorter heating periods become necessary. The stored energy per capacitor bank is 3.95 MJ. Commercial capacitors are available in this voltage range but not in this capacitance value. This presents no problem since single units can be easily paralleled using a relatively simple fuse arrangement to protect against excessive energy dump in the event of a capacitor failure.

The design of each SCR switch shown in Figure VI-6 consists of an array of 21 SCR units in series and 76 SCR units in parallel. Each SCR unit is operated with a current of only 75% of the continuous current handling limit and reverse bias voltage of 2/3 the peak inverse rating. Saturable inductors of 33.3 μ H value are in series with each of the 76 parallel branches, so that the di/dt is kept within the limits specified in Table VI-2. One turn on a Deltamax core with an 0.15 m square cross section provides the necessary protection against excessive current rise time.

The energy loss with this design is about 60 MJ per fusion reactor cycle.

Figure VI-9. Inductor-Converter Voltage Wave forms when $I_s \ll I_E$.

Figure VI-10. Relative Timing Sequence for Switching SCR's to Produce Symmetrical Voltage Waves with Inductor-Converter Network for $I_s \gg I_E$, $I_s = I_E$, and $I_s \ll I_E$.

TABLE VI-2. SCR Design Parameters for EF Coil Inverter-
Converter Network Design

RMS Peak Current	1600 A
Current Handling Limit at 120° Conduction Angle	1400 A
Design Current per SCR	1050 A
Peak Inverse Voltage (Repetitive)	1500 V
Design Inverse Voltage	1000 V
Turn-Off Time	100 to 120 μ s
di/dt Rating	800 A/ μ s
dv/dt Rating	200 V/ μ s
Voltage Drop at Maximum Current Conduction	1.75 V

D. Neutral Beam Power Supplies

A total of 12 beam injection ports are used for 24 neutral beam lines. Each beam line power supply unit is rated 180 kV 113 A, for a total peak power requirement of 485 MW. The injectors operate for approximately 5 s every 70 s.

The design of the neutral beam system includes energy recovery, both direct electrically and thermally from the beam dumps as well. The thermally recovered energy enters the steam system and is returned over the whole fusion cycle. The electrical energy recovered comes back essentially simultaneously and must be included in the power supply design. Although a detailed design of the energy recovery system has not been made, it is assumed that the system will have similar characteristics to those of the Novosibirsk electron cooling system, which successfully demonstrates high efficiency electrical energy recovery. In this case the recovery ion current flows back into the power supply at a reduced (collecting) voltage, thus reducing the requirement for the power supply. In this case the reduction in power supply is dramatic because of the low neutralization efficiency of the 180 keV beam. Of the 485 MW flowing to the injector system, 278 MW is recovered

electrically, reducing the power supply requirement to 207 MW. Alternately one might build a separate energy recovery power supply system which returned the recovered energy to the energy storage inductor, but in this case the total power supply system would be required to handle 763 MW, and would be almost four times as expensive.

The voltages to the electrodes of an injector must be applied within a short time to limit heating of the electrode grids. In case of an arc within an injector, the HV must be removed before damage occurs. These switching functions are presently performed by ignitrons and hard tube modulators (switchtubes) for voltages ≤ 60 kV, as illustrated in Figure VI-11. At the injector power level required for the EPR, large numbers of ignitrons must be connected in series and parallel to switch-on, or crowbar. Vacuum switchtubes to interrupt at these voltage and power levels do not exist; and when developed, may themselves be subject to damage from arcing. One major disadvantage of the circuit of Figure VI-11 is that the signal required to operate the crowbar results from the application of excessive arc current to the injector. Another is that the switching and control functions are done at the high voltage (HV) level.

The 207 MW power pulses required for the injectors would cause intolerable voltage fluctuations, "flicker," for the electric power system. Therefore, pulsing power is accumulated and stored between pulses in the central superconductive inductor. The inductor buffers the injector load fluctuation from the power system to such an extent that voltage flicker will be negligible. It also will eliminate "peak demand charges" by the electric utility company.

Arc currents can be limited with a saturated time delay transformer (STDT)⁵ connected in series with the injector. The STDT detects overcurrent and limits it until the arc has been extinguished or the HV power source removed. It appears possible to eliminate the need for vacuum switchtubes and ignitrons by developing high power HV supplies that can react fast enough to be compatible with the requirements of a neutral beam injector and STDT.

A simplified conceptual design of such an HV power supply system is shown in Figure VI-12. Pulsing power for the system is stored in the superconductive inductor L_0 . It is charged from the three-phase power system by

Figure VI-11. Conventional NBI Power Supply System

Figure VI-12. HV Power Supply for NBI Block Diagram

means of an ac/dc converter. Energy can be drawn from the power system and stored in the inductor or alternately returned to the power system as desired by appropriate phase control of the converter.

The energy stored in inductor L_0 provides a precisely regulated dc voltage source by means of the dc/ac/dc inductor-converter unit described in the previous section on the HV coil system. The adjustable dc voltage is smoothed by a low-pass filter and energizes polyphase inverters operating at relatively high frequencies, e.g., 10 kHz. For instance, four 10 kHz phase-displaced 3-phase bridge inverters could feed a summing transformer to generate a 3-phase output waveform having 24 steps. The lowest harmonic of consequence present in the output wave form of such a 24-phase inverter⁶ is the twenty-third. With a fundamental frequency of 10 kHz, this harmonic would be 230 kHz.

The filtered 10 kHz 3-phase voltage is applied through SCR ac switches to the primary of an HV rectifier transformer. The HV secondary transformer windings feed diode rectifier circuits that are connected in series to obtain a voltage with 24-phase ripple (≥ 240 kHz). Since 24-phase ripple causes a voltage fluctuation of only $\pm 0.43\%$, no dc filter is required. The HV output is connected to the injector through an STDT which has a time delay compatible with the inverter frequency (say, $\Delta t = 125$ ns).

When an arc fault occurs, the STDT initiates blocking of all ac switches. This will de-energize the rectifier transformers within approximately one-half cycle (≈ 50 ns). After an arc has been extinguished and a suitable time has elapsed to prevent arc restrikes, the ac switches will be re-energized to resume injector operation.

In case the arc fault persists for more than a cycle, due to a fault in the SCR ac switches, a crowbar time delay circuit (CTD) will energize the crowbar before the time delay of the STDT has elapsed. Energizing of the crowbar will initiate operation of mechanical circuit breakers to disconnect the summing transformers. An alternate location for the crowbar would be on the left hand side, as shown by dashed lines in Figure VI-12.

Core losses are the predominant factors in determining transformer size, because the core materials saturate at nearly the same induction whether

the frequency is 60 Hz or 10 kHz. At 10 kHz, the core loss is so high that the material can only be used at a fraction of its saturation density. However, this low induction is of advantage when switching ac voltage to the rectifier transformer primary in order to apply HV to the injector. When a transformer is first energized, a transient exciting current flows to bridge the gap between the conditions existing before the transformer is energized and the conditions dictated by steady-state requirements. For any given transformer, this transient current depends upon the magnitude of the supply voltage at the instant the transformer is energized, the residual flux in the core, and the impedance of the supply circuit. Under the worst conditions, with a residual flux of 0.9 times the normal flux and switching at zero voltage, the transient total flux would have a peak that is 2.9 times the steady-state peak flux. For grain-oriented steel, the B-H curve is approximately linear to 14 kG. Therefore, restricting the steady-state flux density to $\leq 14 \text{ kG} / 2.9 = 4.8 \text{ kG}$ will restrict transient transformer inrush currents to 2.9 times the steady-state magnetizing current, which is negligible. In practical applications, the 10 kHz losses will restrict flux densities to less than 4 kG; for an economical transformer design, core laminations of 0.01 cm should be used.

If advantage is taken of both the core material and high temperature insulation, 10 kHz transformers can be reduced to approximately 8% of the size of 60 Hz transformers of the same rating. With the transformer coils designed for minimum leakage inductances, the relatively small 10 kHz rectifier transformers have a fast response to a step change.

Figure VI-13 is a simplified equivalent of the proposed 3-phase switching circuit. The ac switches, S_1 , are turned on when HV is to be applied to the entrance grid of the neutral beam injector. The transformer leakage inductances are small compared to the inductance L_{σ}^{STDT} of the saturated STDT. For an STDT rated 180 kV, 120 A, we have $L_{\sigma}^{\text{STDT}} \leq 5 \text{ mH}$. The circuit capacitance to ground connected to the injector grid is estimated as $C_1 \leq 200 \text{ pF}$ ($\leq 50 \text{ pF}$ from the STDT). This results in a resonant frequency of $\sim 160 \text{ kHz}$, or a rise time of $T/4 \sim 1.6 \mu\text{s}$. The injector load of $1.6 \text{ k}\Omega$ damps the circuit.

Figure VI-13. 10 kHz 3-Phase Switching Circuit for Neutral Beam Injector Power Control.

In case of an arc in the neutral beam injector, the STDT will limit the current and will initiate blocking of the gate drives of switches S_1 . This removes the ac voltages from the rectifier transformers when the line currents go through zero. With an operating frequency of 16 kHz, the primary voltages and the energy stored in the transformer will be removed in approximately ± 50 μ s, with the last 25 μ s being a decaying quarter sine wave. The energy stored in the STDT discharges into its secondary bias circuit. The only energy flowing into the injector is the energy stored in C_1 and in the lead inductance. With $C_1 \pm 200$ pF at 180 kV, the energy in C_1 is 3.2 MJ.

The advantages of the proposed system are:

1. Commercially available components are used throughout, eliminating the need for developing switchtubes for ± 200 kV.
2. The injector is never exposed to overcurrent.
3. All switching and regulating is done at a lower voltage level.

A superconductive inductor-converter system rated ~ 23 MW could be provided for each of the 24 neutral beam injectors, as shown in Figure VI-12. If one inductor-converter would fail, only 4.2% of the total neutral beam energy would be lost. The central superconducting magnetic energy storage unit is described in detail in section VI-F.

E. RF Heating System Power Supply

The power supplies for the ion cyclotron resonance and lower hybrid resonance rf heating systems are also designed using the I-C concept described in section VI-C, operating with the energy storage inductor described in section VI-F. The loads presented by the rf systems are resistive and require a virtually constant voltage, to within $\pm 1\%$. The charging-recharging fluctuations that appear on the load shown in Figure VI-7 are unacceptable for the rf conversion devices. Therefore, the I-C design is based on a five-phase bridge. Voltage variations are maintained to within $\pm 1\%$ without a filter by selecting time constant between the capacitors and the resistive load to be at least 50 times the charging time of the capacitor.

The ICR heating system requires 18,000 V and 271 A. The effective resistance is 66.4 Ω per unit. Two ICR heating units are operated in parallel. Each capacitor in the bridge is 2220 μ F and is charged to

9000 V. The I-C is operated from the central energy storage inductor described in Section VI-F. SCR switching intervals vary from 250 μ s when the storage current is 80,000 A to 500 μ s when the energy storage inductor current drops to 40,000 A. The voltage variation goes from $\pm 0.17\%$ when the storage inductor is at 80,000 A to $\pm 0.34\%$ when the storage inductor is at 53,400 A. The SCR parameters are the same as those described in Table VI-2. Each SCR switch indicated in Figure VI-6 consists of 19 SCR units in parallel and 9 in series.

The LHR heating system requires 64.5 kV and 51 A per klystron. The effective resistance is 316 Ω for a unit consisting of four klystrons in parallel. Each capacitor in the bridge has a value of 615 μ f and is charged to 32,250 V. The SCR switching steps vary from 250 to 500 μ s as the central energy storage inductor current varies from 80,000 to 40,000 A. Each SCR switch arm consists of 10 SCR units in parallel and 33 units in series.

F. Energy Storage Inductor

In order to operate the poloidal coil and neutral beam systems it will be necessary to store energy on site so that the large power pulses required to initiate and terminate the tokamak discharge do not perturb the electrical power network. Superconductive energy storage offers an efficient and economic means to provide these pulsed energy requirements. The development of ac cryogenically stable cables, required for the equilibrium-field and ohmic-heating coils, also provides a solution for pulsed superconductive energy storage inductors. In fact, the conductor requirements for the energy storage inductor are considerably less severe than for the ohmic heating coil and are comparable in $B_{\max}$, $\dot{B}_{\max}$, current and voltage to those for the equilibrium field coils. These in turn are not far from present day technology except in their massive scale.

The size of the energy storage unit is determined by the energy balance in these systems. These are shown in Chapter III, Figure III-23 and Figure III-24 for the reference design case. The power supply design case, (Case 3 in Table III-11) however, is more demanding. The beams, in this case, are turned on at the end of the OH current reversal, causing a greater requirement $U_{OH} \approx 1100$ MJ for the homopolar generators than for the reference case. The EF coil ramps up in seven seconds, with most of the power coming after one second. The neutral beams come on at two seconds for

a period of 4.8 seconds. In this case, as in the reference case, the EF coil does not drive the resistance of the plasma strongly because of its slow turn on rate. Then essentially all (124 MJ) of the 130 MJ of $I_p^2 R_p$ energy is provided by the OH system. The poloidal coil power supply losses were previously estimated to be about 110 MJ, but for conservatism an energy loss of 5% of the energy transferred is assumed, corresponding to 370 MJ. Of this, slightly over half will be in the EF rectifier inverter, and half of this, or 100 MJ will have been lost by the end of beam heating.

In the neutral beam system it is necessary to deal with the problem of energy recovery, as it affects the design of these power systems. Electrical energy recovery can be accomplished either by a separate inverter system connected to the storage unit, or preferably, incorporated in the design of the neutral beam power supply. This latter choice will reduce the power supply requirements, as was done in the main demonstration of electrical energy recovery, the electron beam proton cooling system at Novosibirsk.⁸ So far as the energy storage unit is concerned, the power requirement is the net electric power taken for the beams, excluding thermally recovered electric energy, which comes on a different time scale. It is shown in Chapter V that the electrical efficiency of the beam system is 0.29; that is, for 60 MW of beam power to the plasma, 207 MW of power must be withdrawn from the energy storage unit, with a total energy withdrawal of 924 MJ. The OH rectifier, which provides volt-seconds to adjust the plasma current during burn, provides an average of about 10 MW for the 45 s period, or a total power supply output of 460 MJ. Shutdown is accomplished by putting reverse power (~ 66 MW) into the OH coil for about 5 s and then ringing back the OH coil through the homopolars to reverse the OH current. The EF coil power is reversed during this period to maintain plasma equilibrium, with most of the energy coming out in 6 s. The EF power supply must provide a maximum of 400 MW, but for energy balance, can be taken to be an average power of 250 MW for 6 s.

The power cycle and energy demand are shown in Figure VI-14. At the end of the burn cycle 1.50 GJ has been withdrawn, including power supply losses, leading to an average power of 21.4 MW. This will be put in at a constant rate, leading to a net zero balance at the end of the burn cycle. The peak net withdrawal of 2.4 GJ occurs at the end of the beam heating, which is also the time of peak power (400 MW EF, 207 MW Beams, 21.4 MW grid).

Figure VI-14. Power and Energy Balance in Pulsed Systems

The maximum current in the inductor is dictated by the nature of the EF rectifier system to be 80 kA, as was discussed in Section C of this chapter. If it is assumed that 75% of the energy is withdrawn from the inductor (1/4 stress level, 1/2 current level) then the maximum short-time averaged voltage on the inductor must be $600/40 = 15$ kV, which is comparable to the voltage required in the coil systems. In Figure VI-15 the total pulsed power system, with its interconnections, is displayed. The properties of the elements of the total system are listed in Table VI-3.

The maximum design field, B_m , of the coil is 5 T, as in the poloidal coil. The average current density is 10^7 A/m². Higher current densities lead to (1) higher stress, (2) larger aspect ratios, (3) larger dipole moments and stray fields and (4) larger volumes. A simple ring dipole has been selected since it has been well studied.⁷

The minor radius a is determined by the maximum field and average current density

$$a = \frac{2B_m}{\mu_0 J} = 0.8 \text{ m} \quad (\text{VI-16})$$

The major radius R is determined from the stored energy requirement

$$W_m = \frac{B_m^2}{2\mu_0} (2\pi a)^2 R \left[\ln \frac{8R}{a} - 1.75 \right] \quad (\text{VI-17})$$

to be $R = 5.57$ m. The average loop stress S_H is given by

$$S_H = 2 \frac{B_m^2}{2\mu_0} \times \left[\ln \frac{8R}{a} - 0.75 \right], \quad (\text{VI-18})$$

and $S_m = 6.5 \times 10^7$ N/m² = 9,500 psi. The average compressive stress is given by

$$S_c = 2 \times \frac{B_m^2}{2\mu_0} \left[\ln \frac{8R}{a} - 2.25 \right] \quad (\text{VI-19})$$

and $S_c = 3.54 \times 10^7$ N/m² = 5,130 psi. The inductance is

$$L = 2 W/I^2 = 1 \text{ H}, \quad (\text{VI-20})$$

the number of turns is

$$N = \frac{\pi a^2 J}{I} = 250, \quad (\text{VI-21})$$

and the total volume of the coil is

$$V = (2\pi a)^2 R = 69.7 \text{ m}^3. \quad (\text{VI-22})$$

Figure VI-15. Energy Storage and Transfer System

Table VI-3. Energy Transfer and Storage Systems — Maximum Ratings

Ohmic Heating Systems

Drum homopolar generators

No. of generators in series	16
No. of drums/generator	6
Total energy transfer (MJ)	1200
Peak power (MW)	1900
Peak voltage (kV)	51
Peak current (kA)	68
Equivalent capacitance (F)	0.897

Rectifier system

Type	Inductor-converter bridge
Energy transfer (MJ)	600
Peak power (MW)	66
Peak current (kA)	80
Peak voltage (kV)	0.8

Equilibrium Field System

Type	Inductor-converter bridge
Energy transfer (MJ)	1500
Peak power (MW)	416
Peak current (kA)	80
Peak voltage (kV)	21
Peak switching frequency (Hz)	1330

Neutral Beam System^{a,b} (60 MW)

Type	SCR, DC/AC/DC at 10 kHz
Energy transfer (GJ)	1
Voltage (kV)	180
Power (MW)	207

RF System (60 MW)^b

Type	5 phase inductor-converter bridge
Voltage (kV)	
ICR	18
LHR	64.5
Power (MW)	
ICR	94
LHR	125

Central Energy Storage Inductor

Type	Superconductive ring dipole inductor
Energy stored (GJ)	3.2
Energy transfer (GJ)	2.4
Peak current (kA)	80
Peak power (MW)	620
Average power from 60 Hz line (MW)	21

^a Assumes electrical energy recovery in power supply.

^b Neutral beam and rf are alternative options.

There are 7×10^8 amperes of cable, leading to 0.58 m^3 of NbTi. The coil will be constructed of pancakes separated by mica or fiberglass-epoxy boards 1.25 cm thick, with 50% coverage. Alternate pancakes are wound clockwise and counterclockwise to facilitate layer to layer connections. Conductor transposition is achieved by winding top and bottom halves with 40 kA conductor and operating the coil halves in parallel, although other schemes can work equally well. There are a total of 12 layers of coils, wound to approximate a circular coil cross section. Voids on the periphery are filled with blocks and the coil is banded to give the coil structural integrity. Special care must be taken in the design to accomplish the correct friction coefficient to achieve proper stress distribution. The cryostat is of the low-loss type discussed in Chapter IV.

The basic conductor unit includes the structural support, as shown in Figure IV-16. A fiberglass epoxy board 3 cm x 12 cm with a 0.5 cm x 10 cm slot and cooling holes comprises the support. The conductor is composed of 200 strands of 1 mm diameter wire wound around a 0.3 cm x 9.7 cm fiberglass epoxy board. This winding achieves transposition as is current practice in ac cables. Each 1 mm strand can be the same wire as for the poloidal coil cables without the last factor of two reduction in area (see Chapter IV).

Means must be provided for venting helium gas bubbles to the outside so that upper coil layers will not become vaporlocked by gas from the lower layers. Insulation presents a problem, particularly at the cross connection between layers. There, the voltage between layers is a maximum and presents a potential arcing problem. The actual current distribution, as compared to the idealized constant current density, causes the maximum field at the outside conductors to be 10% higher than the idealized design field. This is also true in the poloidal conductors, and the conductor design accounts for the higher field.

G. Research and Development Requirements

With the exception of the TF coil power supply system, the design of EPR power supplies makes use of technology that has not yet been demonstrated in a technical sense. The primary reasons for not utilizing present technology are cost and efficiency. The EF coil, OH coil, neutral beam and rf heating power supplies inevitably require large energy storage and transfer

Figure VI 16. 40 kA Energy Storage Conductor

devices because either the systems must charge and discharge superconducting coils that store gigajoules of energy or they present enormously large power demands for short periods of the cycle. Conventional devices such as capacitors or motor-generator flywheel sets are likely to be prohibitively expensive. Or, at least, if not prohibitively expensive, the cost of implementing present technology is likely to be sufficiently expensive to warrant the development of the less expensive alternatives suggested in the previous text and discussed below.

In the present EPR design, and most likely in future tokamak fusion power systems, the OH coil power supply is a passive device; that is, in the OH coil circuit the energy storage and transfer is accomplished by switching the coil current through a passive circuit element. A multidrum type homopolar generator forms the basis of the present design. However, the device has never been demonstrated to work reliably, although clearly some homopolar generators in some form or another have worked. The basic problem with more "conventional" type homopolar generators is the poor impedance match they present to the superconducting coils. As discussed in Chapter IV, many obstacles exist before the present EPR coil design of 80 kA can be reliably built. The problems with going to the higher currents and lower voltages, which would match "conventional" homopolar generators, are technically far more difficult and likely to increase the coil costs enormously, even if the technical problems are overcome. The homopolar generator presented in section VI-B offers a solution to the coil matching, which should save a considerable sum of EPR construction money, even if development costs are included.

A shorting switch capable of interrupting the full coil current also needs to be developed. The technical problems associated with the switch do not represent either as severe a technical extrapolation or as major a development cost as do the homopolar generators.

Other devices such as superconductive magnetic energy storage with mechanical energy transfer might also provide a cogent alternative to present technology. More study in this area is needed before a development program can be recommended.

The αF coil will require an actively controlled power supply. Superconductive magnetic energy storage with electronic energy transfer appears to offer the most attractive solution to this problem. The major problems which must be overcome before implementing this technology are associated with the development of pulsed superconductive energy storage units, selection of the cheapest and most reliable SCR bridge network, analysis of the dynamic characteristics, analysis of the bridge under fault conditions, and development of fault protection techniques. Undoubtedly, demonstration of the technical ability and reliability of a fairly large-sized model will be needed before the technology can be implemented with full confidence on something as costly as the EPR.

The design of the neutral beam power supply also applies technology which currently is not in use and has not been demonstrated. The voltage levels at which the EPR device must operate are in the regime where high power vacuum tube devices do not exist. Although a development program on switch tubes might lead to higher voltage devices, ultimately a high-power vacuum tube in the hundreds of kilovolt range becomes as vulnerable to spark damage as does the neutral beam accelerating column. The design suggested in section VI-D offers an entirely new approach which, if developed, can extrapolate to considerably higher voltage with minimum development effort.

The Saturated-Time-Delay-Transformer can perform the initial spark suppression, and the high frequency power supply can react quickly enough to prevent damage currents from flowing in the circuit. Development efforts are needed to design the least expensive, most flexible, and reliable power supply. And here, too, demonstration of the technique with a fairly representative-sized model is advisable.

-
1. R. L. Kustom, R. B. Wehrle and J. H. Norem, "The Counter Cyclonic Generator: Conceptual Design for a Rotating Capacitor System for the Tokamak Experimental Power Reactor," ANL/CTR/TM-49, Argonne National Laboratory (July 22, 1975).
 2. R. L. Kustom, J. H. Norem and R. B. Wehrle, "A Rotating Capacitor System for the Tokamak Experimental Power Reactor," Proc. of the Sixth IEEE Symposium on Engineering Problems of Fusion Research, San Diego, Ca., November 18-21, 1975, pp. 322-325.

3. R. A. Marshall, "Design of Brush Gear for High Current Pulses and High Rubbing Velocities," IEEE Transactions on Power Apparatus and Systems, PAS-85, November, 1966, pp. 1177-1188.
4. H. A. Peterson, N. Monam, W. C. Young and R. W. Boom, "Superconductive Inductor-Converter Units for Pulsed Power Loads," Proc. of International Conference on Energy Storage, Compression and Switching, Asti-Torino, Italy, November 5-7, 1974, Plenum Press, N.Y., 1976, pp. 309-317.
5. W. F. Praeg, "A Saturated Time-Delay Transformer for Overcurrent Protection of dc Accelerators," ANL/CTR/TM-59, Argonne National Laboratory (December, 1975).
6. C. W. Flairty, "A 50 kW Adjustable-Frequency 24-Phase Controlled Rectifier Inverter," IRE Transactions on Industrial Electronics (May, 1962).
7. F. E. Mills, et al., "Cryogenic Energy Storage System Design Report," FN-264, Fermi National Accelerator Laboratory (October, 1974).
8. G. I. Budker, "An Effective Method of Damping Particle Oscillations in Proton and Antiproton Storage Rings," Soviet Atomic Energy, 22, 438 (1967).

CHAPTER VII

VACUUM SYSTEMS

The toroidal vacuum system of the EPR provides the vacuum conditions necessary to initiate and maintain a thermonuclear plasma within the 711 m^3 volume of the torus. Pumping by the 32 cryosorption pumps, following a burn cycle, is sufficient to reduce the pressure within the torus to 1×10^{-5} Torr in less than 15 s. The cryosorption pumps will be regenerated every four hours to remove the deuterium and tritium that has been pumped and stored upon the liquid helium-cooled panels, for reasons of safety and to reduce tritium inventory. Two sets of cryosorption pumps are provided to allow for continued operation during the regenerating period.

Neutral beam vacuum system requirements are rather severe. Each of twelve beam injectors requires $\sim 5 \times 10^6 \text{ l/s}$ of pumping. One hundred square meters of cryosorption panel are required for each injector. To allow continued operation during regeneration, the pumping area will have to be twice this amount, and means must be provided for isolating that portion of cryosorption panel to be regenerated.

The high impedance characteristics of the waveguide used for lower hybrid mode rf heating will necessitate a separate pumping system to assure adequate reduction of pressure along its length. The pump, either a mercury diffusion or turbomolecular pump, must have an effective speed at $\sim 10,000 \text{ l/s}$ to maintain the desired pressure of $\sim 1 \times 10^{-6}$ Torr at the waveguide window.

A. Toroidal Vacuum System

The EPR toroidal vacuum system must: (1) evacuate the residual gas from the toroidal chamber after a burn pulse in a time that is short compared to the burn pulse, in order that a reasonable duty cycle can be achieved; and (2) achieve a base pressure less than 10^{-8} Torr at the beginning of an operational period.

The residual gas load after a burn is shown in Table VII-1. This gas load is based upon the reference case burn cycle described in Chapter III. It is assumed that the gas in the chamber at the end of the burn pulse is in thermal equilibrium with the first-wall, for the purposes of computing the chemical composition and the pressure of the residual gas.

Table VII-1. EPR Gas Load After Burn

Wall Temp. (°C)	1Ft E _{max}	Gas Load (Torr-sec)										
		D ₂ *	OD *	O ₂	BeD *	BeO	He	He	A	Be	D *	O
1000	10 T	3178.96	17.33	2.68	0.02	9.18	3.0 × 10 ⁻⁵	529.21	15.94	520.01	1.12	4.0 × 10 ⁻²
	8 T	2027.06	11.62	1.89	0.01	4.93	1.4 × 10 ⁻⁵	337.54	10.17	332.60	0.89	3.8 × 10 ⁻²
900	10 T	2932.84	9.62	1.60	0.018	16.55	2.9 × 10 ⁻⁵	487.64	14.69	471.07	0.18	4.0 × 10 ⁻⁴
	8 T	1870.11	6.82	1.26	9.0 × 10 ⁻⁴	9.40	1.2 × 10 ⁻⁵	311.02	9.37	301.61	0.14	4.2 × 10 ⁻⁴
800	10 T	2685.65	3.27	0.40	0.016	22.80	2.5 × 10 ⁻⁵	446.07	13.53	423.25	0.02	2.0 × 10 ⁻⁵
	8 T	1712.61	2.49	0.36	8.0 × 10 ⁻⁵	15.92	1.0 × 10 ⁻⁵	284.51	8.57	270.58	1.6 × 10 ⁻⁴	2.0 × 10 ⁻⁵
700	10 T	2436.54	0.62	0.036	0.014	23.68	2.0 × 10 ⁻⁵	404.50	12.18	380.81	1.5 × 10 ⁻²	3.0 × 10 ⁻⁷
	8 T	1553.89	0.49	3.5 × 10 ⁻⁵	7.0 × 10 ⁻⁵	14.98	8.8 × 10 ⁻⁶	257.99	7.77	243.01	1.2 × 10 ⁻⁴	2.6 × 10 ⁻⁷
600	10 T	2186.36	6.9 × 10 ⁻²	1.4 × 10 ⁻⁴	1.2 × 10 ⁻⁵	21.79	1.9 × 10 ⁻⁵	362.93	10.93	341.12	6.0 × 10 ⁻⁵	2.0 × 10 ⁻²
	8 T	1394.38	5.5 × 10 ⁻²	1.4 × 10 ⁻³	6.0 × 10 ⁻⁵	13.86	7.5 × 10 ⁻⁶	231.48	6.97	217.59	5.1 × 10 ⁻⁵	2.1 × 10 ⁻²
500	10 T	1935.95	4.3 × 10 ⁻³	2.2 × 10 ⁻⁵	1.0 × 10 ⁻⁵	19.36	1.6 × 10 ⁻⁵	321.35	9.68	301.99	1.2 × 10 ⁻⁵	3.0 × 10 ⁻³
	8 T	1234.68	3.5 × 10 ⁻³	2.2 × 10 ⁻⁵	5.0 × 10 ⁻⁵	12.34	6.4 × 10 ⁻⁶	204.96	6.17	192.61	9.5 × 10 ⁻⁶	3.0 × 10 ⁻³
400	10 T	1685.51	1.2 × 10 ⁻⁴	1.1 × 10 ⁻⁷	8.7 × 10 ⁻²	16.86	1.3 × 10 ⁻⁵	279.78	8.43	262.92	7.0 × 10 ⁻³	6.6 × 10 ⁻¹⁵
	8 T	1074.96	9.6 × 10 ⁻⁵	1.1 × 10 ⁻⁷	4.0 × 10 ⁻¹	10.75	5.3 × 10 ⁻⁶	178.45	5.38	167.68	5.6 × 10 ⁻³	6.6 × 10 ⁻¹⁶
300	10 T	1435.06	9.8 × 10 ⁻⁷	7.8 × 10 ⁻¹¹	7.1 × 10 ⁻³	14.35	1.1 × 10 ⁻⁵	238.21	7.18	223.85	7.2 × 10 ⁻¹⁴	7.7 × 10 ⁻¹¹
	8 T	915.23	7.8 × 10 ⁻⁷	7.8 × 10 ⁻¹¹	4.0 × 10 ⁻³	9.15	4.3 × 10 ⁻⁶	151.93	4.58	142.78	5.6 × 10 ⁻¹²	7.7 × 10 ⁻¹¹
200	10 T	1184.61	1.1 × 10 ⁻³	2.9 × 10 ⁻¹⁵	5.4 × 10 ⁻³	11.85	8.3 × 10 ⁻⁶	196.64	5.92	184.79	3.9 × 10 ⁻¹⁶	7.7 × 10 ⁻¹⁸
	8 T	755.50	8.7 × 10 ⁻¹⁰	2.9 × 10 ⁻¹⁵	3.0 × 10 ⁻³	7.55	3.4 × 10 ⁻⁶	125.42	3.78	117.86	3.2 × 10 ⁻¹⁶	7.7 × 10 ⁻¹⁸
100	10 T	934.17	3.3 × 10 ⁻¹⁶	4.8 × 10 ⁻¹⁴	3.8 × 10 ⁻³	9.34	6.2 × 10 ⁻⁶	155.06	4.67	145.72	1.2 × 10 ⁻¹²	---
	8 T	595.78	2.6 × 10 ⁻¹⁶	4.8 × 10 ⁻¹⁴	2.0 × 10 ⁻³	5.96	2.5 × 10 ⁻⁶	98.90	2.98	92.94	1.0 × 10 ⁻¹²	---
50	10 T	808.94	1.7 × 10 ⁻¹⁷	---	3.0 × 10 ⁻³	8.09	5.3 × 10 ⁻⁶	134.28	4.04	126.19	---	---
	8 T	515.92	1.3 × 10 ⁻¹⁷	---	1.5 × 10 ⁻³	5.15	2.2 × 10 ⁻⁶	85.64	2.58	80.48	---	---
20	10 T	738.81	5.2 × 10 ⁻²⁰	---	2.5 × 10 ⁻³	7.33	4.8 × 10 ⁻⁶	121.81	3.67	114.47	---	---
	8 T	468.00	4.2 × 10 ⁻²⁰	---	1.3 × 10 ⁻³	4.68	1.9 × 10 ⁻⁶	77.69	2.34	73.61	---	---

* Includes tritium and deuterium.

Reduction of the residual gas density by two orders of magnitude is the design requirement for the toroidal vacuum system. Because impurities (He, Be) are on the order of 10% in the residual gas, a 10^2 reduction in the residual gas density should suffice to limit the impurity concentration in the new gas charge to on the order of 0.1%, which is acceptable from a plasma contamination viewpoint. The design objective of a 75% duty cycle can be satisfied if the chamber evacuation takes place in 10-20 s.

The required pumping speed is determined from

$$S = \frac{2.303 V \log_{10} \frac{p_1}{p_2}}{\Delta t} \quad (\text{VII-1})$$

where V is the chamber volume, p_1 and p_2 are the initial and final pressures, and Δt is the pumpdown time.

The pressure p_2 is the pressure existing at the start of a new cycle and is assumed to be 1×10^{-5} Torr. Pressure p_1 , based on 10 T operation, 500°C first wall temperature, and a gas load of 2588 Torr-l, is $\sim 2.2 \times 10^{-3}$ Torr. The effective speed, S_{eff} , is a function of the conductance of the pumping ducts and the speed of the pumps, and is derived from the formula,

$$\frac{1}{S_{\text{eff}}} = \frac{1}{S_{\text{pump}}} + \frac{1}{C_{\text{duct}}} \quad (\text{VII-2})$$

Under the conditions given above, Δt is less than 15 s. The principal characteristics of the toroidal vacuum system for the EPR are given in Table VII-2.

The toroidal vacuum chamber is preconditioned to a base pressure less than 10^{-8} Torr at the beginning of each operational period. Preconditioning consists of decontamination of the first-wall surfaces either by a bakeout at temperatures at least 100°C above the operating temperature or by discharge cleaning.

The feasibility of using the toroidal vacuum system to remove the gas diffusing out of the plasma by continuous pumping during the burn cycle was examined. A representative gas load can be computed from the particle flux to the wall shown in Table VII-3.

Table VII-2. EPR Toroidal Vacuum System Parameters

Volume:		
w/o Neutral Beam Ducts		711 m ³
Inc. Neutral Beam Ducts		754 m ³
Surface Area:		
w/o Neutral Beam Ducts		592 m ²
Inc. Neutral Beam Ducts		771 m ²
Gas Load After Burn (wall temp. $\approx$ 500°C)		
At 10 T operation		2588 Torr-l
At 8 T operation		1651 Torr-l
Number of Pumping Ports		32
Diameter of Port (at first wall)		0.95 m
Diameter of Pumping Duct		1.1 m
Length of Duct		6.0 m
Number of Cryosorption Pumps (64 for on line regeneration)		32
Rated Pumping Speed (Hydrogen) of each Cryosorption Pump		25,000 l/s
Pumpdown Time After Burn Cycle		
At 10 T operation		$\sim$ 12 s
At 8 T operation		$\sim$ 11 s
Secondary Pumps:		
Number of 10,000 l/s aluminum zirconium getter pumps		32
Number of 1300 CFM blower stations		16
Number of 1400 l/s Turbomolecular Pumps		16

Table VII-3. Contribution to EPR Gas Load from First Wall
Particle Currents During Burn

<u>Species</u>	<u>Current (Particles/s-m²)</u>	
	<u>10 T Operation</u>	<u>8 T Operation</u>
Fast Alphas	5.3×10^{15}	3.5×10^{15}
Deuterium	1.6×10^{19}	6.8×10^{18}
Tritium	1.6×10^{19}	6.8×10^{18}
Slow Alphas	3.8×10^{17}	1.6×10^{17}

In terms of individual molecules, the ideal gas law can be written $p = NkT$ where N is the number of molecules per unit volume and k is the gas constant per molecule or the Boltzman constant. From this it can be shown that the number of molecules per cm^3 , N , is given by

$$N = 9.6 \times 10^{18} \frac{p_t}{T} \quad (\text{VII-3})$$

where p_t is the pressure in Torr and T is the absolute temperature = 773°K. By substitution (using 10 T operating values),

$$\frac{(5.3 \times 10^{15} + \frac{1.6 \times 10^{19}}{2} + \frac{1.6 \times 10^{19}}{2} + 3.8 \times 10^{17}) (A)}{V} = 9.6 \times 10^{18} \frac{p_t}{T}$$

where A = surface area of torus = 592 m^2

V = volume of torus and neutral beam ducts (754 m^3) minus volume of plasma (544 m^3) = 210 m^3 or $2.1 \times 10^6 \text{ cm}^3$

then

$$p_t = 3.72 \times 10^{-3} \text{ Torr, the pressure rise per second}$$

The gas load $p_t V = Q = 781 \text{ Torr-}\ell/\text{s}$. With thirty-two 1.1 meter diameter ducts and thirty-two 25,000 ℓ/s pumps, the base pressure p_B that can be maintained during a burn is,

$$p_b = \frac{Q}{(N)(S_{\text{eff}})} \quad (\text{VII-4})$$

where Q = Gas load = 781 Torr- ℓ/s

N = Number of pumps = 32

S_{eff} = Effective speed of each pump = 13,357 ℓ/s .

Then $p_B = 1.83 \times 10^{-3} \text{ Torr}$

Assuming no pumping by the plasma itself, the toroidal vacuum system can maintain a pressure of $1.83 \times 10^{-3} \text{ Torr}$ during the burn cycle. It is likely, however, that the plasma will pump D-T and He back into the plasma quite effectively. Since the cryosorption pumping ports represent only $\sim 4\%$ of the first wall area, the plasma would be the dominant pump, and the concern is that the D-T may collect in the outer edge region and not diffuse to the middle. This result would require additional refueling and lead to a pressure

buildup of relatively cool gas in the outer regions. This could have unfavorable consequences for a plasma burn which is longer than a particle confinement time ($\tau_p \sim 5-10$ s). It appears that the toroidal vacuum system would be ineffective in eliminating these potential unfavorable conditions. Because of its ineffectiveness during the burn cycle, it is intended to isolate the toroidal pumping system from the plasma region during the burn cycle.

Isolation of the toroidal pumping system from the plasma region will be done with plug valves (Figure VII-1). With this valve there are some added advantages, relative to a butterfly valve previously envisioned for this function.¹ Whether open or closed, it will act as a baffle against radiation, and since no moving parts exist at the plug end it should provide more reliable operation under the extreme conditions for which it must perform. Although the plug valve adds some impedance, the increase in diameter at the duct results in a pumpdown time, after the burn cycle, of ~ 12 s.

Some thought has been given to the possible advantage of pumping the $\sim 12\%$ helium gas load by means other than cryosorption. Figures VII-2 and VII-3 show the adsorption of hydrogen and helium on cryosorption panels at low temperatures and pressures. In the intended operating pressure range of the EPR, $1-5 \times 10^{-3}$ Torr to $1-5 \times 10^{-5}$ Torr, it appears that hydrogen can be pumped effectively even at temperatures of 20°K , while pumping helium at this temperature becomes impractical. In Table VII-4 it can be seen that the pumping speed of helium is barely measurable even at 17°K and does not increase very rapidly until below 10°K . The pumping speed is also very dependent on the temperature of the baffles shielding the cryosorption panel. Table VII-4 shows very little pumping at 10°K with a shielding temperature of 77°K . The shielding temperature for the adsorption curves shown on Figure VII-3, however, is 15°K and good adsorptive capacity is shown with the cryosorption panel even at 12°K . This difference in panel and shielding temperature requirements between the two gases suggests the possibility of pumping the helium with mercury diffusion or turbomolecular pumps and running the cryosorption panels at $\sim 15^\circ\text{K}$ with somewhat higher shielding temperatures, for pumping the hydrogen only.

Figure VII-1. Vacuum Port Configuration

Figure VII-2. Adsorption of hydrogen on Molecular Sieve 5A at low temperatures and pressures. Experimental points obtained at 20°K. Below 10⁻⁶ Torr experimental points are affected by residual pressure of the apparatus.²

Figure VII-3. Adsorption of helium on Molecular Sieve 5A at low temperatures and pressures. Experimental data obtained at 4.2°K. Below 10⁻⁶ Torr experimental points are affected by residual pressure of the apparatus.²

Table VII-4. Cryosorption Pumping of Helium as a Function of Temperature (on Molecular Sieve 5A Cryosorption Panels).³

Cryosorption Panel Temperature (°K)	Gas Temperature (°K)	Maximum Pumping Speed (liter/sec-ft ²)	Sticking Coefficient α
17.0	77	Barely measurable	--
13.6	77	55	3.7×10^{-3}
10.0	77	720	4.8×10^{-2}
4.2	4.2	27000	0.9

The decision as to which way to pump the helium is a matter of economics and availability of space. Table VII-5 shows the cryogenic requirements to cool down and maintain thirty two 25,000 ℓ/s (hydrogen) cryosorption pumps for pumping both hydrogen and helium. These figures are extrapolated from requirements for an existing pump with a speed of 12,000 ℓ/s for hydrogen. These figures do not include line losses, cool down after regeneration, and thermal loads from the neutron flux. Assuming an 8°K operating temperature and a 30°K regenerating temperature, then the quantity of heat, Q , required to cool the ~ 6.00 pound aluminum cryosorption panel having a constant specific heat, c , and causing the temperature to change from T_1 to T_2 is,⁴

$$Q = Mkc (T_2 - T_1) \quad (\text{VII-5})$$

Table VII-5. Cryogenic Requirements for Thirty-Two 25,000 ℓ/s (Hydrogen) Cryosorption Pumps

A. Liquid Nitrogen		
1)	Cool down to 80°K and Fill	3660 liters
2)	Usage Rate (after cool down)	36 liters/hr
B) Liquid Helium		
1)	Cool down from 80°K and fill	4990 liters
2)	Usage Rate	22 liters/hr

where $Q = B$
 $M = \text{pounds}$
 $T_2 - T_1 = ^\circ\text{C}$
 $k = 1.8$
 $c (\text{aluminum}) = 0.226$

then $Q = 53.7 B$

Since the heat of vaporization of helium is 10.26 Btu/lb and one pound of helium is equal to 3.631 l of liquid,⁵ 19 l of liquid helium would be required to cool the 6 lbs of aluminum cryosorption panel from 30°K to 8°K. Repeating the above exercise but assuming an operating temperature of 15°K for just pumping hydrogen, the usage rate for cool down after each regeneration reduces to 13 l of liquid helium. The difference of 6 l of liquid helium per pump or a total of 192 l represents the savings in liquid helium used during each regeneration if the 32 cryosorption pumps are used to pump hydrogen only. This amount appears insignificant when regeneration of the pumps is only due to saturation, which would occur after ~ 1100 burn cycles or about every 18.5 hours. Due to the desire to minimize tritium inventory and to the safety precautions regarding large volume storage of explosive gases, however, regeneration will be every 4 hours. With a four-hour regeneration cycle the differences in cryogenetic requirements becomes significant and would effect cryogenic storage and equipment costs.

Another aspect to consider in the decision on how to pump the helium is the partial pressure of the helium itself. At an 8°K operating temperature the effective pumping speed for helium is ~ 4500 l/s with the proposed cryosorption pump. With 32 pumps a pumpdown time of ~ 20 s would be required to reduce the partial pressure of the helium to 1×10^{-5} Torr after a burn cycle. The gas load in the torus at this point would be essentially all helium. If this partial pressure of helium is too high, the effective speed for helium can be raised to ~ 8000 l/s per pump by reducing the operating temperature of the cryosorption pump to 4.2°K. At the end of 12 s of pumping the partial pressure of helium will be ~ 7×10^{-6} Torr and the partial pressure of hydrogen will be ~ 3×10^{-6} Torr. Lowering the operating temperature will raise the total extra liquid helium required to cool down after regeneration from 192 l to about 300 l.

To realize an effective speed of 8000 l/s for helium, a mercury diffusion pump must have an unbaffled speed of at least 20,000 l/s. A pump of this speed will be about 76 cm in diameter and, by the time it is baffled and mounted, will have a height of about 3 m. If used, it would be mounted to a pipe connecting each pair of cryosorption pumps (one of which is pumping while the other is being regenerated). Valves on either side of the mercury diffusion pump would permit isolation from the cryosorption pump being regenerated.

Instead of the mercury diffusion pump, a large turbomolecular pump could be used. Large turbomolecular pumps with pumping speeds in excess of 10,000 l/s for both hydrogen and helium have been in use for at least 3 or 4 years and have a good history of reliability.⁶ The pumps have a maximum diameter of about 1 m and would require a vertical height of about 1.5 m mounted. The mounting of the large turbomolecular pumps would be identical to that prescribed for the mercury diffusion pumps above.

After consideration of all the above points, it was decided to pump both hydrogen and helium with the cryosorption pumps. The additional cryogenic requirement to pump helium becomes insignificant in comparison to the large quantities required for the superconducting magnets. The added space requirement for separate helium pumps, especially with the additional need for large regenerating pumps in the same area, also makes the dual pumping role for the cryosorption pump look more attractive.

Regeneration of the cryosorption pumps on the EPR requires special attention. After each burn cycle, each cryosorption pump will store ~ 81 Torr-l of gas. Assuming one burn cycle every minute, each pump will store ~ 19,440 Torr-l liters of gas in four hours of operation. To regenerate the pumps after four hours, it is intended to raise the temperature of the cryosorption panels to 30°K to release the hydrogen and helium and then cool the panels back down to operating temperature. Raising the temperature to 30°K must be a gradual process so that the regenerating pump can maintain a pressure of 1×10^{-3} Torr or less. If the pressure is permitted to go much higher than this, due to too rapid a release of gas, heat conduction will cause a sharp increase in cryogen required to maintain the proper temperature on the shielding baffles and to return to operating temperature.

To help simplify processing in the tritium recovery circuit, getter pumps, such as zirconium/aluminum, are planned for regenerating the cryosorption pumps. Upon activation of the zirconium/aluminum getter, the tritium and deuterium are released but other gases such as nitrogen and oxygen are held. When the getter elements become saturated with the retained species of gas, they must be replaced. Effective pumping is maintained with the elements at $\sim 300^{\circ}\text{C}$. Activation temperature is approximately 700 to 800°C . The $19,440$ Torr- ℓ released at each cryosorption pump will result in a volume of $\sim 30 \times 10^6$ ℓ of gas, at 1×10^{-3} Torr and 300°C , going to the zirconium/aluminum pump. Of this volume $\sim 4 \times 10^6$ ℓ is helium and is pumped by another pump on the opposite end of the zirconium/aluminum pump. With an effective speed of $10,000$ ℓ/s the zirconium/aluminum pump will take about 45 minutes to getter the remaining gas. Once isolated from the cryosorption pump, the hydrogen isotopes can be released for circulation through the fuel recovery circuit.

One zirconium/aluminum getter pump will serve each pair of cryosorption pumps. Mounting of the zirconium/aluminum pumps will be similar to that prescribed for the mercury diffusion pumps above. The physical size of these pumps will be approximately 1-m in diameter and about 1-m long. Each pump will contain about 3 m^2 of zirconium/aluminum getter surface and sufficient heaters to raise and control the temperature of these surfaces up to $\sim 800^{\circ}\text{C}$.

B. Neutral Beam Vacuum System

Pumping requirements for each neutral beam injector is divided between two chambers (see Figure V-1). Chamber 1 is on the inlet side of the neutralizer and Chamber 2 is on the exit side of the neutralizer.

The gas load in chamber 1 is calculated to be 90 Torr- ℓ/s . Assuming an average pressure of 3×10^{-5} Torr as adequate in Chamber 1, the required pumping speed will be $\sim 3 \times 10^6$ ℓ/s . This rate of pumping speed requires $\sim 60 \text{ m}^2$ of cryosorption panel. In Chamber 2, the gas-load will be 20 Torr- ℓ/s . With an average pressure of 1×10^{-5} Torr, the pumping speed required will be $\sim 2 \times 10^6$ ℓ/s . Approximately 40 m^2 of cryosorption panels are required to provide this rate of pumping speed. The gas loads given above are for Reference Design I (Table V-8).

Interference of the gas flow must be avoided, and this is accomplished by placing the panels within the neutral beam enclosure itself. Utilizing the vertical side walls of this enclosure appears most appropriate. Dividing the pumping surface equally between both sides of the enclosure will insure more uniform pumping.

In the practical application, the cryosorption panels will have to be shielded from contamination by condensable gases by means of an array of baffles refrigerated to temperatures between 4.2° and 20°K. The array has the function of cryopumping the condensable gases, and its actual temperature depends on the nature of these gases and the vacuum level desired. This array of baffles will have to be shielded, in turn, from thermal radiation and energetic particle bombardment by a second array of baffles refrigerated to $\sim 77^\circ\text{K}$. The above panel area requirements are based on the pumping speed of shielded panel. Estimated cryogen requirements are given in Table VII-7. These figures do not include line losses, cool down after regeneration, and thermal loads from the neutron flux.

Periodic regeneration of the cryosorption panels will be necessary. To avoid interruption of reactor operation at this time, cryosorption panel area can be doubled and the means provided for isolating half of the area for regeneration while the other half is pumping. One way of accomplishing this would be to put half the pumping panels on moveable walls and the other half on the neutral beam enclosure top and side walls. When the moveable walls are down, they isolate the enclosure walls, thus permitting them to be regenerated. When the moveable walls require regeneration, they are pulled upward into pockets where they are regenerated. Cryogen requirements in Table VII-7 will double if the panel area is doubled.

If warming up the huge cryosorption panels in the neutral beam injectors above 30°K during regeneration is to be avoided, large volumes of gas will have to be handled. Should regeneration be accomplished every four hours, as for the cryosorption pumps in the toroidal vacuum system, a gas load of $\sim 1.9 \times 10^6$ Torr-l of gas at 1×10^{-3} Torr or 1.9×10^9 l will have to be pumped from each injector. A pump with an effective speed of 250,000 l/s would be required to degass the cryosorption panels of each injector in two hours. This would leave two hours to cool the panel down

Table VII-6. Neutral Beam Vacuum System Parameters

No. of Injectors	12
Volume/Injector	250 m ³
Surface Area/Injector	254 m ²
Gas Load/Injector	110 Torr l/s
Cryosorption Pumping/Injector	100 m ² of panel
Effective Pumping Speed/Injector	5 x 10 ⁶ l/s
Secondary Pumps:	
Aluminum/Zirconium Getter Pumps	12-25,000 l/s
No. of 1300 CFM Blower Stations	Same 16 as on Torus
No. of 1400 l/s Turbo- molecular Pumps	Same 16 as on Torus

Table VII-7. Cryogenic Requirements for 12 Neutral Beam
Injectors

A. Liquid Nitrogen		
1)	Cool down to 80°K	274,500 l
2)	Usage Rate (after cool down)	2,700 l/hr
B. Liquid Helium		
1)	Cool down from 80°K	374,250 l
2)	Usage Rate	1,650 l/hr

to operating temperature. It probably would be more economical to remove the gas at 1×10^{-2} Torr. This would require one tenth the pumping capacity to remove the gas in the same period of time. The saving in space and pump costs would offset the cost of extra cryogen required due to conduction losses at 1×10^{-2} Torr. The regenerating pumps for the neutral beam injectors could be zirconium/aluminum getter pumps as used on the toroidal system. Once gettered, the gas can be released when convenient for processing in the recovery circuit.

C. Waveguide Vacuum System

Some form of supplementary heating power would be required to reach ignition in the EPR. One of the suggested means of supplying this supplementary heat is by radio frequency in the lower hybrid mode. Launching of the electromagnetic heating wave into the plasma is through the opening of a "Grill" waveguide structure.

The waveguide structure, Figure V-10, when viewed from the plasma region looks like a grill. Each opening in the grill structure, however, is the entrance to a duct, ~ 4.5 m long, between the plasma region and a ceramic window. The ceramic window isolates the plasma region from the klystron region. Sixteen such ducts form the "Grill" waveguide structure.

At the start of an EPR cycle, ionization will very quickly reduce the neutral atom density in the torus to a pressure equivalent of $\sim 2 \times 10^{-6}$ Torr. Although the pressure within the waveguide will also be reduced by this source, a separate pumping system is being considered for the waveguide to assure adequate reduction of pressure along its length.

A requirement of 1×10^{-6} Torr at the waveguide window and 2×10^{-6} Torr at the torus is assumed. Then

$$Q = 2C (p_2 - p_1) \quad (\text{VII-6})$$

where

Q = Gas load in Torr ℓ /s

C = Conductance per sector of waveguide = 63 ℓ /s

$p_2 = 2 \times 10^{-6}$ Torr

$p_1 = 1 \times 10^{-6}$ Torr

and Q_1 for one sector is 2.78×10^{-4} Torr ℓ/s . For the 16 sectors the gas load is 4.45×10^{-3} Torr ℓ/s .

To assure proper operation of the waveguide, pumping must be done through holes in the waveguide sectors no larger in diameter than 1/2 inch. Fifteen 1/2 inch holes, grouped together on each narrow side of every sector and close to the window, will assure adequate pumping conductance. A manifold will be built around the section of waveguide containing the 1/2 inch holes. The pump, either a mercury diffusion or tubomolecular pump, must have an effective speed of $\sim 10,000$ ℓ/s to provide the required pumping.

-
1. W. M. Stacey, Jr., "Tokamak Experimental Power Reactors Studies," Argonne National Laboratory Report ANL/CTR-75-2 (1975).
 2. P. J. Gareis and S. A. Stern, "Cryosorption Pumping of Helium and Hydrogen," Cryogenic Engineering News (October 1967), Systems Engineering and Research Laboratory, Linde Division, Cheswick, Pennsylvania.
 3. G. E. Grenier and S. A. Stern, "Cryosorption Pumping of Helium at 4.2°K," *J. of Vac. Sci and Tech.*, Vol. 3(6) (1966).
 4. Ralph G. Hudson, The Engineers Manual, John Wiley & Sons, Inc., United States, 2nd Edition, page 156.
 5. Industrial Gas Data, Air Reduction Company, Inc., ADE-890B, Printed in U.S.A.
 6. Information received from AIRCO Temescal, a division of Airco, Inc.

CHAPTER VIII

THE FIRST-WALL SYSTEM

A major problem relating to the achievement of the EPR objectives is the satisfactory design of the first-wall system. The primary function of the first-wall system is to provide a low pressure region of controlled impurity level and appropriate geometry in which satisfactory plasma burn can occur. For purposes of this discussion, the first-wall system includes the vacuum vessel that surrounds the plasma region and other associated components such as a coolant panel liner, a flux breaker, and the vacuum wall penetrations. In the present study a conceptual design for the first-wall system has been developed and subjected to sufficient analysis to indicate that it meets requirements imposed by plasma impurity control, materials integrity, fabrication and assembly, and thermal-hydraulic performance. Table VIII-1 contains a summary of design and operating parameters for the present EPR first-wall system.

The current first-wall reference design consists of a 16-segment self-supporting vacuum wall of stainless steel plate with external rib and spar support. A three-point suspension system is used to support each of the sixteen cylindrical segments that form the irregular toroidal wall. Detachable water-cooled panels shield the more permanent vacuum wall from the high heat fluxes deposited on the plasma-exposed surface. The operating conditions and allowable lifetimes of the vacuum wall are limited primarily by: (1) loss of ductility caused by displacement damage and helium generation at the high neutron fluxes; and (2) dimensional instabilities that result from radiation creep, radiation-induced swelling and thermal creep. Pressurized water is considered to be the most effective means of removing the large quantities of heat deposited in the coolant panels. In addition to extensive radiation damage, the coolant panel will be subjected to severe thermal fatigue produced by the cyclic heat deposition (equivalent to $\sim 20\%$ of the neutron wall loading) that results from the cyclic nature of the plasma burn. Excessive thermal cycling poses a major limitation to the expected lifetime of the coolant panel. A low-Z liner in the form of a thin plasma-sprayed beryllium coating on the plasma-exposed surface of the coolant panel is used for control of the high-Z impurities in the plasma.

Table VIII-1. First Wall Design and Operating Parameters

DESIGN DESCRIPTION

- Free-standing, stainless steel vacuum wall with rib and spar reinforcing
- Detachable, water-cooled stainless steel panels to shield vacuum wall from plasma
- Low-Z coating on plasma-exposed face of coolant panel for high-Z impurity control

DESIGN PARAMETERS

Vacuum Chamber

Material	316 SS
Design stress (ksi)	10
Major Radius (m)	6.25
Minor radius (m)	2.4
Volume (m ³)	711
Wall Area (m ²)	592
Wall Thickness (cm)	2

Ring and Spar

width (cm)	5
depth (cm)	11

Vacuum ports

number	32
diameter (m)	0.95
total port area (m ²)	22.7

Auxiliary Heating ports

number	16
total port area (m ²)	6.7

Experimental ports

number	4
diameter (m)	1.5

Current Breaker

material	Cr ₂ O ₃
form	coating
preparation	chemical bond

Coolant panel

Material	316 SS
Number	352
Area per panel (m ²)	1-2
Length (m)	1-2
Width (m)	~ 1
Total panel thickness (cm)	~ 1
Thickness front wall (cm)	0.5
Low-Z coating	
material	Be
thickness (μm)	100-200
preparation	plasma spray

PLASMA PERFORMANCE - NOMINAL

Capacity factor (%)	50
Operating Cycle	
Burn (s)	45
Exhaust and replenishment (s)	15
Average power loading during burn	
Neutron (MW/m ²)	0.5
Radiation, conduction, convection (MW/m ²)	0.1

Table VIII-1. First Wall Design and Operating Parameters (cont'd)

OPERATING PARAMETERS

Stainless steel vacuum wall

Maximum temperature (°C)	< 500
Minimum yield stress at 500°C (ksi)	17
Maximum annual fluence (n/m ²)	6 x 10 ²⁵
Neutron Damage (dpa/y)	2.8
Helium generation (appm/y)	54
Hydrogen generation (appm/y)	133

Stainless steel coolant panel

Maximum temperature (°C)	380
Minimum yield stress at 500°C (ksi)	17
Maximum annual fluence (n/m ²)	6 x 10 ²⁵
Neutron Damage (dpa/y)	2.8
Helium generation (appm/y)	54
Hydrogen generation (appm/y)	133
Maximum heat deposition (W/cm ²)	5.8
Maximum ΔT across panel surface (°C)	20
Maximum ΔT through panel face	
with Argon shutdown (°C)	100
without Argon shutdown (°C)	75
Maximum ΔT during burn cycle (°C)	100
Maximum thermal strain range	
operating cycle (%)	0.14
burn cycle (%)	0.09

Beryllium coating

Maximum surface temperature (°C)	407
Helium generation (appm/y)	780
Hydrogen generation (appm/y)	12
Maximum erosion rate (μm/y)	30

Water coolant

Maximum pressure (psi)	2000
Velocity (m/s)	1.6
Inlet temperature - first panel (°C)	40
Exit temperature - eighth panel (°C)	310
Pumping power (MW)	< 1

DESIGN LIMITS

Vacuum wall

Design life (y)	10
Integrated neutron wall loading (MW-y/m ²)	2.5
Yield strength - 10 y (ksi)	75
Uniform elongation - 10 y (%)	> 1
Radiation swelling - 10 y (%)	< 4
Limiting criterion	ductility

Coolant panel

Design life (y)	5
Total burn cycles - 5 y	10 ⁶
Fatigue lifetime (y)	5
Radiation lifetime (y)	8
Limiting criterion	thermal fatigue

Low-Z coating

Design life (y)	3-5
Limiting criterion	D-T sputtering

Physical sputtering of the beryllium coating will be the major source of plasma contamination since blistering can be minimized by proper selection of coating preparation.

An electric insulator in the form of a ceramic coating on two of the vacuum-wall flanges serves as a current breaker for the first wall. In addition to the insulator requirement, the flux breaker joints must also be vacuum tight.

The design description, materials considerations, thermal and stress analyses, and the research and development requirements for the first-wall EPR reference design are discussed in the following sections of this chapter. Additional background information pertaining to plasma impurity control, materials considerations, and structural analysis of the first wall is presented in Appendices B, E, and H, respectively.

A. Design Description of the EPR First-Wall System

The principal requirements of the EPR first-wall are: (1) to protect the plasma region from excessive atmospheric contamination, (2) to prevent excessive plasma contamination by products of plasma-wall interactions, and (3) to maintain its structural integrity for sufficient times under the severe radiation, thermal, and stress conditions imposed by an operating fusion reactor. The first two criteria are essential for the attainment of satisfactory plasma performance while the latter criterion is necessary to achieve acceptable reactor lifetimes. Based largely on the materials and stress considerations, the toroidal plasma chamber design consists of a structurally self-supporting, stainless steel vacuum vessel. The vacuum vessel is equipped with two electrically insulative rings that serve as breakers for the currents induced in this vacuum wall. A number of ports in the vacuum vessel are provided for evacuation, neutral beam injection, instrumentation, experimentation, and repair and maintenance. Replaceable water-cooled panels shield the more permanent vacuum wall from the high surface heat loads produced by plasma radiation and particle losses. These coolant panels are designed to withstand the high cyclic thermal stresses caused by the cyclic nature of the plasma burn. The low-Z coating concept is used for control of high-Z impurities in the plasma. As indicated in Appendix B, satisfactory plasma performance can be achieved with the beryllium coating. For the 0.5 MW/m^2

neutron wall loading (50% capacity factor), the 10-y design life of the vacuum wall at the $\sim 500^{\circ}\text{C}$ operating temperature is limited by radiation induced embrittlement of the stainless steel. Thermal fatigue limits the design life of the coolant panel to 5 y. Depending on the redeposition rate of the sputtered beryllium coating, refurbishment of the coating at more frequent intervals, i.e., 3-5 y, may be required. The design and operating parameters for the first-wall are summarized in Table VIII-1 and details of the component designs are discussed below.

1. Vacuum Vessel

The vacuum vessel is constructed from sixteen 4.8-m-diameter cylindrical segments that form $22\text{-}1/2^{\circ}$ increments of the 12.5-m-diameter irregular torus shown in Figure VIII-1. The vacuum wall is fabricated from 2-cm-thick Type 316 stainless steel plate that is reinforced with an external ring and spar framework. As illustrated in Figure VIII-1, two circumferential support rings on each of the sixteen segments are located adjacent to the vacuum ports, and the longitudinal spars are spaced at selected positions around the cylindrical segments so as to avoid the penetrations. Both the vacuum-wall plate and the stainless steel framework are joined by standardized welding procedures. The 5-cm-wide by 11-cm-thick rings and spars are designed to support their own weight, the vacuum wall, and the coolant panels when suspended at points 45° from the vertical diameter of each segment (see Section D for analysis). The reference design is a three-point per segment, roller/slide pad-type support from the blanket to the lower spars and rings. An alternate design utilizes a similar three-point per segment suspension from the overhead magnet support structure. In either case, the support must accommodate approximately 6 cm of expansion in the 350 ton toroidal vacuum vessel during heatup from ambient to operating temperature.

The sixteen segments that form the vacuum vessel are joined by formed rings, which house the coolant manifold piping as illustrated in Figures VIII-2 and VIII-3. Fourteen of the welded vacuum wall joints are of the type shown in Figure VIII-2. The other two manifold enclosures are of special design shown in Figure VIII-3 to accommodate the current breaker insulators. Highly reliable vacuum tight joints are required to achieve the desired plasma environment in the chamber.

Figure VIII-1. Vacuum Vessel Torus. a. Partial Plan View. b. Partial Elevation

DETAIL A
CROSSOVER MANFOLD SECTION

(TYP. 6 PLACES)

DETAIL B
INLET/OUTLET MANFOLD SECTION

(TYP. 8 PLACES)

Figure VIII-2. Coolant Manifold Arrangements

Figure VIII-3. Crossover Manifold Section with Current Breaker

Cooling of the vacuum vessel is accomplished by circulating pressurized water in a network of channels that line the plasma side of the vacuum wall as shown in Figure VIII-4. The channels are formed by roll bonding pre-formed stainless steel sheet to the vacuum wall by the Panelcoil* process. The coolant section consists of 0.1-cm-high by 0.75-cm-wide parallel channels that extend across the vacuum wall segments as shown in Figures VIII-4 and VIII-5. Each section covers from 1 to 2 m², depending on the position in the vessel. These coolant sections have their own header systems that supply high pressure coolant from the manifold piping. The header systems are designed to permit coolant from one vessel segment to flow through successive segments or to return headers as required. Sufficient coolant is provided to maintain the vacuum wall at temperatures below 500°C. Structural components, such as the reinforcement rings and spars and the manifold rings, are cooled by a separate smaller network of coolant channels.

Fifty-two major ports, which are provided in the vacuum wall to accommodate various equipment and apparatus, are described below. The positions of these ports in the first wall are indicated in Figure VIII-1.

(a) Vacuum Ports

Thirty-two vacuum ports are provided in the vacuum wall; one located near the top and bottom centerline of each segment (see Figure VIII-1). These ports are 0.95 m in diameter and also serve as auxiliary maintenance/inspection ports.

(b) Neutral Beam and rf Heating Ports

Sixteen ports, one per segment, are provided in the outer horizontal midplane of the first wall. These ports, which are elliptical in shape, will accommodate a pair of intersecting neutral beams up to 0.75 m in diameter. Twelve of the ports, located in groupings of three, are for the neutral beams. The other four, which are evenly spaced around the torus, are intended for rf heating, but can also accommodate additional neutral beams if required.

* Panelcoil is a tradename of Dean Products, Inc., Brooklyn, New York.

Figure VIII-4. Coolant Channel Arrangement for Coolant Panels and First Wall

Figure VIII-5. Photograph of Panel coil section manufactured by Dean Products, Inc., Brooklyn, N.Y.

(c) Experimental Ports

Four experimental ports, 1.5 m in diameter, are provided in the same segments as the rf heating ports. The experimental ports are located in the central upper region just above the rf ports. These ports will accommodate experiments and materials test samples as well as provide access to the inside of the vessel for inspection, repair, maintenance, and modification. These ports can also be used for diagnostic equipment if needed. Similar ports of smaller size, 0.5 m in diameter or less, will be provided as the detailed design warrants.

All vacuum-vessel ports will have adequate reinforcement at the nozzle penetrations and will be provided with cooling and plasma shielding as required. Final assembly of the vacuum-wall segments will be done on site. In general, three segment subassemblies will be made up and rigged for installation. Final assembly of the vacuum wall will conclude with the installation of a single segment. A set of special remotely operated tools will be developed to support the maintenance and repair of the internals of the vacuum wall. Further details relating to fabrication, assembly, maintenance and repair of the first wall are discussed in Chapter XII.

2. Coolant Panel

The entire inner surface of the vacuum vessel is shielded from the plasma by convection-cooled, stainless steel coolant panels. These detachable panels protect the more permanent vacuum wall from the high surface heat fluxes emanating from the plasma during normal operation and the potential plasma dumps that may occur. The plasma-exposed surface of the coolant panel is covered with a low-Z coating for purposes of high-Z impurity control in the plasma.

The coolant panel design given in Figure VIII-4 utilizes the Panelcoil concept shown in Figure VIII-5. Depending on the position in the torus, each of the 352 panels cover from 1 to 2 m² of the vacuum wall between the manifold rings of the vessel segments. Parallel channels, 0.1-cm-high by 0.75-cm-wide, are formed by roll bonding a 0.2-cm-thick corrugated sheet to the 0.5-cm-thick front plate. A similarly formed distribution main at both ends of each panel distributes and collects water to the individual coolant channels. The mains are supplied from 1-1/2 inch pipes located in the formed manifold rings that connect each of the sixteen vacuum vessel segments (see Figures VIII-2 and

VIII-3). The coolant manifolds are joined between vessel segments as indicated in Figures VIII-4 and VIII-6. Eight panel sections are connected in series around the torus in order to obtain the desired temperature rise and yet minimize differential thermal stresses in the wall. The 1-1/2 inch stainless steel supply and return manifolds are located in a circular array and fitted with bellows expansion joints as needed to allow for thermal expansion and alignment variations. Each of these manifolds has four or more supply nozzles that penetrate the vacuum-vessel manifold ring through a bellows seal and exit the reactor blanket and shield in specially designed access ports.

The panels are slide-mounted to brackets welded to the vessel in the coolant manifold ring area and are accessible for attachment along the manifold edges. A water-cooled filler section covers the area between coolant panels. These strip sections have coolant channels running in the poloidal direction and are capable of handling very high heat loads. They are the last components to be installed and the first to be removed for inspection or repair.

The coolant panels are designed for 2000 psi coolant at a maximum temperature of 310°C. Under normal operation, i.e., 0.5 MW/m² and 50% capacity factor, the panel life expectancy is limited to five years by thermal fatigue considerations.

3. Current Breaker

The current breaker is shown in Figure VIII-3. It consists of four major components; an outer ring, two manifold ring segments, and an internal compression ring seal unit. The breaker joint is formed by an insulative coating that is chemically bonded to the surface of adjoining components. The outer ring provides the structural support through the two ring segments. The coating and serrated surfaces form a labyrinth seal to the vacuum in the inner vessel. A U-shaped seal ring gasket made of stainless steel and coated with insulative material is clamped into position and forms an inner insulative seal. The volume between seals is pumped by a separate vacuum system that both monitors the seals for leaks and compensates for small to modest leakage into the system. Two such current breaker units are used in the vessel wall. The concept described is repairable from inside the vacuum vessel;

DEVELOPED VIEW C-C
 (22 $\frac{1}{2}$ ° SEGMENT OF TORUS VIEWING INSIDE WALL)
 SCALE: 1" = 1 METER

Figure VIII-6. Vacuum Wall Segment
 (For angular reference see Figure
 VIII-1b)

however, its reliability is highly dependent on the insulator material performance and bonding/fabrication technique. Alternative designs and the absolute resistance values required are subjects for further investigations.

Since the coolant panel liners are metallic and in contact with the vessel wall, the interconnecting piping contains electrically insulative joints to preserve the current breaking integrity of the first-wall system. The various insulative pipe connections are shown in conceptual form in Figure VIII-7. The reliability of these components depends heavily on the material radiation resistance and the chemical bonding/fabrication process. Alternate high resistance pipe connections are being investigated.

4. Limiter

Provisions have been made for placement and cooling of a limiter; however, no distinct limiter design has been established. The areas provided are the inserts located circumferentially between the coolant panel liners. These paneled areas extend closest to the plasma on the inner portion of the torus. They are readily accessible for replacement, and a water coolant capability sufficient to remove up to 10 MW/m^2 can be provided.

B. First-Wall Materials

The proper selection of materials for the first-wall system will have a major influence on the attainment of the design objectives of the EPR. As stated previously, the primary objectives of the EPR are the production of suitable amounts of thermonuclear power and the conversion of this power to sensible heat, which may then be used to produce electricity. The elevated temperatures implicit in these objectives pose a number of serious materials problems that have not been encountered in previous fusion devices. Also, the absence of a tritium-breeding blanket alters the priorities in materials selection for EPR relative to a demonstration or commercial-scale reactor. The strong incentive to utilize available materials technology in order to meet the near-term design objectives places further restraints on the materials selection. In some cases it will be necessary to utilize materials in EPR that are probably not optimal for commercial-power reactor applications, since time and economic restrictions prohibit the development of an advanced materials technology for the EPR.

Figure VIII-7. Typical Manifold Section Piping

The materials assessment for the first-wall system is discussed according to the application, viz., structural, liner, insulator and coolant, as described in Section A. Structural materials are used for the vacuum wall, coolant panels and coolant headers. The low-Z liner materials, which shield the plasma from the high-Z structural materials, are needed primarily for impurity control. Electrical insulators are required for current breakers, rf coils and diagnostics, and the primary purpose of the coolant is to maintain the first-wall components at acceptable operating temperatures. The materials selected for the first-wall reference design and alternate materials are listed in Table VIII-2. The requirements, proposed materials and lifetime limitations relating to the various applications in the proposed first-wall design are discussed in the following sections, and the design and operating parameters are summarized in Table VIII-1. Available property data for candidate materials are reviewed in more detail in Appendices B and E.

Table VIII-2. First-Wall Materials Selection

Application	Reference Design	Alternate Materials
Structure	Type 316 Stainless Steel	Low-swelling Stainless Steel
Low-Z Coating	Beryllium	B ₄ C, BeO, BN, B, Be ₂ C, SiC
Current Breaker	Cr ₂ O ₃	Y ₂ O ₃ , MgO, Al ₂ O ₃
Coolant	Pressurized Water	Helium

1. Structural Materials

The structural materials in the EPR first wall must maintain their mechanical integrity and dimensional stability for extended periods under the severe radiation, thermal, chemical and stress conditions imposed by a fusion reactor environment. The important property requirements for the structural

material and a qualitative evaluation of candidate materials were given in Appendix C of reference 1. Primarily on the basis of established fabrication technology and available materials property data, austenitic stainless steel was selected as the prime candidate for the first-wall structural material. Reliable fabrication procedures have been developed for stainless steel and a substantial data base on the behavior of Types 304 and 316 stainless steel under neutron radiation at elevated temperatures has been obtained from fission reactor programs. Although stainless steel may not be the optimum structural material for demonstration- or commercial-type fusion reactors,¹ it is considered to be the best choice for the limited objectives of a near-term EPR. The relevant properties of Types 304 and 316 stainless steel have been reviewed² and are summarized in Appendix E. Type 316 stainless steel is preferred for the present application because of slightly better ductility, lower swelling and higher strength. Although it is known that cold-working improves the swelling resistance of Type 316 stainless steel, annealed material was selected because of its greater ductility, which is more critical than swelling for EPR conditions.

Loss of ductility of the stainless steel as a result of radiation damage and helium generation is considered to be the primary factor that establishes the operating limitations and design life of the vacuum wall. For a ten-year life at a 0.5 MW/m^2 neutron wall loading and 50% capacity factor, the vacuum wall will receive a neutron fluence of $6 \times 10^{26} \text{ n/m}^2$ that will produce an accumulated radiation damage of 28 dpa and ~ 540 appm helium. At this radiation damage level, a sharp decrease in the ductility of stainless steel occurs under fission reactor irradiation, particularly at elevated temperatures.³ This loss in ductility is more pronounced at temperatures above 500°C ; decreasing to less than 0.1% uniform elongation after irradiation at 650°C . Therefore, the maximum operating temperature for the first wall has been set at 500°C . The estimated uniform elongation for annealed Type 316 stainless steel at end of life (10 years) as a result of displacement damage at 500°C is $\sim 2\%$ (see Figure E-1). However, further reductions in ductility are expected because of the helium (~ 540 appm) produced in the stainless steel by the high energy neutrons.⁴ Although no data exist on the combined effects of displacement damage and helium production for the EPR conditions, available data obtained under a variety of conditions suggest that a uniform elongation of $\sim 1\%$ will be retained

in annealed Type 316 stainless steel at operating temperatures $< 500^{\circ}\text{C}$. The ductility requirements are obviously dependent on the component design and the stress levels; however, a 1% uniform elongation is probably adequate for the vacuum wall. The hydrogen generated in the steel is not expected to contribute significantly to the embrittlement, since it should diffuse out at the proposed operating temperatures.⁵

The tensile and creep properties of stainless steel are adequate for operating temperature $\leq 500^{\circ}\text{C}$. As indicated by the tensile and creep data in Appendix E and reference 2, the allowable stresses decrease substantially with increases in temperature above $\sim 500^{\circ}\text{C}$. The estimated 1% radiation creep for EPR conditions greatly exceeds the 500°C -10 ksi thermal creep contribution. However, thermal creep would become important at operating temperatures much above 500°C . The predicted void swelling of $\sim 4\%$ in the vacuum wall at end of life (10 years) is considered acceptable.

In addition to radiation effects similar to those sustained by the vacuum wall, the coolant panel is also subjected to extensive thermal fatigue because of the cyclic nature of the high heat deposition on the panel surface. Figure VIII-8 shows available 600°C fatigue data for Type 316 stainless steel in air and the design curve (ASME code case 1331-8) for temperatures of 538 to 650°C .⁶⁻⁸ A factor of two reduction in fatigue life for design allowables such as radiation damage, helium content, mean stress, surface coating and environmental effects is considered reasonable. The fatigue lifetimes for the coolant panels are discussed in Section D for various operating conditions.

Other areas in which a degree of uncertainty exists with respect to first wall performance include effects on weldments, coolant compatibility, and magnetic field considerations. Extensive weldments, which are required for the first-wall fabrication, will be exposed to the same severe radiation, thermal and stress conditions as the base metal. Only limited data are available upon which to assess the behavior and reliability of the large amount of weldmetal and heat affected zones in the first wall. Under certain conditions, the austenitic stainless steels are susceptible to intergranular stress corrosion cracking.⁹ This problem may be particularly critical because of limitations on coolant chemistry control imposed by the tritium

Figure VIII-8. Elevated temperature fatigue life of austenitic stainless steel

handling requirements. The interactions of the magnetic field with the first-wall structural material have not been analyzed in detail. The major concern in this area relates to the presence of ferrite in welds (typically $\sim 10\%$) and additional ferrite formation in the base metal enhanced by the radiation environment and elevated temperature aging processes.^{10,11}

Although considerable extrapolation and interpolation of available data are required to attain an assessment of the structural materials behavior in a fusion reactor environment, it appears that with proper design, annealed Type 316 stainless steel is adequate for the EPR first-wall design objectives.

2. Liner Materials

(a) Low-Z Coating Concept

Results of plasma physics calculations (see Appendix B) indicate that control of high-Z impurities in the plasma is extremely critical. Of the various impurity control concepts, viz., a divertor, a low-Z coating on the first wall, a monolithic low-Z stand-off curtain or liner, and a low-sputtering liner, a low-Z coating on the stainless steel coolant panel provides the most attractive near-term solution to excessive contamination of the plasma by high-Z elements that result from plasma-wall interactions. The low-Z coating concept circumvents several disadvantages of the separated liners. Since the stainless steel substrate provides the structural support, the coating material can be selected primarily on the basis of desirable surface properties. As a result, a wider variety of low-Z liner materials are feasible. The technology exists for a variety of coating techniques that can be used to tailor the surface microstructure for purposes of minimizing the extent of blistering and the severity of radiation damage effects. Since only thin coatings are necessary, the cost effectiveness of the more exotic low-Z materials is substantially better for the coating concept than for thicker monolithic liners. Microcracking will not substantially degrade the performance of the coating; however, excessive flaking and exposure of the substrate are not tolerable. The failure mode of the coating is expected to be less critical than that for the separated liners and the feasibility of in situ repair of the coating may be advantageous. In addition to more favorable outgassing and vacuum requirements, the coating may also serve as an effective tritium permeation barrier for the first wall.

(b) Low-Z Coating Material

Several low-Z materials are regarded as feasible for the first-wall coating. Primarily on the basis of effective "Z", desirable physical properties, and available coating technology, beryllium is presently considered to be the best choice for near-term applications and has been selected for the EPR reference design. The compound B_4C is considered to be the primary backup coating material; however, several other candidate materials, including BeO , BN , Be_2C , B , SiC , and C , are still being considered. The properties of the candidate coating materials are summarized in Appendix E.

Beryllium has the lowest atomic number of the feasible wall materials and, as indicated in Appendix B, would have the minimal effect on plasma performance for a constant impurity fraction. The high thermal conductivity and heat capacity of beryllium are desirable for minimizing temperature and temperature gradients in the coating. Although beryllium has a fairly high coefficient of thermal expansion, it matches well with that of stainless steel and makes beryllium particularly suited for this substrate material. The relatively low melting temperature of $1277^\circ C$ and the relatively high vapor pressure at the higher temperatures limit its usefulness to temperatures below $\sim 800^\circ C$; however, this temperature is more than adequate for EPR applications. High helium production rates, which are common to all the low-Z materials, require special attention. As discussed in the following section, the small grain size and substantial porosity that facilitate helium release can be obtained by selected coating procedures. Although beryllium is a neutron multiplier, there is no significant effect on the neutronics because of the small quantity of material in the thin coating. Activation products and decay heat from beryllium are unimportant; however, slight activation will occur from impurities such as iron that are present at the part-per-million level. The major impurity in beryllium is oxygen in the form of beryllium oxide. Since BeO is very stable, oxygen contamination during preparation of the coating and operation of the first wall at elevated temperatures must be considered. Since the actual beryllium coating will probably contain significant concentrations of oxide on the surface, the consequences of this, which may be beneficial, should be considered in the analysis. For this reason, a graded beryllium-beryllium oxide coating may prove to be an attractive liner. The substantial

technology base that has been developed on beryllium coating for the space programs is an important consideration for near-term applications.

Boron carbide possesses several properties that make it suitable for a first-wall coating. Because of its stoichiometry, the effective Z of B_4C is not much different than that of boron. The more refractory properties of this material would permit higher temperature operation than for beryllium. The extensive data base developed in the fission reactor program on boron carbide in a nuclear environment is of value. A major concern in the use of B_4C is the high helium production rate of over 10^4 appm/year. Since the major portion of helium is produced by the (n, α) reaction of ^{10}B , the problem can be substantially reduced by use of boron that is depleted of ^{10}B . The technology base for this enrichment has been developed for fission reactor programs.¹² Since the quantity of material required for the thin coating is relatively small, this enrichment appears to be a desirable solution.

(c) Fabrication Methods

Plasma spraying appears to be the most suitable method for applying the low- Z coating to the first wall. Deposition rates obtainable by this method are appropriate for the desired coating thicknesses,^{13,14} viz., of the order of a hundred micrometers. The powder size and grade can easily be adjusted to obtain variations in grain size and porosity that will facilitate helium release and minimize blistering. The elongated grain structure typically obtained by plasma spray processes is well suited for accommodating thermal expansion of the substrate. This process can also be used to prepare graded coatings, which can be utilized, if necessary, to improve the adherency of certain coating materials. A substantial technology base for the coating of beryllium by the plasma spray process was developed for space applications.^{14,15} This is particularly important for near-term applications. Some development would probably be required for in situ coating preparation if this approach is recommended. The detonation-gun process, which is a modification of the plasma spray process, can be used to obtain more adherent coatings of certain materials.¹⁴ Various types of chemical vapor deposition processes are more suitable for application of some materials, e.g., silicon carbide.¹³ An excellent review of various coating processes and the state of coating technology is given in reference 13.

(d) Surface Effects

The surface effects related to impurity control in the plasma are discussed in detail in Appendix B. Therefore, the emphasis here will be placed on surface effects as they relate to materials problems and the integrity of the first wall. Physical sputtering of the first wall by high-energy plasma particles, viz., n, D, T, He and impurity ions, will cause erosion of the coating. Results given in Appendix B indicate that the major contributions to the physical sputtering are from deuterium and tritium for the EPR conditions. These calculations are based on energy dependent physical sputtering curves of the type shown in Figure VIII-9 for deuterium incident on beryllium and iron for both monoenergetic and Maxwellian energy distributions. The sputter yields of the first wall are strongly energy dependent and the maximum erosion rates vary with both incident particle and wall material. For light ions with energies of a few hundred electron volts, the erosion rates of the low-Z materials are typically higher than those of the higher-Z elements (see Figures B-2 through B-5). However, because the radiation losses are strongly dependent on atomic number, viz., a power function, the low-Z materials are preferable on the basis of plasma contamination. The advantages of the low-Z materials become even more prominent as the incident particle energies increase. Maximum predicted erosion rates from physical sputtering of the beryllium coating are about 30 μm per year, which would result in a maximum coating lifetime of three to five years. However, high sticking probabilities of sputtered self-ions that subsequently impinge on the wall will tend to reduce the net erosion rate. Evidence also indicates that a significant portion of material sputtered from fusion reactor walls will redeposit on the wall after the burn cycle.¹⁶ If this effect is substantial, much longer coating lifetimes would be allowable. Also, thicker coatings might be used to extend coating lifetime.

Erosion rates of the coating by blistering or exfoliation can be maintained at acceptable levels by judicious selection of microstructures and porosity of the coating material. Exfoliation of sintered material by 100 keV helium, for both beryllium and aluminum, is several orders of magnitude lower than that for the respective vacuum cast materials.¹⁷ On the basis of this type of data, blistering erosion rates of < 2 μm per year

Figure VIII- 9. Energy dependent physical sputtering yields of iron and beryllium irradiated with D^+ ions that have monoenergetic and Maxwellian energy distributions.

are expected for plasma sprayed beryllium with a microstructure and porosity similar to sintered material. Chemical sputtering of beryllium should not be significant since beryllium does not readily react with hydrogen at low partial pressures. However, reaction of beryllium with oxygen impurity will occur at elevated wall temperatures. The effects of some beryllium oxide on the surface must be evaluated in terms of other surface phenomena.

(e) Vacuum Properties

Vacuum properties of the coated first-wall concept should be very good, since the amount of coating material is small and additional surfaces, such as in the honeycomb and separated liner concepts, are not required. Helium generated in the coating and subsequently released to the chamber will not contribute significantly to the gas load. Coating of vacuum and beam ports, which undoubtedly will be required to limit plasma contamination, should not affect the pumping requirements. Vacuum requirements for the coated first-wall concept should be much less than those for the separated monolithic- or cloth-liner concepts.

(f) Failure Analysis

A major consideration in the selection of the coating concept over the other low-Z liner concepts relates to the potential failure modes and the resultant effects on reactor operation. If the coating can be made to resist extensive flaking, satisfactory plasma performance appears feasible. As discussed above, erosion rates from ion bombardment are acceptable for reasonable lifetimes. Microstructures with small grain size and adequate porosity should accommodate sufficient swelling and provide satisfactory gas release to resist excessive bulk radiation damage for acceptable lifetimes. Chemical interaction of beryllium with either the plasma or the substrate at the projected temperatures ($\leq 500^{\circ}\text{C}$) should not be excessive. The primary reason for the thin ($\sim 100\text{ }\mu\text{m}$) coating is to minimize stress effects on the substrate. Since the thermal expansion coefficient of beryllium matches that of stainless steel fairly well, differential expansion effects are minimized. Some microcracking of the coating is anticipated during heat up since the expansion coefficient of stainless steel is slightly greater than that of beryllium. However, the microcracking should not expose significant areas of the high-Z substrate to the plasma. The greater

thermal expansion of the steel may be advantageous since the coating will normally operate at a higher temperature than the substrate. Also, enhanced ductility of the platelet-type grain structure of plasma-sprayed material in the direction parallel to the surface will be beneficial. Diffusion of the high-Z structural material elements through the coating is not expected to be significant at the proposed operating temperatures. The potential failure modes of the other liner concepts, which conceivably can cause accelerated degradation, are discussed in Appendix B.

3. Insulator Materials

Electrical insulators are required in the first-wall design for current breakers, and may be needed for the rf coils as well as for plasma diagnostic and control instrumentation. Only low voltages (< 100 V per breaker) are expected for the current breaker applications, while high voltages (> 1000 V) insulators are required for the rf coils. Both low- and high-voltage insulators will probably be required for plasma diagnostic and control applications. Surface radiation effects are not critical for the current breaker since it can be shielded from the plasma. Although the rf coils will be directly exposed to the plasma, it may be possible to shield the insulators. In the following discussion emphasis is placed on the assessment of the current breaker materials since the operating requirements and the relevant property data for the other applications are less well established.

a. Current Breakers

The first-wall design utilizes current breakers in the form of insulator coatings on the vacuum wall joints (see Section A and Figure VIII-3). Because of geometrical considerations the coating concept appears to be the most suitable means of satisfying both the physical and materials requirements for the current breakers. Fabrication and installation of thin insulator rings or gaskets of the size necessary to fit the 5-m-diameter wall was not considered feasible. Since the current breaker is an integral part of the vacuum wall, it must also form a reliable, leak-tight vacuum seal. Ceramic-type insulators appear to be the most suitable for the present application since these materials have good electrical and high temperature properties. The properties of selected ceramic insulator materials are summarized in Appendix E. On the basis of the limited available property data, Cr_2O_3 ,

Y_2O_3 , Al_2O_3 , and MgO are presently considered to be the prime candidates for the current breaker. The most suitable method of coating has not been established; however, the chemical bonded coatings of the K-RAMIC-type* possess a number of favorable characteristics.¹⁸ The coating material is easily applied by painting or dipping and bonded at relatively low temperatures ($\sim 500^\circ C$). Coatings of type and thickness suitable for the present application have been prepared by this technique on stainless steel substrates with complex geometries. Compositional variations of oxide coatings have been developed to match the thermal expansion of the substrate to improve the crazing and flaking resistance. Highly thermal shock resistant coatings have been prepared by this method and smooth surfaces, which may be required for vacuum purposes, are obtainable.

The major limitations to the design lifetime of the current breakers are caused by the severe radiation and thermal environments. Most ceramic insulators are susceptible to dimensional instabilities and loss of mechanical integrity as a result of displacement damage, swelling, and gas production.¹³ The variation of properties with irradiation temperature and damage level has not been systematically evaluated for most insulator materials. Since the porosity, grain size, purity, surface condition, and fabrication method all have a substantial influence on the properties of ceramics, correlation of available data is difficult. However, Al_2O_3 , which has been the most extensively investigated, shows severe degradation at neutron fluences corresponding to one year of EPR operation, i.e., $\sim 6 \times 10^{25} \text{ n/m}^2$.¹³ Loss of integrity of the insulator material caused by thermal effects such as thermal fatigue, thermal gradients, and differential thermal expansion with respect to the substrate is also critical. These effects are particularly important for the ceramic materials because of their inherent brittleness. Since only low voltages are encountered in the present application, loss of mechanical and vacuum integrity is probably more crucial than breakdown of dielectrical properties. Both detailed thermomechanical analysis and experimental data are required in the near-term to reliably assess the operating limitations of the current breaker materials in the first-wall thermal and radiation environment.

* Trade name of product of Kaman Sciences Corporation, Colorado Springs, Colorado.

b RF Coil and High-Voltage Insulators

The necessity of rf coils for auxiliary heating has not definitely been established; however, high-voltage insulators will probably be required either for this or diagnostic applications. In either case, the requirements are not well defined and there is a serious lack of both surface and bulk radiation data. Therefore, selection of specific materials is not appropriate at the present time. In contrast to the current breaker application, breakdown of the dielectric properties will probably be a limiting factor, particularly for the high-voltage applications. If the insulators are exposed to the plasma radiation environment, surface radiation effects and effective atomic number will be important considerations with respect to plasma contamination. The cyclic thermal stresses and intense radiation environment will severely restrict the operating lifetime of most insulator materials. Extensive research effort will be required to develop materials that will satisfy the rf coil insulator requirements. Problems similar to those described for the current breakers and rf coils will also be encountered in the insulators for diagnostic and control applications.

4. Coolant

Based on the survey presented in Appendix C of reference 1, the selection of a coolant for the EPR initially reduced to a choice between pressurized water ($\sim 310^\circ\text{C}$ maximum and 2000 psi maximum) and pressurized helium ($\sim 530^\circ\text{C}$ maximum and 750 psi maximum). As a result of further assessment conducted early in the FY 1976 design studies, water was judged to be preferable to helium for near-term application in an experimental fusion device like the EPR. The basis for this judgement stems from an examination of the constraining features of the cooling systems, including thermal/mechanical performance, effects on neutronics, compatibility with tritium systems, cost, consistency with overall EPR objectives, and extrapolation to future devices. A subjective evaluation of these features for the water and helium options is given in Table VIII-3.

Previous concerns over the use of water as a coolant in fusion reactors related mainly to: (1) inherently limited thermal efficiency; (2) undesirable

Table VIII-3. Assessment of Water and Helium as Primary Coolants for an EPR

<u>Constraint</u>	<u>Water</u>	<u>Helium</u>
Extrapolation to net power reactors	Materials limit efficiency Difficult interface with breeder blanket Extrapolation is marginal	Advanced power cycles are possible He test modules could be used in Stage II
Effects on neutronics	Excellent moderator; could replace most of the B_4C Small coolant channels minimize shielding difficulty	No moderating power; acts like a void Large void fraction increases shielding difficulty
Thermal/mechanical performance	Superior heat transfer characteristics Well established technology Requires negligible pumping power Larger ΔT 's with H_2O	Large void fraction complicates design Large pressure drops compared to H_2O Less well established technology than H_2O
Compatibility with tritium systems	A virtual sink for tritium Could lead to large volumes of tritiated effluents Primary circuit would provide excellent barrier for migration to secondary circuit Tritium entering H_2O is essentially lost from fuel cycle	Ideally suited for use in tritium environment Tritium removal is straight forward No large effluent problem Compatible with high level enrichment scenarios for fuel cycle
Cost	SS- H_2O design could lead to a relatively low cost device	Higher fabrication costs for comparable design base
Consistency with performance objectives	Best prospects for reliable long-term performance of EPR Incompatibilities with tritium systems are main drawbacks	More difficult to implement in a small tokamak EPR can't take full advantage of He usage

tritium interactions; and (3) reactivity with liquid metal (principally lithium) blankets. For the EPR design presented in this report, limitations imposed on the upper operating temperature of the construction materials (mainly Type 316 stainless steel) would restrict the peak efficiencies achievable with helium to values very near those obtainable with conventional pressurized water reactor thermal conversion practices.

An analysis of the implications with respect to tritium contamination and subsequent handling of the pressurized water (Chapter X) indicates that the problems are no more serious than those presently encountered (and dealt with) in pressurized heavy water reactors. The reactivity with breeder blanket materials is of no concern for the early operating stages of the EPR. In any case, the interfacing of pressurized water circuits with liquid metal circuits is not unprecedented, vis-a-vis LMFBR's.

In areas of direct concern to EPR operational reliability, water is much preferred over helium in that: (1) a more substantial technology base presently exists for water than for helium; (2) water is an excellent neutron moderator whereas helium essentially represents a void; (3) coolant pumping power and void fraction requirements in the first wall and blanket are much lower with water, which eases the complexity of both nuclear and thermal/mechanical design; and (4) water is more easily implemented in the design of relatively small tokamak-type power reactors.

Although a comprehensive design analysis of the blanket module test sections for the advanced operating stages of the EPR remain to be completed, there do not appear to be any compelling reasons why helium cooled test modules could not be included in the advanced program. Furthermore, the heretofore assumed marginal utility of water as a long range fusion reactor coolant needs to be reassessed in the context of the limiting features of other proposed coolants like helium, liquid lithium, molten salt, etc.

C. Thermal Analysis

The first wall design, as described in Section A, consists of a 2-cm-thick water-cooled stainless steel vacuum vessel protected from the plasma

by beryllium-coated stainless steel coolant panels. The design point for the analysis presented here is for a neutron wall load of 0.5 MW/m^2 , with the 1.0 MW/m^2 condition also investigated. A transient heat transfer analysis based on this design has been carried out for a representative 45-s-burn pulse followed by a 15-s exhaust and replenishment phase (burn pulses in the range ~ 25 to 60 s seem feasible for EPR). The coolant panels serve as a shield to protect the more permanent vacuum wall from the high cyclic heat loads that occur during normal operation and possible uncontrolled plasma-energy dumps. During normal operation the coolant panels receive a substantial portion of the fusion energy (equivalent to 20% of the neutron wall load) in the form of surface heating from plasma radiation and energetic ions. The vacuum wall, blanket and shield regions receive only the neutron heat loads. The results reported in this section were obtained from one-dimensional calculations carried out using a finite-difference, time-dependent heat transfer code. Three-dimensional analyses have also been performed; but, with the exception of regions near the cooling channels, the three-dimensional results are similar to the one-dimensional results presented here.

In keeping with the concept of using a low-Z liner material to reduce plasma contamination problems, the coolant panel surface was assumed to be coated with a $100\text{-}\mu\text{m}$ -thick layer of beryllium. A value of $8500 \text{ W/m}^2\cdot\text{K}$ ($1500 \text{ Btu/hr}\cdot\text{ft}^2\cdot^\circ\text{F}$) was developed for the resistance to heat transfer at the coating-substrate boundary. This resistance is likely to be a function of the age of the first wall, but was assumed to be constant in the present analyses. The heat transfer coefficient between the steel and the pressurized water coolant has been approximated at $2800 \text{ W/m}^2\cdot\text{K}$ ($500 \text{ Btu/hr}\cdot\text{ft}^2\cdot^\circ\text{F}$). The present coolant panel design (see Figure VIII-4) allows 75 to 80% of the 0.5-cm-thick panel face to be in intimate contact with the coolant.

The analysis summarized below was carried out for a case where the pressurized water was allowed to flow through a series of coolant panels until it was heated to a temperature suitable for electric power production by conventional methods, viz., existing PWR technology. The pressurized water was assumed to enter the first panel at 38°C and to exit the last panel (eighth in the series) at 310°C or less. A parametric analysis was completed on the present design for two neutron wall loads,

viz., 0.5 and 1.0 MW/m², and for two panel-wall thicknesses, viz., 0.5 and 1.0 cm. The plasma performance characteristics for the reference design are given in Table VIII-1. Results of this analysis, which are summarized in Table VIII-4, formed the basis for the fatigue calculations presented in Section D. The inlet temperature and coolant flow rates were set at values appropriate for net power production by the reactor. These conditions represent the limiting situation that the first wall will be required to withstand. The results indicate that there are no serious problems encountered in removing the thermal energy deposited in and on the first wall at 0.5 MW/m² for either wall thickness. However, at 1.0 MW/m² the 500°C design temperature is approached because of limited heat-transfer through the stainless steel wall. The 0.5-cm-thick panel wall, which meets both fabrication and coolant pressure requirements, has been selected for the reference design because of the greater margin of safety.

Table VIII-4. Summary of Coolant Panel Heat-Transfer
Analysis for the Eight Panel

Neutron Wall Load (MW/m)	0.5		1.0	
Panel-Wall Thickness (cm)	0.5	1.0	0.5	1.0
Coating	Be	Be	Be	Be
Maximum Temperature (°C)				
Coating	407	448	556	637
Stainless Steel Panel	380	416	506	587
Coolant				
Pressure (atm)	136	136	136	136
Flow Rate (lb/s)	2.13	2.13	2.13	2.13
Inlet Temperature (°C)	260	260	260	260
Exit Temperature (°C)	279	281	294	296

Since the most severe conditions with respect to materials response occur in the last panel, the calculational results for that panel are presented. Figure VIII-10 shows the radial temperature distribution through the 0.5-cm-thick beryllium-coated stainless steel plate after the first burn pulse. The upper and lower curves represent locations on the coolant exit and inlet planes, respectively. Figure VIII-11 shows the same type of data for the end of the sixth burn pulse, i.e., after 345 s. Figures VIII-12 and VIII-13 show the transient temperature histories for locations in the coolant inlet plane and exit plane, respectively. The upper, middle and lower curves in each figure represent, respectively, locations at the surface of the coating facing the plasma, at the surface of the stainless steel touching the coating, and at the stainless steel/coolant interface. The sharp spike in the coating temperature is caused by the introduction of argon into the plasma during shutdown. This increases the thermal radiation to the coating surface for a brief period near the end of the burn cycle, and is the most thermally limiting controlled shutdown option. A striking point brought out by these histories is that thermal equilibrium conditions are reached after only a few burn pulses.

Figures VIII-14 and VIII-15 represent the temperature distributions in the 0.5-cm wall operating with a 1.0 MW/m^2 neutron wall loading. The maximum temperature in the stainless steel is higher than the previous case but still within the design limit of 500°C maximum. The temperature at the interface between the coolant and the channel wall is close to the boiling limit; however, since the coolant is subcooled, no boiling is expected. The present analysis indicates that a 1.0 MW/m^2 neutron wall loading is near the limit for sustained power producing operation of this design. This limitation is due primarily to the relatively poor thermal conductivity of the stainless steel.

D. Stress Analysis

The stress analysis performed in conjunction with the structural design of the first wall has two basic goals: (1) to contribute to the development of a first-wall configuration with as small a mass as practical for the applied mechanical and thermal loads and other design constraints in order to minimize coolant system requirements and conserve space within the TF

Figure VIII-10. Temperature distribution through the beryllium-coated stainless steel panel after the first burn pulse (45 s) for locations on the coolant inlet plane (lower curve) and exit plane (upper curve).

Figure VIII-11. Temperature distribution through the beryllium-coated stainless steel panel after the sixth burn pulse (345 s) for locations on the coolant inlet plane (lower curve) and exit plane (upper curve).

Figure VIII-12. Transient temperature histories (0.5 MW/m^2) locations on the coolant inlet plane at the surface of the beryllium coating (upper curve), at the surface of the stainless steel in contact with the coating (middle curve), and at the stainless steel surface in contact with the coolant (lower curve).

Figure VIII-13. Transient temperature histories (0.5 MW/m^2) locations on the coolant exit plane at the surface of the beryllium coating (upper curve), at the surface of the stainless steel in contact with the coating (middle curve), and at the stainless steel surface in contact with the coolant (lower curve).

Figure VIII-14. Transient temperature histories (1.0 MW/m^2) locations on the coolant inlet plane at the surface of the beryllium coating (upper curve), at the surface of the stainless steel in contact with the coating (middle curve), and at the stainless steel surface in contact with the coolant (lower curve).

Figure VIII-1. Transient temperature histories (1.0 MW/m^2) locations on the coolant exit plane at the surface of the beryllium coating (upper curve), at the surface of the stainless steel in contact with the coating (middle curve), and at the stainless steel surface in contact with the coolant (lower curve).

coils; and (2) to maximize first-wall lifetime for prescribed operating conditions. A number of parameter studies were conducted to evaluate the feasibility of various first-wall design concepts from the stress analysis standpoint and to establish reasonable dimensions for those concepts that appeared most promising. These parameter studies are described in detail in Appendix H.

A design decision was made to divide the functions of the first wall between a vacuum vessel and detachable coolant panels. The implications of this decision for the structural analysis are that the vacuum vessel supports the atmospheric pressure load and the weight of the first wall, while the coolant panels absorb the most severe thermal loading but perform no structural function. Conservative limits are placed on allowable stresses and maximum temperature of the vacuum vessel to assure an adequate lifetime. Designing within these limits avoids stress failure, low-cycle fatigue, and thermal creep of the vacuum vessel and its supports; high-cycle fatigue of the vacuum vessel does not limit reactor life because its thermal cycling is less severe than that of the coolant panels. The design of the coolant panel support is such that no structural loads are transferred to the panels from the vacuum vessel, so that stress failure is avoided and the possibility of thermal ratcheting failure of the panels is eliminated. Fatigue resulting from thermal cycling is, therefore, the limiting factor on coolant panel lifetime. Stress analysis of the vacuum vessel and coolant panels is summarized below, with details given in Appendix H.

1. Vacuum Vessel

Several design concepts for the vacuum vessel and its supports were evaluated from stress analysis considerations. Supporting the vacuum vessel from the blanket was rejected because of the possibility of inducing excessive stresses in the vessel as a result of differential thermal expansion between the blanket and vessel. Use of a uniform solid wall for the vessel was rejected because of the large wall thickness required to prevent excessive bending stresses caused by distortion of the vessel under its own weight. A thick wall would be difficult to cool evenly, and relatively more heat would be deposited in the first wall rather than the blanket where it can be more efficiently used in the plant power cycle. Sandwich-wall construction offers a significant reduction in vacuum vessel mass as compared to a solid wall;

however, it was rejected because of the fabrication difficulty and expense in providing a bond with the required structural strength between the sandwich core and cover plates.

A vacuum vessel composed of a thin-walled shell with reinforcing rings has less than half the mass of a uniform solid-wall shell for a design stress of 10,000 psi. Buckling strength rather than stress considerations determines the wall thickness of the shell. The rings are sized to support the weight of a one-meter portion of the shell, their own weight, and the pressure differential across the vacuum wall. The roller/slide pads and the suspension hangers eliminate stresses from differential thermal expansion between supports and vessel.

Figure VIII-16 shows the effect of varying the support location on the required ring depth for 5-cm-wide rings and a 2-cm-thick shell. For a design stress of 10,000 psi, the original ring depth can be reduced from 19 cm to 8 cm if the supports are moved from the top to the 45° location. The vessel mass in the latter case is about two-thirds that of the former. The mass of the ring is thereby decreased to about 10% of the mass of the portion of the shell which it supports. Any further decrease in vessel mass would have to be achieved through a reduction in wall thickness.

The effect on ring size of supporting the weight of the coolant panels was not included in the above analysis. The panels plus the coolant tubes attached to the vessel are equivalent to about an additional 2-cm thickness of stainless steel attached to the vacuum vessel. The 5-cm-wide ring hung at 45° locations should have a depth of 11 cm to support the weight of the shell, coolant panels, coolant tubes, its own weight and the atmospheric pressure load on the vessel. A 10-cm-wide by 8-cm-deep ring would also support the same load at the 10,000 psi design stress.

Geometric-discontinuity stresses occur at the intersection of the shell and reinforcing rings. The circumferential membrane stress and axial bending stress in the shell at this location are less than 2000 psi. The maximum discontinuity stress resulting from a difference in average temperature between the shell and rings is about 100 psi/°C, so reasonably uniform temperature distributions must be provided by the structural coolant system to avoid large localized thermal stresses.

Figure VIII-16. Maximum Stress in Reinforcing Ring Supported at Various Locations.

A complete buckling analysis of the complex first-wall structure is beyond the scope of the current design effort. Analysis of some simpler approximating configurations gives a buckling pressure of 90 psi for the 2-cm-thick shell with supporting rings and spars. This corresponds to a safety factor of six on the applied atmospheric pressure; therefore, a decrease in the shell thickness to reduce the mass of the first wall may be justifiable. However, a reduction in thickness was not considered prudent without further study for three reasons: (1) the buckling pressure varies approximately as the cube of the wall thickness and so is quite sensitive to changes in this dimension; (2) simplified buckling analyses are usually non-conservative when compared with more detailed treatments of a complex structural configuration; and (3) analytical predictions of buckling loads for ideal shell structures usually overestimate the buckling loads of actual structures.

2. Coolant Panels

The supporting structure that connects the coolant panels to the vacuum vessel is such that the panels are kept flat but are free to expand laterally (see Figure VIII-4). There are two important advantages of these sliding supports over a fixed support arrangement.

- (1) Since no net force is transmitted to the panels from the vacuum vessel through the sliding supports, structural loads from the weight of the first wall and the pressure difference across the vacuum wall are not transferred to the coolant panels. For fixed supports, the panels and vacuum wall are structurally in parallel; therefore, design specifications and fabrication and assembly tolerances would have to be very accurate to avoid overloading of the weaker panels. The sliding support configuration prevents direct stress failure of the coolant panels from transferred structural loads as well as thermal ratcheting, where the combination of mechanical loads and thermal stresses results in plastic strain accumulation in one direction.
- (2) The overall thermal expansion of the coolant panels is not constrained by the sliding supports. Consequently, their mechanical

strain variation during thermal cycling is less than that of panels with fixed supports, and their predicted fatigue lifetime is considerably longer. The coolant panels are connected in series such that their average temperature varies from panel to panel, but the variation about the average temperature during the burn cycle is essentially the same for each panel. With sliding supports, each panel then has about the same expected number of burn cycles to fatigue failure since the pertinent strain range depends on the temperature variation during the cycle. However, with fixed supports maintained at the temperature of the average panel, the hotter and colder panels would be more severely strained than the average and so have shorter expected lifetimes.

The coolant panels are subjected to two types of thermal cycles; the burn cycle of about one minute duration and the reactor warm-up/cool-down cycle of widely variable duration. For a time-dependent temperature variation $\Delta T(t)$ across the panel thickness (see Section VIII-C), the strain range for the burn cycle depends on the difference between the maximum and minimum values of ΔT during the cycle. The strain range for the plant warm-up/cool-down cycle is a function of the average value of ΔT during the burn cycle. Assuming the duration of the warm-up/cool-down cycle is long enough that stress relief occurs, the strain range for the coolant panels with sliding supports is 0.085% for the burn cycle and 0.14% for the warm-up/cool-down cycle. These values correspond to fatigue design lifetimes of 5×10^6 burn cycles and 10^5 warm-up/cool-down cycles, respectively (see Figure VIII-8). For fixed supports, these strain ranges would be 0.21% and 0.64%, respectively, which correspond to design lifetimes of only 10^4 burn cycles and 3×10^2 warm-up/cool-down cycles. These estimates are based on the assumption that the lifetimes for the two types of cycles can be computed independently, i.e., that the expected lifetime from one type of cycling is reached much sooner than that from the other. If the operating history of the reactor is such that both lifetimes correspond to about the same passage of calendar time, a more detailed analysis using hypothesized histograms will be required to assess the cumulative fatigue damage.

E. Research and Development Requirements

The research and development requirements for the EPR first-wall system are summarized in this section. More detailed descriptions of these R & D requirements are given in reference 19. The major areas include materials technology, coolant technology, and structural mechanics. Effort required in these three areas is discussed below.

1. Materials

Substantial effort will be required on the development of low-Z coatings for impurity control, electrical insulators for flux breaker applications and mechanical property data for irradiated structural materials. Technology of coating processes and parameters must be investigated to develop optimized coatings that will withstand the fusion reactor environment. This effort includes determination of the thermomechanical and chemical stability of candidate coated composites under simulated reactor conditions. Surface radiation effects, e.g., physical sputtering, blistering, electron and photon bombardment, and various synergistic effects, on candidate coating materials must be investigated for appropriate particle energies and fluxes and for various coating microstructural characteristics. Bulk radiation effects at elevated temperatures must be evaluated for the composites under high helium generation rates.

Considerable effort is necessary to establish the operating limitations of electrical insulators in the severe radiation environment. This will include determination of both the mechanical integrity and dimensional stability of the insulators in addition to radiation effects on the dielectric properties. Development work associated with fabrication technology for current breaker applications is essential.

Further effort is also required to establish the operating limitations of the structural components under the anticipated radiation and thermal environments. In particular, mechanical property data for stainless steel under simultaneous displacement damage and high helium generation are needed for both bulk and weld material. Fatigue data for stainless steel coated composites under appropriate environmental conditions are important. Compatibility effects related to the tritiated water require further investigation.

2. Coolant Technology

Important early needs include development of computational methods and experimental assemblies to quantify the thermal-fluid behavior of the coolant, e.g., heat transfer coefficients and effective thermal conductivities for the first-wall designs. Of special importance to advanced operation with test modules is experimental confirmation that circulating liquid lithium in tokamak-type magnetic field configurations will not compromise either the plasma stability (by perturbing the magnetic flux) or the coolant performance of the lithium.

3. Structural Mechanics

Computational methods including code development and engineering support should be constantly upgraded to guarantee a firm technology base for the EPR. Analytical formalisms for modeling thermal, mechanical and kinetic behavior in toroidal geometry must be developed and tested. The response of the first-wall system to the mechanical and thermal stresses produced by the EPR burn cycle must be evaluated experimentally.

1. W. M. Stacey, Jr., et al., "Tokamak Experimental Power Reactor Studies," ANL/CTR-75-2, Argonne National Laboratory (July 1975).
2. R. F. Mattas, "Austenitic Stainless Steel Property Considerations for Fusion Reactor Applications," (to be published as ANL/CTR/TM).
3. See for example Effects of Radiation on Substructure and Mechanical Properties of Metals and Alloys, ASTM STP 529, American Society for Testing and Materials, (1973).
4. E. E. Bloom and F. W. Wiffen, "The Effects of Large Considerations of Helium on the Mechanical Properties of Neutron-Irradiated Stainless Steel," *J. Nuc. Mat.* 58, 171 (1975).
5. T. E. Scott, "Hydrogen Embrittlement and Other Effects in Thermonuclear Reactor Materials," IS-3634, Iowa State University, (1975).
6. J. B. Conway, R. H. Stentz, and J. T. Berling, "Fatigue, Tensile, and Relaxation Behavior of Stainless Steel," USAEC TID-26135, Technical Information Center, Office of Information Services, (1975).
7. C. R. Brinkman, G. E. Korth, and R. R. Hobbins, "Estimates of Creep-Fatigue Interaction in Irradiated and Unirradiated Stainless Steel," *Nuc. Tech.* 16, 297 (1972).

8. D. L. Smith, K. Natesan, T. F. Kassner, and G. J. Zeman, "Effects of Sodium on the Low Cycle Fatigue Behavior of Austenitic Stainless Steel," ASME Symp. on Structural Materials for Service at Elevated Temperatures in Nuclear Power Generation, MPC-1, 290 (1975).
9. S. H. Bush, "Structural Materials for Nuclear Power Plants," *ASTM J. of Testing and Evaluation*, 2, 435 (1974).
10. Joining of Stainless Steels, American Society for Metals, Metals Park, Ohio (1967).
11. J. T. Stanley and K. R. Garr, "Ferrite Formation in Neutron Irradiated Type 316 Stainless Steel," *Met. Trans.* 6A, 531 (1975).
12. "A Compilation of Boron Carbide Design Support Data for LMFBF Control Elements," HEDL-TME-75-19, Hanford Engineering Development Laboratory (1975).
13. A. A. Bauer and J. L. Bates, "An Evaluation of Electrical Insulators for Fusion Reactors," BMI-1930, Battelle Columbus Laboratory (1974)
14. J. F. Pesek and R. Tucker, Union Carbide Corp., personal communication.
15. D. Rossen, Oak Ridge National Laboratory, personal communication.
16. R. J. Colchin, "Impurities from Plasma-Wall Interactions in ORMAK," Proc. Inter. Conf. Surface Effects in Controlled Fusion Devices, San Francisco (1976) to be published *J. Nucl. Mater.*
17. S. K. Das and M. Kaminsky, "Reduction of Surface Erosion Caused by Helium Blistering in Sintered Beryllium and Sintered Aluminum Powder," Proc. Inter. Conf. Surface Effects in Controlled Fusion Devices, San Francisco (1976) to be published *J. Nucl. Mater.*
18. E. L. Dean and S. P. Smith, Kaman Sciences Corp., Colorado Springs, Colorado, personal communication.
19. F. E. Mills, "Research and Development Required for the Tokamak Experimental Power Reactor," ANL/CTR/TM-65, Argonne National Laboratory (1976).

CHAPTER IX

BLANKET/SHIELD SYSTEM

The EPR primary energy conversion system (PECS) studies have sought to identify the major design criteria and constraints for the first wall, blanket, and radiation shield of a tokamak experimental power reactor. The criteria for the first wall assembly and the design point that has been reached in an effort to accommodate them are described in the preceding chapter (Chapter VIII). In this chapter, the results of a systematic analysis of the important operational features of EPR-type blanket and shield systems are presented in the context of a consistent reference design description for these systems. The blanket/shield design features that have appeared to be the most important relate to: (1) nuclear performance; (2) segmentation to allow field penetration and reduce magnetically induced torques; (3) materials property limitations; (4) accessibility for fabrication, assembly, and maintenance; and (5) thermal-hydraulic performance. This design is summarized in Table IX-1.

The nuclear design is based on optimized configurations of radiation attenuating materials that permit the retrieval of the neutron and secondary gamma ray energies as sensible heat in a form suitable for conversion to electricity using conventional power cycle technology. An important objective of the blanket and shield nuclear design is to provide the degree of radiation attenuation necessary to protect the superconducting magnet systems from excessive radiation damage and nuclear energy deposition. With the PECS design, > 92% of the thermonuclear power is deposited in the ~ 4-cm-thick first wall assembly and the 28-cm-thick stainless steel blanket assembly immediately surrounding the first wall. The remaining radiation shielding is provided by the near-ambient shield system that circumscribes the blanket. A significant finding of the nuclear analyses has been the need for appreciable thicknesses of highly efficient radiation shields around the major penetrations. The important nuclear performance related parameters for the EPR are listed in Table IX-1, and the details of the nuclear analyses are given in Section A of this chapter and in Appendix C.

Table IX-1. Summary of Blanket/Shield Design Parameters for the ANL-EPR

BLANKET SYSTEM

Design Basis Operating Life (years)	10
Nominal Power During Burn (MW)	400
Design Basis Neutron Wall Loading, (MW/m ²)	0.5
Plant Capacity Factor (%)	50
Blanket Structure	
Thickness, (m)	0.28
Type Metal/Volume Fraction	316-SS/0.9
Type Coolant/Volume Fraction	H ₂ O < 0.05
Penetration/Volume Fraction	
Inner Blanket	~ 0.02
Outer Blanket	~ 0.05
Maximum Temperatures, °C	
In Support Structures	500
In Bulk Materials	550
Nuclear Parameters	
Maximum Heat Generation (W/cm ³)	3.5
Maximum Fluence at 2.5 MW-yr/m ² (n/m ²)	5 x 10 ²⁶
Maximum dpa at 2.5 MW-yr/m ² (dpa)	17
Maximum helium production at 2.5 MW-yr/m ² (appm)	230
Maximum hydrogen production 2.5 MW-yr/m ² (appm)	600
Mechanical Parameters	
Design Stress in Support Structure, ksi	≤ 10
Minimum Material Yield Stress, ksi	20
Ductility at 2.5 MW-yr/m ² (% uniform elongation)	≥ 3
Swelling at 2.5 MW-yr/m ² (% of initial volume)	≤ 2
Maximum torque from pulsed fields (ft.-lb)	125,000
Coolant Parameters	
Type	H ₂ O
Maximum Pressure (psig)	2000
Fractional Pressure Drop (%)	< 1
Maximum Velocity (m/s)	2.4
Pumping Power (MW)	< 1
Coolant Inlet Temperature (°C)	40
Maximum Coolant Exit Temperature (°C)	309

Table IX-I. (Cont.) Summary of Blanket/Shield Design Parameters for the ANL-EPR

SHIELD SYSTEM

Design Basis Operating Life (years)	10
Shield Structure	
Thickness (m)	
Inner Shield	0.58
Outer Shield	0.97
Beam Duct Shield	0.75
Evacuation Duct Shield (movable plug)	0.90
Biological Shield	1.5
Materials	
Inner Shield	304-SS/B C
Outer Shield	304-SS/Pb mortar/C/Al ⁴
Beam Duct Shield	316-SS/B C/Lead/Al ⁴
Evacuation Duct Shield (movable plug)	304-SS/B C ⁴
Biological Shield	Concrete
Temperature (°C)	< 90°C
Coolant	H ₂ O
Maximum Torque From Pulsed Fields (ft.-lb)	253,000
Maximum Nuclear Heating (W/cm ²)	0.3
Fraction of Fusion Power Deposited in Shield Systems	~ 0.07
Maximum Energy Current at Outer Surface of Shield (W/cm ²)	
Neutrons	~ 2 x 10 ⁻⁴
Gammas	~ 5 x 10 ⁻⁵
Residual activity from First Wall Blanket/Shield radiation waste after 2 yr. operation in Ci/MWt	
Immediately after shutdown	3.5 x 10 ⁶
1 yr. after removal	8.0 x 10 ⁵
10 yr. after removal	7.0 x 10 ⁴
100 yr. after removal	60

The selection of materials for the EPR has been predicated largely on state-of-the-art and reasonably extrapolated technology. A comprehensive assessment of existing information regarding (1) irradiation effects on materials properties, (2) present day fabrication capabilities and limitations, and (3) the costs associated with various materials alternatives has led to the selection of various grades of stainless steels as the major construction materials for the PECS. A similar basis has been developed for the selection of B_4C , graphite, lead mortar, and a variety of insulating materials for specific functions as indicated throughout this chapter. More detailed discussions of the factors that have impacted on or otherwise influenced the selection of materials for the PECS are given in Section B of this chapter and in Appendix E.

One of the more significant findings of the PECS design studies has resulted from analyses of the interactions of the time varying magnetic fields with the blanket and shield assembly. Eddy currents generated in the blanket and shield region by the pulsed fields produce opposing magnetic fields that tend to interfere with the plasma equilibrium and at the same time create large forces and torques within the blanket/shield assembly. The solution developed for this problem was to subdivide the blanket and shield into sufficiently small electrically insulated sections (blocks) such that the field perturbations at the plasma and the torques were reduced to tolerable levels. The methods developed to analyze this problem, the nature of the solution, and the overall impact on the blanket/shield design are described in Sections C and D of this chapter and in Appendix F.

The mechanical design for the EPR has evolved on the basis of nuclear functional requirements, energy recovery considerations, field penetration criteria, materials constraints, fabricability, maintainability, and economics. Two types of blanket/shield arrangements have been considered: (1) a contoured close-fitting circular assembly and (2) a rectangular slab-type assembly. Although the circular assembly is taken as the reference design, both assemblies have been subjected to design analysis. The circular design consists of 16 segments, each composed of 17 blanket blocks and 26 shield blocks, which cover the first wall assembly with a total of 688 separate blanket and shield blocks. Each block is electrically insulated from its neighboring blocks and is independently cooled.

Detailed descriptions of the blanket/shield mechanical design are given in Section D of this chapter.

Thermal performance analyses of the blanket blocks show that these blocks can be adequately cooled with pressurized water under conditions where: (1) the coolant volume fraction and pumping power requirements are quite reasonable; (2) the coolant inlet and exit temperatures, pressures, velocity, etc. are suitable for use with a conventional pressurized water reactor-type steam generator; and (3) there is sufficient adjustment potential in the coolant parameters to accommodate a wide range of cooling requirements. Cooling of the shield blocks with a low pressure water circuit was also found to present no serious problems. A discussion of the thermal performance analyses is given in Section E.

The last section of this chapter (section F) contains a summary of the major research and development items that have been identified during the course of the blanket/shield design activities of the past two years.

A. Nuclear Design of the Blanket/Shield System

The neutronics and photonics analyses play a major role in defining a technically sound design for the blanket/shield system. This section is devoted to the nuclear analysis of the EPR reference design. The discussion is limited to only the major design criteria and the prominent neutronics and photonics effects in the reactor. Additional detailed calculations and sensitivity studies are presented in Appendix G. The analyses of radioactivity, afterheat, and biological dose in the reference design are given in Chapter XIV.

1. Primary Design Criteria and Constraints

The primary function of the blanket is to convert, and possibly multiply, the kinetic energy of fusion neutrons and secondary gamma-rays into heat at sufficiently high power density so that thermal energy retrieval and conversion to electricity with a reasonably high efficiency is feasible. This requirement of high power density implies the use of efficient attenuating materials which in turn leads to a relatively small overall blanket thickness.

The blanket/shield system is required to reduce the radiation level in all reactor components to a permissible level. The most critical of

these components are the superconducting toroidal field (TF) coils. Protection of these coils is also the most difficult in view of (1) space restrictions in the high magnetic field region between the plasma and the interior portion of the D-shaped magnet; (2) the presence of many large-size penetrations on the top, bottom, and outer side of the plasma which greatly enhance radiation streaming to the TF coils; and (3) the stringent magnet protection criteria which result from low operating temperature, low threshold for radiation damage, and the intolerable consequences of virtually all major failure modes in the TF coils. A study of the radiation effects on components of superconducting magnets as well as a trade-off study of the conflicting requirements for an EPR has resulted in the definition of the following primary magnet protection criteria:

- (1) Maximum radiation-induced resistivity increase in the copper stabilizer of the superconducting toroidal field coils should be $< 3 \times 10^{-8} \Omega\text{-cm}$ for a maximum magnetic field of 8 T and $< 1.5 \times 10^{-8} \Omega\text{-cm}$ for a maximum field of 10 T during the longest time period between scheduled magnet anneals.
- (2) Maximum tolerable decrease (due to irradiation) in the critical current density of the NbTi superconductor during the longest time period between scheduled magnet anneals is $< 5\%$ of the pre-irradiation value.
- (3) Maximum local energy deposition in the conductor matrix by nuclear radiation must not exceed 10^{-3} W/cm^3 at full reactor power.
- (4) The total nuclear heating in all the toroidal field coils should be small compared to other heat sources that contributes to the refrigeration power requirements.
- (5) The total radiation dose to all cryogenic coil-insulators must be kept at a level that permits all these insulators to function properly for the magnet lifetime ($\sim 10^9$ rads for the aromatic base epoxies).

The foundations for these design criteria are discussed in detail elsewhere.^{1,2} However, a brief clarifying discussion is in order. Radiation damage studies on normal and super conductors at 4°K indicate that the increase in the resistivity of the normal conductor (used as the stabilizer) and the decrease in the critical current density of the superconductor can be recovered by near room temperature annealing. Therefore, criteria 1 and 2 above are set for only the time span between magnet anneals. Since inorganic insulators that are resistant to radiation damage are too brittle to be useful in the toroidal field coils, organic insulators have to be used. As will be discussed subsequently in this section, radiation damage to organic insulators is irreversible. In addition, any replacement of the coil insulation is almost impossible, and exchange of the coils themselves is very expensive. Therefore, criterion 5 requires that the maximum radiation dose tolerated in the coil insulation be based on the magnet lifetime. Data on radiation damage to organic insulators at 4°K do not exist. In view of this situation and until more experimental results are available, extrapolation of available data at higher temperatures has led to the tentative choice of an epoxy coil insulation set with an aromatic type curing agent, reinforced with fiberglass and filled with granular alumina. A total integrated radiation dose of 10^9 to 10^{10} rad may be tolerable for these epoxy composites.

One of the major constraints on the blanket/shield system in EPR is a severe restriction on space in the high magnetic field region on the inside of the torus between the plasma and the vertical segment of the D-shaped toroidal field coils. To conserve on space in this critical region without violating the magnet protection criteria discussed above, a combination of materials that is very effective in attenuating neutrons and secondary gamma-rays must be used on the inner side of the torus. Unfortunately, such materials are generally more expensive and they result in significantly more neutron-induced activation than the less efficient attenuators. However, because of the relatively small volume of the inner shield (5 to 10% of the total), the increase in material cost - if kept moderate - can be more than offset by a net reduction in the overall reactor cost resulting from a substantial reduction in reactor size and/or a more than compensating increase in the reactor power output.

Since the vertical and horizontal bores of the D-shaped TF coils can be increased without large cost penalty, there can be more space on the top, bottom, and outer side of the plasma than is available on the inside. Thus, the shield in these regions can, in principle, be constructed with materials that meet many other desirable criteria (such as reduced cost and induced-activation) but still provide adequate protection of the TF coils and auxiliary equipment. However, the EPR, as well as future tokamaks, requires that the blanket/shield system accommodates major penetrations, including those for vacuum pumping, neutral beam and/or radio frequency (rf) heating, diagnostics, and maintenance access. Many of these penetrations represent large open regions (some $\sim 0.6 - 1 \text{ m}^2$ in cross-sectional area) which extend from the first wall (directly visible to the plasma neutrons) radially outward through the blanket/shield and between the toroidal-field coils. Since these penetrations are numerous and large they can be accommodated only on the top, bottom, and outer regions of the shield. As will be shown later in this section, the bulk magnet shield can do very little to attenuate radiation streaming through these penetrations, and a special penetration shield has to be provided. The penetrations and their special shield create a space problem inside and between the toroidal field coils. This again calls for efficient attenuating materials for the penetration shield.

Since the bulk shield is not efficient in protecting the TF coils from neutrons and gamma-ray streaming through penetrations, the following design criteria were adopted:

- (1) For a given material composition, the dimensions of the bulk shield should be kept to the minimum that is sufficient to protect the TF coils in regions far away from the penetrations (i.e. in complete absence of any penetration effects).
- (2) The penetration shield must completely offset the penetration effects at the TF coils; i.e., the penetration shield must reduce the maximum radiation level at the TF coil to that in the absence of penetrations.
- (3) The penetration shield must also extend far enough to protect auxiliary systems external to the TF coils that are affected by penetration-assisted streaming.

In the presence of an abundance of high energy neutrons, activation of many of the reactor components is unavoidable and the consequences must be factored into the design. Construction of all components of an EPR from low-activity materials, such that contact maintenance is feasible everywhere in the system in a short time after shutdown, is not possible at present. For example, employing aluminum as the supporting structure and stabilizer in superconducting magnets requires resolving a considerable number of technological and economic problems. The use of a graphite blanket and Al-B₄C shield on the inner side of the torus results in a large size reactor with an added cost that outweighs the benefits of contact maintenance. Thus, remote maintenance has to be planned for in the EPR design.

There are two goals, however, concerning induced-activation which are achievable for EPR and which have been included as design criteria for the present EPR design. These are:

- (1) The total inventory of radioactive materials should be kept to a minimum in cases where there are no serious conflicts with other critical design criteria. This aims toward reducing the inventory of materials that will require provision for long-term storage at disposal time.
- (2) The biological dose in the space inside the reactor building and external to the torus and the magnets should be reduced to a level which permits controlled personnel access to this region within a reasonably short time (~ 24 hours) after shutdown. Since the walls of the reactor building, which also serve as the biological shield during operation, are thick (~ 1.5 m), there is a substantial saving on remote maintenance equipment cost and repair time if personnel access to the reactor building is feasible.

2. Description of the Nuclear Systems for the EPR: Reference Design and Calculational Models

For the purposes of the nuclear analysis, the blanket/shield system can be classified into the following subsystems: (1) first wall assembly, (2) blanket, (3) bulk shield and (4) penetration shield. Figure IX-1 shows a schematic cross section of the reactor. Many of the engineering details

Figure IX-1. EPR Blanket/Shield Layout

are omitted from this figure. The bulk shield circumscribes the blanket and consists of the inner, top, bottom, and outer shield. Since the bulk shield is essentially the same in the last three regions, these regions will be referred to collectively as the outer shield unless a need for distinction arises. Tables IX-2 and IX-3 show the dimensions and material compositions of the first wall, blanket, and bulk shield for the inner and outer regions, respectively, at the midplane.

The stainless steel assembly which forms the first wall, with internal coolant passages (described in Chapter VIII), is idealized as a uniform 4-cm-thick stainless steel region that has an inner minor radius of 240 cm and the coolant passages homogenized. The blanket is symmetric and consists of a 28 cm-thick region of stainless steel with space provided for coolant. The inner shield consists of a 58-cm-thick region containing alternating layers of stainless steel and boron carbide in an optimized configuration to produce maximum attenuation of neutrons and secondary gamma-rays. The two sides of the inner/outer shield interface form a 50° angle at the plasma center. The outer bulk shield is 97 cm thick and consists of 15 cm of graphite with 1% natural boron, 65 cm of lead mortar, 8 cm of stainless steel, and 9 cm of aluminum. The composition and atom densities of lead mortar are shown in Table IX-4. The major constituents of lead mortar are H, O, B, C, and Pb, all of which have no significant long term activation. Lead mortar can withstand heat conditions up to $\sim 150^\circ\text{C}$. While the graphite is not as effective in neutron and gamma-ray attenuation as lead mortar, graphite is employed at the outer perimeter of the blanket to ensure a sufficiently low power density in the lead mortar. Zones 19 through 27 in Table IX-2 and Zones 26 through 40 in Table IX-3 represent a reasonable idealization of the toroidal-field coil assembly. The aluminum structure in the exterior regions of the outer shield and the TF coils has been purposefully included in the design for reasons which will be discussed subsequently. Because of the complexity of the geometric details required to describe the penetration shield, its description is deferred to part 7 of this section, which is devoted to the penetration analysis.

Neutron and photon transport calculations for the nuclear analyses were carried out in one-, two-, and three-dimensional geometries. One-

Table IX-2. Dimensions and Material Compositions of the EPR Inner Blanket/Shield at Midplane

Zone	Outer Major Radius, ^a cm	Outer Minor Radius, ^b cm	Thickness cm	Material Composition	Density Factor	Description
1	625	210	210	Plasma	0.0	Blanket First Wall
2	415	240	30	Vacuum	0.0	
3	385	261	1	Stainless Steel	1.0	
4	384	242	1	Stainless Steel (+ coolant)	0.4	
5	381	244	2	Stainless Steel	1.0	
6	381	254	10	Stainless Steel	0.9	
7	371	264	10		0.9	
8	361	272	8		0.9	
9	353	273	1	Vacuum	0.0	Shield
10	352	276	3	Stainless Steel	0.45	
11	349	281	5	Boron Carbide	0.90	
12	344	291	10	Stainless Steel	0.95	
13	334	297	6	Boron Carbide	0.90	
14	328	307	10	Stainless Steel	0.95	
15	318	315	8	Boron Carbide	0.90	
16	310	325	10	Stainless Steel	0.95	
17	300	330	5	Boron Carbide	0.90	
18	295	331	1	Stainless Steel	0.95	
19	294	334.5	3.5	TFC dewar (Al)	1.0	TF Coil Assembly
20	290.5	337	2.5	Vacuum, liquid nitrogen tubing, and superinsulation	0.0	
21	288	342	5	Thermal shield (Al)	0	
22	283	344.5	2.5	Vacuum (+ super-insulator)	0.0	
23	280.5	345.7	1.2	TFC bobbin (SS)	1.0	
24	279.3	350.7	5.0	TFC (44% SS + 41% Cu + 3% NbTi + 12% He)	1.0	
25	274.3	355.7	5.0		1.0	
26	269.3	360.7	5.0		1.0	
27	264.3	416.2	55.5		1.0	
28	208.8	440.0	23.8	Support Cylinder (SS)	1.0	
29	185	465.0	25	OHC (40% SS + 28% Cu + 2% NbTi + 30% He)	1.0	

^a Minimum distance in midplane from the center of the torus to the outer boundary of the zone.

^b Distance along the minor radius from the center of the plasma to the outer boundary of the zone.

Table IX-3. Dimensions and Material Compositions of the EPR
Outer Blanket-Bulk Shield at Midplane

Zone	Outer Major Radius, ^a cm	Outer Minor Radius, ^b cm	Thickness, cm	Material Composition	Density Factor
1	835	210	210	Plasma	0.0
2	365	240	40	Vacuum	0.0
3	866	241	1	Stainless Steel	1.0
4	867	241	1	SS (+ coolant)	0.4
5	869	241	2	Stainless Steel	1.0
6	893	254	10	Stainless Steel	0.9
7	889	254	10		0.9
8	897	252	8		0.9
9	898	273	1	Vacuum	0.0
10	901	276	1	Stainless Steel	0.95
11	906	281	5	Graphite (1% B)	0.90
12	911	286	5		0.90
13	916	291	5		0.90
14	921	296	5	Stainless Steel	0.95
15	926	301	5	Lead Mortar	0.95
16	926	311	10		0.95
17	946	321	10		0.95
18	956	331	10		0.95
19	966	341	10		0.95
20	976	351	10		0.95
21	981	356	5		0.95
22	986	361	5		0.95
23	991	366	5	Aluminum	0.95
24	995	370	4	Aluminum	0.95
25	1061.3	436.3	66.3	Free Space (Vacuum)	0.0
26	1064.8	439.8	3.5	TF coil dewar (Al)	1.0
27	1067.3	442.3	2.5	Vacuum, liquid nitrogen tubing, and superinsulation	0.0
28	1072.3	447.3	5	Thermal shield (Al)	1.0
29	1074.8	449.8	2.5	Vacuum (+ superinsulation)	0.0
30	1076	451.0	1.2	TF Coil bobbin (SS)	1.0
31	1081	456.0	5.0	TF Coil (44% SS + 41% Cu + 3% NbTi + 12% He)	1.0
32	1086	461.0	5.0		1.0
33	1091	466.0	5.0		1.0
34	1146.5	521.5	55.5		1.0
35	1154.8	529.8	8.3	Helium bath (liquid + gas)	0.0
36	1156	531	1.2	TF Coil bobbin (SS)	1.0
37	1158.5	533.5	2.5	Vacuum + superinsulation	0.0
38	1163.5	538.5	5	Thermal shield (Al)	1.0
39	1166	541	2.5	Vacuum, liquid nitrogen tubing, and superinsulation	0.0
40	1169.5	544.5	3.5	TF Coil dewar (Al)	1.0

^aMinimum distance in midplane from the center of the torus to the outer boundary of the zone.

^bDistance along the minor radius from the center of the plasma to the outer boundary of the zone.

Table IX-4. Composition and Atom Densities of Lead Mortar^{*}

Element	Composition w/o	Atom Density (10^{22} atoms/cm ³)
H	2.4	3.6
O	3.3	0.3
B	5.0	6.7
C	15.2	1.9
Pb	73.6	0.5
Others	0.5	-

* Composition selected here corresponds to Chemtree product CT-1-6-82-5 with a physical density of 2.5 gm/cm³

and two-dimensional geometries were calculated by the S_n method with ANISN,³ DOT,⁴ and TWOTRAN.⁵ The continuous energy Monte Carlo code VIM⁶ was employed to treat the three-dimensional geometries. The S_n calculations were carried out with 100 energy groups for the neutrons and 43 energy groups for the gamma-rays with cross sections generated from ENDF/B. Response functions were generated with MACK⁷ from ENDF/B.⁸

Nuclear analysis for the EPR blanket/shield requires multi-dimensional calculations to treat the following effects: (1) the presence of major penetrations, (2) the toroidal geometry, and (3) the non-symmetry of the shield that can be seen in a vertical cross section of the ANL-EPR design (Figure IX-1). The penetrations require 3-D calculations, but the other two effects can be analyzed by adaptation of a 2-D model to handle toroidal geometry as well as any non-symmetry in the vertical cross section. A solution accurate everywhere in the system requires full 3-D geometric modeling, as it is not possible to completely isolate the major individual effects listed above. At present, prediction of neutron and photon transport in 3-D is feasible only with the Monte Carlo method. Because of the statistical nature of the method, obtaining a reasonably accurate solution everywhere in a complicated large-size geometry with deep radiation penetration is too expensive at present.

Since the ANL-EPR blanket/shield is toroidally axisymmetric, all the effects except those for penetrations can be reasonably accounted for in a 2-D model. The approach used for the nuclear analysis here is (1) application of 2-D calculations to predict the gross behavior of the neutronics and photonics effects away from the penetrations and (2) application of 3-D calculations with geometric modeling which includes the penetrations and the penetration shield to analyze the effects in penetration regions and to superimpose the penetration effects on the gross behavior predicted by the 2-D calculations.

The rest of this section is organized as follows. Subsection 3 is devoted to the toroidal geometry effects. Subsections 4 and 5 present the gross behavior of the neutronics and photonics effects in the blanket and bulk shield away from the penetration regions. Since the criterion for the local penetration shield has been to reduce the radiation level at the TF coils to that in the absence of penetrations, the neutronics effects in the TF coils given in subsection 6 are essentially the same (except for small variations) in the absence of penetrations as in the presence of fully-shielded penetrations. Subsection 7 is devoted to an analysis of penetrations, penetration shield, and penetration effects on the nuclear performance of the blanket and bulk shield.

3. Toroidal Geometry Effects in the Reference Design

The neutron transport equation can be solved in an axisymmetric toroidal system (see figure IX-2) using two approaches: (1) a 2-D geometrical model in (R,Z) cylindrical representation with the poloidal axis as the axis of the cylinder and R as the major radius; and (2) a 2-D geometrical model in (r,χ) representation with r as the minor radius and χ the poloidal angle, provided that the toroidal curvature is properly accounted for in the transport operator. The first method has at least one drawback, particularly for circular tokamaks. Current S_n codes employ a rectangular spatial mesh which cannot yield a sufficiently adequate representation of the geometry, particularly the first wall, with a reasonable number of mesh points. The second (r,χ) approach is very useful in overcoming this difficulty. Therefore, the TWOTRAN program⁵ was modified according to the formulation presented in reference 9 to solve the transport equation in axisymmetric toroidal geometry with the cross section of the torus represented in the (r,χ) coordinates. The

Figure IX-2. A Schematic Reference of Toroidal Geometry.

new program was then used to calculate the neutronics effects in the reference design.

It was found in the course of this work that the local neutronics effects in the first wall and blanket are far more sensitive to the spatial distribution of the D-T neutron source strength as affected by the toroidal geometry and the MHD equilibrium distribution of the plasma than they are to the local toroidal curvature in the first wall and blanket. Therefore, a neutron source distribution was derived from the MHD equilibrium plasma distribution of the reference case plasma, as described in Chapter III. This source distribution is shown in Figure IX-3 as a function of the major radius, R , for several values of $Z = \text{constant} = Z_c$, where Z represents the elevation from the midplane along the poloidal axis. An isomeric view of this source distribution is also shown in Figure IX-4. The neutron source strength per unit volume, $S(R, Z_c)$, peaks inside the plasma region and decreases steadily until it reaches zero at $R = R_0 + Z_c / \tan \alpha$ (note that the major radius at the torus centerline R_0 is 625 cm and the plasma radius is 210 cm). The peak of $S(R, 0)$, i.e. at midplane, occurs at $R = 686 = R_0 + \epsilon_0$ with ϵ_0 defined as the plasma shift. The peak of $S(R, Z_c)$ occurs at a smaller shift as Z_c is increased.

This reference neutron source distribution was used in (r, α) toroidal geometry calculations. The neutron wall loading as a function of the poloidal angle α is shown as curve a in Figure IX-5. The neutron wall loading is used here in its normal definition¹⁰ as the product of the D-T neutron current in the direction of the normal to the first wall times the energy (14.06 MeV) of a D-T neutron. Curve b in Figure IX-5 represents the neutron wall loading when the toroidal geometry calculations are made with a volumetric neutron source distribution which is uniform (constant) throughout the plasma region. The third line, c, in this figure is for a nominal case widely used in the literature, a one-dimensional, infinite cylinder approximation with the cylinder axis representing the toroidal magnetic axis and the volumetric neutron source constant in the plasma region. The three cases are normalized such that the total number of D-T neutrons emitted from the plasma per unit time is the same. In other words, a fixed thermonuclear power for the reactor is taken as the basis for normalization. Thus, the neutron wall loading,

Figure IX-3. Source Strength, $S(R, Z)$, of the D-T Neutrons in the Plasma Region as a Function of Major Radius at Several Values of Z .

Figure IX-4. Isomeric View of the DT neutron source strength in the EPR plasma region.

Figure IX-5. Variation of the Neutron Wall Loading with the Poloidal Angle α for three cases; a-neutron source distribution as derived from the MHD equilibria in the reference plasma calculations, b-uniform neutron source distribution, c-the nominal case of a uniform source distribution in an infinite cylinder approximation.

0.5 MW/m², in the nominal case represents also the mean neutron wall loading in all cases, independent of any assumption about the volumetric neutron source distribution. This mean neutron wall loading is the average of the neutron wall loading over the inner surface area of the first wall, or it is simply equal to the thermonuclear neutron power divided by the first wall area.

Some important observations can be made from the results in Figure IX-5. For the reference case the neutron wall loading, P_w , varies from ~ 0.56 MW/m² at the outermost point ($\chi = 0^\circ$) to ~ 0.4 MW/m² at the innermost point ($\chi = 180^\circ$). The maximum neutron wall loading is 12% above the mean and 40% above the minimum. The uniform source distribution, however, results in only $\sim 8\%$ variation in P_w around the mean. Thus, the neutronics effects are strongly dependent on the form of the volumetric neutron source distribution. This implies a strong correlation between the nuclear performance of the first wall/blanket/shield and the plasma spatial characteristics. These calculations of the neutron wall loading are consistent with the work of Chapin and Price¹¹ which examined the behavior of the uncollided fluxes and currents in toroidal systems.

Figures IX-6 and IX-7 show the variation of the rates of helium production and atomic displacements within the first 1-cm of the first wall with the poloidal angle χ for the same three cases of Figure IX-5. For case a, where the neutron source distribution is derived from the plasma spatial distribution, the helium production is maximum at $\chi = 0^\circ$ (outermost), and decreases monotonically as χ is increased until it reaches a minimum at $\chi = 180^\circ$ (innermost). The maximum to minimum ratio is ~ 1.3 . The uniform source distribution, on the other hand, shows a helium production which is maximum near the top of the torus and is higher in the inner region than on the outside. The helium production for the nominal case of the infinite cylinder approximation is also shown in Figure IX-6. An important conclusion to be drawn from the results in Figures IX-6 and IX-7 and from comparing the results for other response rates is that the infinite cylinder approximation with a uniform volumetric source overestimates the reaction rates at all points of the first wall. Thus, earlier results^{2,12} published in the literature, based on this 1-D model, represent conservative estimates of the radiation damage indicators in the first wall, as far as the maximum rates of gas production and atomic displacements are concerned.

Figure IX-6. Variation of the Helium Production Rate within the First 1-cm of the First Wall with the Poloidal Angle α .

Figure IX-7. Variation of Atomic Displacements within the First 1-cm of the First Wall with the Poloidal Angle χ .

The results in Figures IX-5 through IX-7 can be explained by a detailed analysis of the angular fluxes and the collision probabilities at all points in the first wall for all cases considered. However, the fact that the reaction rates at the first wall calculated in the toroidal geometry representation are lower than those predicted by the infinite cylinder approximation with a constant volumetric neutron source can be explained qualitatively as follows. The collision probability of a neutron incident on the first wall generally increases as the angle of incidence, θ (see Figure IX-2), to the first wall is increased; i.e., neutrons incident at a direction close to the tangent ($\theta = 90^\circ$) to the wall have a greater probability for inducing a reaction in the wall than neutrons incident near the normal ($\theta = 0^\circ$) to the wall. In the infinite cylinder approximation with a volumetric neutron source, the angular flux is peaked (actually singular) at the tangent direction parallel to the cylinder axis. In the toroidal geometry representation, the angular flux is zero for $\theta > \theta_{\max}$, where $\theta_{\max}$ depends on the poloidal angle χ , the neutron source distribution and the characteristics of the torus. The maximum angle of incidence, $\theta_{\max}$, is $< 80^\circ$ at $\chi = 0^\circ$ and it increases slowly but is $< 90^\circ$ at all points on the outer part of the torus. At the inner side of the torus where $\theta_{\max}$ can reach 90° , the angular flux is still higher¹¹ at small θ since the flux near $\theta \sim 90^\circ$ does not cover the entire range of the angle ϕ (See Figure IX-2) while it covers all values of ϕ for small θ . This explains qualitatively two effects: (1) the reaction rates on the outer side of the torus with the uniform and reference source distributions are lower than the reaction rates predicted in the infinite cylinder approximation although the neutron current to the wall is lower in the latter case, and (2) the reaction rates in the toroidal geometry-uniform source case have maxima near $\chi = 105^\circ$.

The volumetric neutron source distribution derived from the reference plasma case has two important characteristics compared with the uniform source distribution. The reference source is peaked inside the plasma and drops rather rapidly to zero at the plasma boundary. Moreover, the peak occurs at the horizontal midplane and is shifted toward the outer part of the torus. The "peaking" effect alone causes a reduction in the number of neutrons incident close to the tangent of the first wall and hence a reduction in the reaction rates in the first wall compared with the uniform source.¹³ The geometric shift of the source peak causes the total number of

neutrons crossing a unit area of the first wall to increase at the outer part of the torus and decrease on the inner side of the torus.

The neutronics calculations in toroidal geometry show several important considerations that need to be accounted for in a tokamak reactor design. Among these are the variation of the angular flux with position around the first wall surface. Figures IX-6 and IX-7 show that the rate of gas production and atomic displacement in the first wall vary with the poloidal angle χ . Variation of radiation damage effects with position in a structural member is undesirable. Differential swelling, for example, may create large stresses. It should be noted, however, that the largest rate of the poloidal variation in the neutronics effects is $< 0.1\%$ per centimeter along the minor circumference of the first wall, compared with a typical variation due to attenuation of $> 10\%$ per centimeter radially outward. Thus, although the poloidal variations are significant enough that they should be considered in the radiation damage and stress analyses, they do not appear to introduce any new major difficulties into the first wall design.

Figures IX-8 and IX-9 show, respectively, for the reference design the rates of atomic displacements and helium- and hydrogen-production as functions of depth in the blanket for two directions defined by $\chi = 4$ and 176 degrees. These results show that the radial attenuation varies slightly with χ . Since the length of a minor circumference increases with depth in the blanket, the rate of change of poloidal variations of the reaction rates along a minor circumference decreases radially outward in the blanket. Thus, the toroidal geometry effects are more pronounced at the first wall than in the deeper regions of the blanket.

The results presented in this subsection show that the toroidal geometry effects are significant and should be accounted for in the detailed EPR design in all relevant technical areas such as the thermal-hydraulic, stress and radiation damage. At this stage of the EPR design, however, it is desirable to use a representative average over each minor circumference in order to reduce the large effort involved in design analyses of the full toroidal geometry. On the other hand, the use of averaged parameters is not acceptable as a design-basis in many critical areas (e.g. radiation damage effects in the first wall) which are more affected by the maximum than by the average. The neutronics and photonics effects derived from the infinite cylinder

Figure IX-8. Atomic Displacement as a Function of Distance from First Wall in the Reference EPR Design (Reference Source) at $\chi = 4^\circ$ and $\chi = 176^\circ$.

Figure IX-9. Helium and Hydrogen Production as Functions of Distance from First Wall in the Reference EPR Design (Reference Source) at $\chi = 4^\circ$ and $\chi = 176^\circ$.

approximation with a uniform volumetric neutron source distribution is more conservative in this regard. Therefore, this latter case is taken as the reference design base for the remainder of the nuclear design analysis as well as other analyses in this report.

4. Neutronics Effects in the First Wall and Blanket

As discussed in the previous chapter, the first wall will be coated on the inside with a very thin layer of low-Z material. The coating will not affect the neutron and gamma-ray fluxes to any appreciable degree, but it is of interest to compare the nuclear performance for the various coating candidates. Table IX-5 compares two critical parameters (helium and hydrogen production rates) in several low Z materials applied as a coating on the inside of the first wall of EPR reference design. It is noted from this table that the hydrogen production rate is < 2% of the helium production rate. The helium production is highest in boron and B_4C , with > 99% of the helium produced by ^{10}B . (At this high rate of (n,α) reaction, the ^{10}B will be burned up at the rate of 30% per MW-yr/m², with a corresponding reduction in the helium production rate

Table IX-5. Rates of Helium and Hydrogen Production in Several Low-Z Coating Materials

	appm/(MW-yr/m ²)	
	Helium	Hydrogen
Beryllium	3110	51
Boron ^a	70130	534
Carbon	2241	--
BeO	2018	72
Be ₂ C	2820	34
B ₄ C ^a	56550	427

^a Boron with natural isotopic enrichment (19.8% ^{10}B and 80.2% ^{11}B).

in boron and boron carbide.) The high rate of helium production in a boron coating can be significantly reduced by using boron that is largely depleted in ^{10}B . It should be noted that the helium production rate in a boron-containing coating can be reduced by more than an order of magnitude by placing a strong low-energy neutron absorber, e.g. ^6Li , behind the first wall.

Figure IX-10 shows the spatial (radial) distribution of nuclear heating in the first wall and blanket of the reference design for a neutron wall loading of 0.5 MW/m^2 . The maximum nuclear heating is 5.8 W/cm^3 in the first wall and 3.5 W/cm^3 in the blanket and it drops to 0.3 W/cm^3 at the outer boundary of the blanket. The total recoverable energy per fusion reaction, including the $3.5 \text{ MeV } \alpha$, is 18.3 MeV of which $\sim 40\%$ is deposited in the first wall assembly, in the absence of penetrations.

Figure IX-11 shows the atomic displacement in stainless steel as a function of position in the first wall and blanket. A displacement energy of 40 eV was assumed for stainless steel. The spatial distribution of the helium- and hydrogen-production rates in the stainless steel are shown in Figure IX-12. The maximum values in the first wall are $11 \text{ dpa/(MW-yr/m}^2)$ for the atomic displacement, and 530 and $215 \text{ appm/(MW-yr/m}^2)$ for the hydrogen- and helium-production, respectively. The helium and hydrogen production rates decrease about an order of magnitude in 13 cm while the atomic displacements decrease at a much slower rate. The ratio of the rates of helium-production to atomic displacements is also shown in Figure IX-12 with some small variations omitted for clarity of the graph. It is worth noting that across the 4-cm thick first wall assembly the helium production decreases by a factor of ~ 2.5 and the atomic displacements decrease only by a factor of ~ 1.5 , causing the helium-to-dpa ratio to vary by $\sim 60\%$ across the wall. Thus, in addition to being relatively high, all the significant radiation damage indicators exhibit a significant spatial dependence.

Figure IX-10. Spatial Distribution of Nuclear Heating in the EPR First Wall and Blanket for a Neutron Wall Loading of 0.5 MW/m^2 .

Figure IX-11. Spatial Distribution of Atomic Displacement in the EPR First Wall and Blanket.

Figure IX-12. Spatial Distribution of Helium and Hydrogen Production Rates in the EPR First Wall and Blanket. Also shown is the spatial dependence of He-to-dpa ratio.

5. Neutronics Analysis of the Bulk Shield

Figure IX-13 shows the spatial distribution of nuclear heating rates in the outer shield for a neutron wall loading of 0.5 MW/m^2 . The maximum heating rate at the inner structural steel ring is $\sim 0.3 \text{ W/cm}^3$. This value drops across 23 cm of SS and C (1% B) to $\sim 0.04 \text{ W/cm}^3$. As expected, the neutron heating is dominant in graphite while the absorption of secondary gamma-rays contributes the greatest part of the nuclear heating in SS. The nuclear heating in lead mortar exhibits a rather interesting behavior. Because of the presence of a significant amount of boron in lead mortar most of the low energy neutrons transmitted from neighboring regions are absorbed in the first few centimeters of lead mortar with an exothermic $^{10}\text{B} (n, \alpha)$ reaction. This causes a maximum heating rate in lead mortar of $\sim 0.2 \text{ W/cm}^3$ which drops to ~ 0.02 within 8 cm and to $\sim 8 \times 10^{-5} \text{ W/cm}^3$ in an additional 55 cm of lead mortar. In the midregion of lead mortar, where the number of low energy neutrons is small, the gamma heating (mostly from absorption in lead) is comparable with the neutron heating.

The rate of nuclear heating in the midplane in the inner bulk shield is shown in Figure IX-14. The nuclear heating in the SS-innermost region is $\sim 0.2 \text{ W/cm}^3$ but it rises to 1.4 W/cm^3 in B_4C . The minimum nuclear heating in the inner shield is 10^{-4} W/cm^3 . The nuclear heating rate per unit volume in boron carbide is about a factor of 6 higher than in the neighboring stainless steel regions. The thermal conductivity is lower for B_4C than for SS; however, no severe difficulties arise in the design of the heat transfer systems because B_4C can be operated at much higher temperatures than stainless steel. In the absence of penetrations, $\sim 7\%$ of the reactor (nominal) thermal power is generated in the shield. The penetrations cause this fraction to be increased by an additional few percent.

The helium and tritium production in boron carbide is of concern. Table IX-6 presents the rates of helium and tritium production in boron carbide in the reference design. The maximum tritium production in boron carbide is $0.36 \text{ appm}/(\text{MW-yr/m}^2)$. This level of tritium production does

Figure IX-13. Spatial Distribution of Heating Rates in the EPR Outer Shield for a Neutron Wall Loading of 0.5 MW/m^2 .

Figure IX-14. Spatial Distribution of Heating Rates in the EPR Inner Shield for a Neutron Wall Loading of 0.5 MW/m².

Table IX-6. Helium and Tritium Production Rates in Boron Carbide in the
ANL-EPR Inner Shield

Distance From First Wall, cm	appm/(MW-yr/m ²)	
	Helium	Tritium
36	1390	3.6 ^a (-1)
38	508	3.0 (-1)
40	368	2.4 (-1)
51	121	4.8 (-2)
53	55	4.0 (-2)
55	35	3.2 (-2)
67	10	5.7 (-3)
69	4.2	4.8 (-3)
71	2.4	3.9 (-3)
73	1.6	3.1 (-3)
85	0.5	5.3 (-4)
87	0.	4.5 (-4)
89	0.1	3.6 (-4)

^a 3.6 (-1) should read as 3.6×10^{-1} , etc.

not present any serious problems. Moreover, tritium diffusion in boron carbide is expected to be very slow, particularly at the low operating temperature of the shield. The spatial dependence of the helium- and hydrogen-production rates in both the SS and B_4C in the inner shield is presented in Fig. IX-15. The maximum helium- and hydrogen-production rates in SS are 1 and 4 appm/(MW-yr/m²), respectively, and are more than two orders of magnitude less than those in the first wall. About 70% of the hydrogen produced in the B_4C is tritium. The helium production-rate in the innermost layer of B_4C is 1390 appm/(MW-yr/m²) a significantly high value. Thus the use of boron carbide with $\sim 92\%$ theoretical density is planned in order to accommodate the anticipated swelling and to accelerate helium diffusion. The helium production in B_4C decreases rapidly inside the shield as the low energy neutrons are captured in the preceding layers of B_4C .

The outer shield material composition is less efficient in attenuating nuclear radiation than the SS- B_4C composition employed in the inner shield. However, the total attenuation provided by the outer shield is significantly better than that provided by the inner shield because of a much larger shield thickness on the outside. From the viewpoint of magnet protection, the outer shield can be replaced with the same material composition (SS- B_4C) and dimensions used for the inner shield. This would have the advantage of providing more access space out board of the shield if such space were needed for other purposes. However, there are two strong reasons that led to the selection of the reference design. First, the graphite/lead mortar/aluminum combination has no significant long-term activation, in sharp contrast with stainless steel. Since the volume of the outer shield is more than 90% of the total shield volume, this leads to a significant reduction in the total inventory of radioisotopes that require special provision for long-term storage at disposal time. Moreover, it is possible in the current design to permit controlled personnel access into the reactor building within 24 hours after shutdown for repair and inspection. The second reason is based on economic considerations. At present, boron carbide with 95% density costs¹⁴ more than 120 \$/kg. A demand for large quantities (~ 0.5 million kg) could possibly reduce the B_4C price by a factor of 2. Other

Figure IX-15. Spatial Distribution of Helium and Hydrogen Production Rates in the Inner Shield (in the midplane) of the EPR Design.

material costs are typically 2.7, 3, and 3.5 \$/kg for SS, C (1% B), and lead mortar,¹⁵ respectively. Thus, for the reference design, the inner shield costs ~ 4 million dollars and the outer shield costs ~ 6 million dollars with a total material cost of ~ 10 million dollars. If the reference design were changed to include a symmetric shield of SS-B₄C, the material cost would be ~ 30 million dollars.

6. Neutronics Effects in the Toroidal-Field (TF) Coils

The maximum radiation effects in the TF coils of the reference design occur in the inner (vertical) segment at midplane. The neutronics effects in this segment are presented in this subsection.

Figure IX-16 shows the nuclear heating as a function of position within the homogenized composition of the magnet. The maximum nuclear heating is $\sim 2.5 \times 10^{-5}$ W/cm³ which is much less than the Joule losses the conductor is designed for. The heating rate decreases with an exponential attenuation coefficient of ~ 0.13 cm⁻¹. The total nuclear energy deposition in the bobbin and winding of the 16 magnets is 1.5 KW. The refrigeration power requirements will depend greatly on whether this thermal load would be removed at 3 or 4°K, with the lower temperature requiring ~ 1% of the plant electrical power output. The magnitude of nuclear heating, however, is much less than the eddy current heating due to pulsed fields.

One of the most important effects of radiation in the superconducting magnet is the increase in the resistivity of the normal conductor used as the stabilizing material. In previous work,^{1,2} available experimental results from 4°K irradiation were used to derive a correlation between the radiation induced resistivity, ρ_r , and the number of atomic displacements. For copper, this correlation is

$$\rho_r = 3 \times 10^{-7} [1 - \exp(-563d)] \Omega\text{-cm}$$

where d is the number of displacements with an assumed displacement energy for copper of 40 eV. The value of ρ_r calculated from this correlation

Figure IX-16. Spatial Distribution of Nuclear Heating in the Inner Segment of the EPR Toroidal Field Coil (in the midplane) for a Neutron Wall Loading of 0.5 MW/m^2 .

may have a 10 to 20% uncertainty due to the scatter in experimental data, a comparable uncertainty due to the harder neutron spectrum in EPR than in experiments and the uncertainties in the energy dependence of the displacement cross sections. The level of atomic displacement is also affected by uncertainties in the neutron cross sections of materials in the shield. The cumulative effects of these uncertainties have not been evaluated yet. However, a 5% change in the "effective" mean free path of neutrons in the blanket/shield can easily lead to more than a factor of 2 change in the radiation level at the magnet.

Figure IX-17 shows the variations of dpa and ρ_r with depth within the windings of the toroidal-field coils. The maximum ρ_r is $6 \times 10^{-9} \Omega\text{-cm}$ after irradiation for an integral neutron wall loading of 1 MW-yr/m^2 . Thus, the reactor can be operated up to 2.5 MW-yr/m^2 before the design value of $1.5 \times 10^{-8} \Omega\text{-cm}$ allowed in the TF coil conductor is reached. The reactor could be operated continuously at 0.5 MW/m^2 for 5 years before magnet warm-up would be needed. This period can be doubled if operation is only at 50% capacity factor. However, it should be strongly emphasized that the uncertainties in calculating ρ_r are great at present, and a more frequent annealing schedule should be planned in the conceptual design. It can be noticed from Figure IX-17 that ρ_r decreases by an order of magnitude in $\sim 25 \text{ cm}$. Beyond that the change in the conductor width to accommodate the radiation-induced resistivity is very small.

The toroidal-field coils employ a variety of organic thermal and electrical insulators. In the temperature range from 3°K to 4.2°K there is a paucity of irradiation data on the properties of these materials.¹⁶ Extrapolation of data accumulated at higher temperatures¹⁷ led to a choice of aromatic base epoxies for the magnet insulators. The radiation dose at which their mechanical, and perhaps dielectric, properties are seriously degraded is not known at low temperatures, but it may be in the range 10^9 to 10^{10} rads. Figure IX-18 shows the absorbed dose in the aromatic base epoxy ($\text{C}_{10}\text{H}_{13}\text{O}_2$) as a function of position inside the conductor windings of the TF coil. The maximum dose to the epoxy inside the conductor is

Figure IX-17. Atomic Displacements and Radiation-induced Resistivity (ρ_r) in Copper (Stabilizer) as a Function of Depth within the Inner Segment of the TF Coils after Reactor Operation for an Integral Wall Loading of $1 \text{ MW}\cdot\text{yr}/\text{m}^2$.

Figure IX-18. Dose Absorbed in Mylar and Epoxy (with aromatic type curing agent) Insulators as Function of Depth within the Inner Segment of the Toroidal Field Coil.

1.4×10^8 rad/(MW-yr/m²). Thus, the aromatic-based epoxy could probably be operated up to more than 10 MW-yr/m². Although an integral wall loading of 10 MW-yr/m² is greater than a reasonably optimistic estimate of the lifetime for EPR, irradiation experiments at 3° to 4.2°K on organic insulators nevertheless needed to provide a more reliable estimate of the tolerable radiation dose in insulators.

Shown also in Figure IX-18 is the radiation dose to mylar (C₁₀H₈O₄), another possible candidate for insulators. The maximum absorbed dose in mylar is $\sim 9 \times 10^7$ rad/(MW-yr/m²) which is $\sim 50\%$ lower than that in aromatic-based epoxy. However, irradiation experiments on mylar at 77°K showed¹⁶ severe effects on its mechanical properties at 10⁸ rad. Thus the mylar could only be used up to 1 MW-yr/m² in the EPR design.

7. Nuclear Design of Penetrations Shield

EPRs, as well as future tokamak fusion reactors, will require provision for several types of access regions into the plasma chamber through the blanket/shield. Penetrations such as those for vacuum pumping and neutral beam and/or radio frequency heating represent large void regions which greatly enhance neutron and gamma-ray streaming into regions external to the blanket/shield. The effects of these penetrations have not been treated in any previous conceptual design studies; however, the sizes and locations of these penetrations indicate that their impact on the reactor design would be significantly large. This was confirmed by a detailed study of the penetrations in EPR, which also indicated that these penetrations and their special shield must be treated as an integral part of the design.

During the course of this study, the penetrations and their shield were considered in trade-off studies for the EPR conceptual design. Detailed neutronics analyses of various penetrations sizes and geometric orientations, as well as various approaches for shielding against major penetration effects, are presented in Appendix G. In this subsection, only a summary of the gross penetration effects is given and the prominent features of the penetration shield adopted for the reference design are described.

The study focused mainly on the large vacuum ducts required for vacuum pumping and neutral beam injection. These ducts are ~ 0.6 to 1 m² in

cross sectional area and they extend from the first wall (directly visible to the plasma neutrons), radially through the blanket/shield, and outward to the exterior of the TF coils (see Figures II-3, and II-4). Two vacuum pumping ducts are located between each pair of the 16 TF coils, one at the top and one at the bottom of the torus. A neutral beam duct is located between each pair of TF coils, centered about the midplane with its axis almost tangential to the major magnetic axis. The vacuum pumping and neutral beam injection ducts are comparable in size and they share many common features as to radiation streaming from the unshielded ducts. However, the approach for shielding adopted for the reference design is different for the two systems because of the substantial difference in the functional requirements of the evacuation and plasma-heating systems. The two systems are discussed separately below. (In the rest of this subsection, the blanket and bulk shield will be referred to collectively as the bulk shield except in those instances where a distinction needs to be made.)

a. Shielding of the Neutral Beam Ducts

The neutronics analysis was carried out using a three-dimensional geometric model with the Monte Carlo code VIM.⁶ These calculations are inherently machine- and man-time consuming, making a thorough 3-D analysis of the full reactor geometry very costly. Therefore, a somewhat simplified 3-D geometric model which is less costly but incorporates the basic features of the reactor geometry and accounts for all first-order effects of penetrations was developed as follows (see Figure IX-19):

- (1) Since the torus consists of 16 segments that are essentially identical, only one segment needs to be analyzed. The segment is defined as the entire reactor region bounded by two planes which intersect at the poloidal axis (see Figure IX-2) and each plane divides a TF magnet into two symmetric halves. A periodic boundary condition is applied at both planes.
- (2) In order to avoid explicit toroidal calculations on penetration analysis the toroidal curvature was ignored, but the dimensions of the segment were adjusted to produce correct volumes and spacing of regions in the neighborhood of the beam ducts. The effect of toroidal curvature on local fluxes is small as shown

Figure IX-19. Schematic of Geometry Representation for Analysis of Neutral Beam Penetrations and their Shield.

previously in this section. However, the toroidal geometry causes the spacing between the TF magnets to vary from almost zero at the inside to a few meters on the outside. In order to account for the correct relative positions of the penetrations, penetration shield, and magnets, the spacing between the TF magnets at midplane (where the beam ducts are located) was employed. Note that the outward shifting of the magnetic flux surfaces which causes an outward shift of the plasma neutron source strength will increase radiation streaming through the neutral beam ducts compared with the uniform source case.

- (3) Since the exact position of the beam axis relative to the toroidal axis cannot be reproduced in the geometric model discussed above, an inclination angle θ_b (see Figure IX-19) of 35° was employed. Studies presented in Appendix G show that the results presented in this section are not overly sensitive to this assumption because of many counteracting and compensating effects when θ_b is varied.

Figure IX-19 shows the geometric representation in the neighborhood of the beam ducts in the reference design. An orthogonal coordinate system (x, y, z) is shown also in the figure. The z axis is taken along the toroidal magnetic axis, x is the minor parameter in the midplane, and y represents the elevation from the midplane. The bulk shield is essentially the same as that of the outer blanket/shield system described in Table IX-3. The beam duct is represented as a cylindrical void region with 85 cm diameter. The parameter z_m shown in the figure is 45 cm and represents a half-width of a TF magnet. (A cross-section of a TF magnet in the x-y plane has a D-shape.)

Table IX-7 shows the neutron fluxes normalized to a neutron wall loading of 1 MW/m^2 at several key locations for three cases. Case 1 represents a design similar to that in Figure IX-19 but in the complete absence of beam ducts, i.e. the bulk shield is solid and continuous everywhere. Case 2 is for the design in the presence of the beam ducts but without any provision for penetration shielding. Case 3 is to be described shortly. The neutron fluxes, ϕ 's, given in Table IX-7 represent averages over geometric

Table IX-7. Total Neutron Fluxes in the Toroidal-Field Coils and Beam Ducts (normalized to a neutron wall loading of 1 MW/m^2) for three cases: (1) no penetrations, (2) unshielded neutral beam ducts, and (3) partial shield of neutral beam duct.

	Case 1	Case 2	Case 3
Code Number	MC 701	MC 702	MC 704
Beam Duct Diameter	0.0	85 cm	85 cm
Penetration Shield thickness (50% SS + 50% B_4C)	--	0.0	50 cm
ϕ_{18}	1.5 (9) ^a ($\pm 25\%$)	9.4 (11) ($\pm 11\%$)	6.1 (10) ($\pm 25\%$)
ϕ_{19}	1.1 (9) ($\pm 26\%$)	8.2 (11) ($\pm 15\%$)	4.0 (10) ($\pm 27\%$)
ϕ_{28}	1.5 (9) ($\pm 25\%$)	1.5 (11) ($\pm 14\%$)	4.8 (10) ($\pm 31\%$)
ϕ_{29}	1.1 (9) ($\pm 26\%$)	8.5 (11) ($\pm 20\%$)	3.2 (10) ($\pm 31\%$)
ϕ_{25}	1.5 (9) ($\pm 25\%$)	1.3 (13) ($\pm 15\%$)	1.1 (13) ($\pm 11\%$)
Neutron Leakage per DT neutron	7.7 (-6) ($\pm 17\%$)	1.3 (-2) ($\pm 5\%$)	4.2 (-3) ($\pm 9\%$)

ϕ_{18} = average flux in the TFC region $[(r = r_m, r_m + 5), (y = -100, 100)]$

ϕ_{19} = average flux in the TFC region $[(r = r_m + 5, r_m + 10), (y = -100, 100)]$

ϕ_{28} = average flux within the first 5 cm of the TFC

ϕ_{29} = average flux within the second 5 cm of the TFC

ϕ_{25} = neutron flux in the beam duct at a location in the immediate exterior of the TF coil.

^a 1.5 (9) should be read 1.5×10^9 , etc.

regions whose numbers are used as subscripts for the ϕ 's. Regions 18, 19, 28 and 29 are inside the TF coils. Regions 18 and 28 constitute the first 5-cm thick zone in the TF coils with region 18 extending from $y = -100$ to $+100$ cm and region 28 spanning the rest of the y values. Thus, region 18 is closer to the beam duct and is more affected by radiation streaming than region 28. Regions 19 and 29 correspond, respectively, to regions 18 and 28 except that they represent the second 5-cm depth in the TF magnets, i.e. $r = r_m + 5$ to $r_m + 10$ cm where r_m is the innermost radius of the magnet ($r = \sqrt{x^2 + y^2}$). The percentage value in parentheses below each flux or leakage value in Table IX-7 is the statistical error as estimated by VIM. Region 25 represents a disc whose radius is equal to that of the beam duct and is 5 cm thick with the center of the disc located at the intersection of the beam axis with the outermost toroidal surface which touches the outer corners of the TF magnet as shown in Figure IX-19.

The values of the neutron fluxes for Case 1 represent a maximum radiation level at the TF coils that meets the magnet protection criteria defined earlier in this section. The presence of the unshielded beam duct causes the radiation level at the TF coils to increase by a factor > 600 . It should be pointed out that this factor is a strong function of the design, as shown for several cases in Appendix G. For example, if the graphite and lead mortar in the bulk shield are replaced with a much better attenuating composition, the radiation levels at the TF coils will drop substantially for Case 1 but only slightly in the presence of the unshielded void ducts. Note also that the void penetrations cause a significant fraction of the high energy neutrons to reach the TF coils and the exterior regions. In any event an efficient shielding scheme is always required to protect against radiation streaming from void penetrations.

There are several shielding schemes to protect reactor components external to the bulk shield from enhanced radiation streaming caused by large-size penetrations. These are:

- (1) Movable Shield Plug: If the functional requirements of a penetration permit that the penetration be closed during the plasma burn, then a shield plug can be moved (mechanically) at the beginning of each pulse to close completely the penetration region embedded in the bulk shield.

- (2) Local Component Shield: Reactor components affected by radiation streaming can be surrounded by a shield capable of reducing the radiation level in the component to a tolerable level.
- (3) Bulk Shield Extension: The bulk shield can be extended into and in between the TF coils, and on to the outside as necessary.
- (4) Local Penetration Shield: Each penetration is surrounded as it emerges from the bulk shield by an appropriate shield sufficient to reduce the radiation level at the TF coils and all other equipment located in the reactor building to a permissible level.

The shield plug is the easiest to define in terms of nuclear requirements, since it needs to have the same dimensions as the penetration itself and it can be of a composition similar to that of the bulk shield. The most important advantage of the movable shield plug in contrast to all other approaches is that it completely eliminates the penetration effects and restores the effectiveness of the bulk shield. It also requires the smallest inventory of shielding materials of the four options. The movable shield plug approach is not practical, however, for the neutral beam ducts for several reasons.

- (1) The tokamak EPR may have to be operated in a beam-driven mode, either to offset subignition confinement or to prolong burn pulses. In this case, the beam duct cannot be closed at the time of plasma burn.
- (2) During the plasma heating phase, the neutral beam is injected for a finite period of time (~ 5 sec in the reference design) and the fusion power increases steadily. The total energy of the neutrons emitted during the beam injection phase depends on the characteristics of the design but it is generally significant and represents $\sim 5\%$ of the total fusion energy during the burn phase in the EPR reference design. Thus radiation streaming during the plasma heating phase when the shield plug cannot be used is high and cannot be tolerated. The same problem arises when the beam is used to extend the burn pulse.

- (3) The neutral beam ducts have to provide a straight-through path from the neutralizer to the plasma chamber. Thus, the mechanical movements of the shield plug to close the neutral beam duct will involve rotational as well as displacement movements. This will involve time delay in closing the beam duct with the plasma already in the ignition phase. Moreover, complicated patterns of movements for placing the shield plug inside the beam duct will magnify the risk of failure that will always be associated with mechanical movements of massive weights on a short time scale.

The local component shield approach can be rejected as the primary approach on the grounds of the large volumes of reactor components that have to be shielded. This approach, however, is a useful supplement for some small-size equipment that are overly sensitive to nuclear radiation.

The simple extension of the bulk shield is not efficient, as demonstrated in Appendix G. It requires much larger inventory of materials than that needed for the local penetration shield for the same efficiency of radiation attenuation.

The local penetration shield is adopted as the principal approach for shielding against the radiation streaming effects of the neutral beam ducts in the reference design. As shown in Figure IX-19, each neutral beam duct is completely surrounded as it emerges from the bulk shield by an effective combination of shielding materials. The dimensions and material composition for this local penetration shield are examined next.

Case 3 in Table IX-7 is the same as Case 2 discussed previously, but a partial penetration shield is provided. This penetration shield consists of 50% SS + 50% B₄C. It is a cylindrical region that completely surrounds the cylindrical beam duct and excludes the bulk shield and the TF coils wherever an interface occurs. The outer diameter of the penetration shield in Case 3 is 187 cm, providing a net shield thickness of 50 cm. In Case 3 particles crossing the outer toroidal surface touching the outermost corners of the TF coils were counted in the leakage term shown in Table IX-7. Comparing cases 3 and 1 in Table IX-7 shows that the 50-cm thick penetration shield is not sufficient, but it brings the maximum neutron fluxes in the TF coils

down to within a factor of 40 from the maximum permissible flux at the TF coils, in addition to reducing the average energy of the neutrons. A characteristic of the Monte Carlo method is that as the shield thickness is increased, i.e. the shield attenuation is improved, the cost of determining the radiation level external to the shield increases dramatically if the same statistical accuracy is to be maintained. As shown in Appendix G, reasonable results can be obtained if the radiation field at the outer surface of the partial penetration shield is used as input to deterministic S_n calculations to define the additional shielding requirements. This approach shows that the penetration shield thickness has to be increased to ~ 75 cm in order to reduce the maximum neutron flux in the TF coils to the tolerable level.

Thus, the penetration shield for the neutral beam duct is specified as 75 cm-thick, the first 65 cm is a 50% SS + 50% B_4C composition, followed by 5 cm lead and an additional 5 cm aluminum alloy. This shield surrounds the beam duct and extends from the outer surface of the bulk shield to the outer surface that touches the outermost corners of the TF coils. This shield is sufficient to protect the TF coils but not the reactor components and auxiliary systems located in the reactor building external to the TF coils, as will be discussed shortly. The SS- B_4C mixture is relatively expensive and has the disadvantage of strong neutron-induced activation in the stainless steel. This composition is used here, however, because of its high attenuation efficiency in order to conserve space and keep the necessary extension of the D-shaped TF coils to a moderate level. The outer regions of lead and aluminum are used to keep the biological dose in the reactor building after shutdown to a specified level, as discussed in Chapter XIV. Future work on these penetration problems should involve, among other things, an additional effort to optimize the geometric shape of the penetration shield.

Two important parameters in Table IX-7 are the neutron flux in region 25, ϕ_{25} , and the neutron leakage per D-T neutron, L_0 , for the three cases described earlier (see also Figure IX-19). The presence of the neutral beam duct causes the neutron flux at the beam axis as it leaves the outermost surface enclosing the TF magnets to increase from 1.5×10^9 to 1.3×10^{13} n/cm² sec. for a neutron wall loading of 1 MW/m². The presence

of the beam duct shield does not significantly reduce the magnitude of the neutron flux along the beam duct, although the average neutron energy is somewhat reduced. It should be noted that although neutrons traveling in the beam duct can strike the shield on the sides of the duct, many neutrons also scatter into the duct where they can travel for a long path before they make another collision on the side shield further down the duct. Thus, the population of the neutrons in the duct remains significantly high, which implies the following: (1) The penetration shield has to be extended for long distances to protect components and equipment located on the sides of the beam duct. The thickness can be reduced, however, as one moves radially out parallel to the axis of the beam duct because of the geometric attenuation and the softening of the neutron spectrum; (2) A significant fraction of the neutrons will travel down the beam duct and strike the objects at the outer end of this duct. In this case, the objects are the components located inside the chamber of the beam injector. The chamber of a beam injector (see Figure V-20) is large in size and is typically 1000 m³ in volume and 600 m² in surface area. Besides possible initiation of hot spots on the structural walls of this chamber, the components located inside the chamber can be affected by nuclear radiation, e.g. the cryosorption panels and the bending magnets. The analyses of neutrons and photons inside the large-size neutral injector chamber were not carried out in detail because of the high computational cost involved. However, it is believed that the potential problems involved here must be investigated. Approximate calculations based on the total neutron leakage to the neutral injector chambers show that the nuclear heating at the cryosorption panel can possibly exceed 0.01 W/cm³ and that the absorbed dose in the epoxy insulator located in the bending magnet may be $> 10^{11}$ rad/(MW-yr/m²) unless additional precautions are taken.

The neutronics effects in the beam duct wall and in the blanket and shield regions adjacent to the beam ducts have also been considered. Figure IX-20 shows the total neutron flux as a function of depth within the blanket and bulk shield for two cases. The first case is in the absence of penetration effects, i.e. in regions far away from the penetration near the lower boundary of Figure IX-19. The second case is for the flux at a surface surrounding the beam duct and is located 5 cm away. In this latter case, the horizontal

Figure IX-20. Total Neutron Flux (normalized to a neutron wall loading of 1 MW/m^2) as a Function of Depth within the Blanket and Bulk Shield at Two Locations; a-a radial line far removed from penetration effects, and b-a surface parallel to the walls of the neutral beam duct and 5 cm away.

axis represents the distance $x-x_w$ in Figure IX-19 with $x_w = 240$ cm. From this figure it can be seen that the beam duct serves the role of a secondary neutron source which redistributes the neutrons and increases their population significantly in the deeper regions of the blanket and bulk shield. At an 80 cm depth into the blanket/shield the total neutron flux in the vicinity of the penetrations is about three orders of magnitude higher than at points located on the same toroidal surface but ~ 100 cm away from the beam walls.

Thus, even for an axisymmetric blanket/shield the presence of penetrations causes spatial variations in the toroidal direction that are stronger near the penetrations and which increase radially outward. These effects must be accounted for in the thermal-hydraulic, stress, and radiation damage analyses. Figure IX-21 shows the neutron heating in the water coolant as a function of distance from the first wall at three concentric cylindrical surfaces. The first surface, a, is the wall of the beam duct; the second surface, b, is located 10 cm outward from the beam duct wall; and the third surface, c, is 30 cm away from the beam wall. The neutron heating in the water coolant at the inner edge (at the first wall) of the beam duct wall is ~ 5 W/cm³ for a neutron wall loading of 0.5 MW/m². This value drops by less than a factor of 10 along the entire length of the beam wall inside the blanket-bulk shield. For comparison, the neutron heating in the water coolant in regions far removed from the beam ducts varies from 5 W/cm³ at the 1st wall to 0.4 W/cm³ at the outer perimeter of the blanket to $\sim 10^{-4}$ W/cm³ near the outer surface of the bulk shield. Figure IX-21 shows that the heating rates in regions surrounding the penetration are significantly increased by penetration-assisted streaming of nuclear radiation. Thus, the ~ 20 cm thick region in the bulk shielding surrounding the beam duct will require an efficient heat removal system similar to that of the blanket. Furthermore, the material composition of this region cannot be a low-temperature material such as lead mortar and has to be constructed of materials that can withstand high heating conditions such as those employed in the blanket. It should be noted that the neutron population is increased near the outer region of the bulk shield (in the absence of the penetration shield) by those neutrons streaming out of the beam duct and backscattered into the bulk shield by external components. This effect is also shown

Figure IX- Neutron Heating in the Water Coolant as a Function of Depth in the Blanket/Shield for Three Locations with Respect to the Neutral Beam Duct: a) at the wall of the beam duct, b) at 10 cm from the wall of the beam duct, and c) at 30 cm from the wall of the beam duct. (Results normalized to 0.5 MW/m² neutron wall loading.)

in Figure IX-21. We should note here that results in Figure IX-22, as well as those in Figure IX-22 were derived from a combination of 3-D Monte Carlo and 2-D S_n calculations in order to obtain detailed spatial distributions.

Figure IX-22 is similar to Figure IX-21 except that the atomic displacements, dpa, in stainless steel is the response rate of interest here. The spatial variation of the dpa exhibits essentially the same general behavior discussed above for the water heating. Of particular concern here, however, is the radiation damage to the walls of the neutral beam duct as well as the walls of any vacuum duct that cannot be completely closed during the plasma burn. These walls must satisfy the same requirements as those on the first wall, i.e. they must guarantee structural integrity and maintain the vacuum integrity of the plasma chamber. The radiation damage indicators, dpa and gas production rates, near the inner end of the duct walls are essentially the same as in the first wall. Moreover, they drop along the duct wall by $\sim 1\%$ per centimeter. In addition to the considerable radiation damage at the inner part of the duct walls, the variations in the dpa and gas production rate, as well as the fluctuations in the temperature along the wall, will aggravate the radiation damage and can create relatively large stresses in this structural member. The inner surfaces of these walls may also have to be coated with a low-Z material to minimize plasma contamination, as described in the previous chapter.

b. Shielding of the Evacuation Ducts

In the reference EPR design, there are 32 evacuation (vacuum pumping) ducts that are approximately cylindrical, each with an aperture at the first wall 95 cm in diameter. They are located at the top and bottom of the torus, each is equally spaced between a pair of TF coils.

These evacuation ducts are larger in size and number than the neutral beam ducts. Radiation streaming through these evacuation ducts was studied and, as shown in Appendix G, it is similar in many respects to the case of the neutral beams, except for some differences due to size and orientation of the ducts. Although the spacing between the TF coils at the top and bottom of the torus is tight, the functional requirements of the evacuation

Figure IX-22. Atomic Displacement in Stainless Steel as a Function of Depth in the Blanket/Shield for Three Locations with Respect to the Neutral Beam Duct: a-at the wall of the beam duct, b-at 10 cm from the wall of the beam duct, and c-at 30 cm from the wall of the beam duct.

ducts permit moderate bends in the duct, which would make the design of an adequate exterior penetration shield feasible. This shield, however, would cost ~ 10 million dollars for the reference design and would occupy most of the space on the top and bottom of the torus as well as the space in between the TF coils. The exterior duct shield would introduce many other disadvantages similar to those discussed for the neutral beam penetration shield.

Since the evacuation ducts can be completely closed during the plasma burn, the concept of a movable shield plug was adopted as the principal design option. The movable shield plug can, in principle, be constructed with materials and with dimensions such that when it is moved to close an evacuation duct the blanket and bulk shield become continuous. However, there must be a finite clearance between the shield plug and the evacuation duct which can provide possible streaming paths for radiations. Therefore, the shielding plug cross section should be varied in steps with corresponding change in the evacuation duct as shown in Figure IX-23. The engineering aspects of this shield plug are discussed later in Section D of this chapter.

B. Blanket and Shield Materials

The 28 cm blanket region receives most of the remaining neutron and gamma energy not absorbed by the first wall. For the most part, the properties and requirements of the blanket material are the same as those of the first wall discussed in Chapter VIII. Therefore, the same material, annealed 316 stainless steel, was chosen for the blanket. The radiation damage level in the blanket adjacent to the first wall is ~ 1.7 dpa/year (at a 0.5 MW/m^2 wall loading and a 50% duty factor) and drops by a factor of two every ~ 7 cm going through the blanket. Operating temperatures in the load bearing portions of the blanket are, like the first wall temperatures, restricted to $\leq 500^\circ\text{C}$, but may be allowed to rise above this level in non-structural components. In general, the less severe radiation environment of the blanket will mean that property changes will be less than in the first wall. After 10 years at a wall loading of 0.5 MW/m^2 and a 50% capacity factor, the swelling in the blanket adjacent to the first wall is expected to remain below 2% (see Figure E-9, Appendix E), the uniform elongation will drop to $\sim 3\%$ (see Figure E-1, Appendix E), and the yield

strength will increase to ~ 75 ksi (see Figure E-3, Appendix E). As the neutron radiation is attenuated through the blanket, the swelling will be reduced to zero after a few cm, and the tensile properties will approach those of unirradiated material ($\sim 25\%$ uniform elongation and ~ 17 ksi yield strength at 500°C). The effect of creep and fatigue will be less than that in the first wall since the blanket is not exposed to the surface radiation from the plasma and will not undergo the large thermal cycling of the first wall. Although helium production rates will still be high in the first few centimeters, helium embrittlement should not be excessive at temperatures below 500°C . There may be problems associated with coolant corrosion, ferrite formation and weld integrity as discussed in Section VIII-B, and Appendix E, but further analysis will be required before the severity of these problems can be determined.

The materials to be used in the EPR shield are shown in Tables IX-2 and IX-3. The shield will receive $\sim 7\%$ of the total fusion energy produced in EPR. The inner shield is composed of alternating layers of stainless steel and boron carbide as shown in Table IX-2, and the outer shield is composed of layers of steel, graphite, lead mortar and aluminum as shown in Table IX-3. In these two regions, no degradation of the bulk properties of stainless steel is expected. Type 304 stainless steel was chosen for use in the inner and outer shield based on economic considerations. The boron carbide located in the inner shield is a brittle material with moderate tensile strength and high compressive strength (see Appendix E). The major effect of radiation on boron carbide is the buildup of helium from (n,α) reactions, which can induce swelling and cracking if present in high concentrations. Neutron irradiation can also substantially reduce the thermal and electrical conductivity. The degree to which radiation affects the bulk properties depends to a large extent on the amount of porosity present in the unirradiated material. The first layer of boron carbide in the inner shield will be the most seriously affected by the neutron irradiation. The first few cm of boron carbide will produce ~ 3500 appm of helium during a 10 year lifetime, but helium production will fall off rapidly past this point, as shown in Table IX-6. This amount of helium is not expected to induce excessive swelling or cracking if a sufficient porosity exists to accommodate the gas. Helium escaping from the boron carbide should be vented to prevent buildup of gas pressure within the shield. The change

in thermal conductivity with radiation will have to be factored into the shield cooling design. The properties of graphite, like boron carbide, depend to a large extent on the porosity which exists in the material. In general, graphite is a brittle material; but, depending on the manufacturing process, its mechanical properties and its response to radiation can vary over a wide range. Irradiation of graphite causes an initial densification followed by a turnaround and rapid expansion. For the conditions expected in EPR, the graphite will densify rather than swell. It is expected that the volume change of graphite due to irradiation can be minimized by a suitable choice of material and should not present a problem. Helium production in the first few cm of the graphite-1% B will reach ~ 770 appm after a 10 year lifetime. As with boron carbide, porosity and venting considerations should be factored into the shield design to accommodate the helium. The materials lying past the first layer of boron carbide in the inner shield and graphite in the outer shield will receive a relatively small neutron fluence, and the bulk properties should not be adversely affected. The lead mortar and aluminum in the outer shield must be operated at temperatures below 100°C, since the lead mortar will begin to break down at temperatures above 150°C.

An insulating material is required to prevent electrical conduction between the blocks that make up the blanket and shield (see Section C of this chapter). Ceramic insulation in the form of a thin coating appears to be a suitable choice to meet the high temperature and radiation requirements. These requirements are similar to those of the first wall current breakers discussed in Chapter VIII. The material should have adequate electrical insulating properties and must adhere to the stainless steel substrate under all operating conditions. Many materials can meet the electrical requirements since only low voltage insulation is required. However, the candidate material should ideally match the thermophysical and radiation swelling properties of the stainless steel in order to minimize stresses at the coated interfaces. High stresses could lead to cracking and spalling of the coating. The ceramic oxides such as Al_2O_3 , Cr_2O_3 , Y_2O_3 , and MgO are believed to be suitable candidate materials. Further effort is required to determine the optimum material and coating application process.

C. Pulsed Magnetic Field Requirements and Torque Effects on Blanket/Shield Design

The time varying magnetic fields from the equilibrium coils and from the plasma currents cause eddy currents in the stainless steel and other electrically conductive materials that make up the blanket and shield. These eddy currents in turn produce magnetic fields which interfere with the plasma equilibrium and which can interact with the toroidal field to create large forces and torques within the blanket and shield. Results of the studies of these eddy current effects, reported in Chapter IV and in Appendix F, show that in the case of a continuous (unsegmented) blanket and shield system having the materials disposition currently considered for the ANL-EPR, the fields induced in the blanket/shield regions by the applied equilibrium field are significant (comparable to the applied field itself). In fact, the induced fields tend to introduce additional currents within the blanket/shield regions which in turn produce secondary fields of sufficient strength to cancel a substantial fraction of the applied field. Further study (Appendix F) showed that by segmenting the blanket and shield into blocks that were electrically insulated from one another, these eddy current effects can be reduced to tolerable levels. The general finding of the studies described in Appendix F was that the limiting criteria which determine the number, size, and, shape of the segments are set by the requirements for establishing the equilibrium field in the plasma with acceptably small time and phase delays; i.e. the number of blanket/shield pieces is determined from field penetration considerations, not torque effects. In any case, it was considered important to evaluate the torque effects to demonstrate that the restraint requirements could be met.

The torques arising from the induced fields tend to rotate each block about the axis perpendicular to the toroidal and equilibrium field axis. These torques exist while the equilibrium field is changing, and reverse direction (by 180°) in going from ramp-up to ramp-down conditions. The peak torque, N_r , on a block due to the interaction between the toroidal field B_t and the eddy currents from a time-varying vertical field, B_o , is shown (in Appendix F) to be

$$N_r = 16 \dot{B}_o \sigma B_t a^3 b^3 c / 9 (a^2 + b^2) , \quad (IX-1)$$

where σ is the electrical conductivity, $\dot{B}_0$ designates the time derivative of B_0 , and $2a$, $2b$, and $2c$ are the dimensions of the block in the radial, toroidal, and vertical directions, respectively. Similarly, if the field B_0 is in the radial direction, the torque N_z is given by

$$N_z = 16 \dot{B}_0 \sigma B_t a^3 b^3 c / 9 (a^2 + b^2). \quad (IX-2)$$

Results of the application of Eqs. IX-1 and IX-2 to some typical EPR blanket and shield blocks are given in Table IX-8. The calculations were made by treating the block as a solid chunk of stainless steel with $\sigma = 0.74 \times 10^{-10} \Omega\text{-m}$ and the peak value of $\dot{B}_0 = B_0/2$ sec. The letter designations for the blocks indicate their position with respect to the circumference of the torus and correspond to the lettering system in Figure IX-24.

Since most of the shield blocks are in fact composed mostly of lead mortar and other materials less conductive than stainless steel, the actual shield block torques would be less than those indicated in Table IX-8. Similarly, the use of large volume fractions of ceramic or molten salt type breeding materials in blanket test modules would tend to reduce the indicated blanket block torques as well. In any case, support against the torques listed in Table IX-8 appears to be achievable with reasonably sized restraining hardware.

Table IX-8. Torques on Typical Blanket and Shield Blocks

	Shield Blocks ^a			Blanket Blocks ^a		
	G	H	N	W	X	Y
a(m)	0.5	0.5	0.5	0.14	1.2	1.2
b(m)	0.5	0.75	1.0	0.75	1.00	0.75
c(m)	0.5	1.25	0.5	1.25	0.14	0.14
B (T)	2.7	8.5	4.1	6.8	5.1	3.4
B _{oz} (T)	0.57	0.42	0.28	0.42	0.03	0.50
B _{or} (T)	0.09	0.12	0.49	0.12	0.40	0.40
N _r (10 ³ ft lb)	2.1	253.1	50.1	6.	14.1	77.
N _z (10 ³ ft lb)	3.1	176.1	89.1	40.1	125.1	34.1

^a See Figure IX-24 for location.

Figure IX-24. EPR Blanket/Shield Arrangement Circular Option

Since the blanket/shield region will have to be broken into modular units for assembly purposes anyway, the concept of insulated segmentation is not incompatible with a practical assembly scenario. As a result of these calculations, a segmented blanket/shield design was developed along the lines described in the following section.

D. Mechanical Design Description of the Blanket/Shield System

The mechanical design of the blanket and shield regions of the EPR is based largely on the technical constraints that arise from results in the preceding sections of this chapter. The selection and disposition of materials derives from the nuclear performance requirements and the materials property limitations imposed by the high radiation and elevated temperature environment. Both the blanket and the shield are divided into several hundred modular block-like units in order to satisfy magnetic field penetration requirements. The structural support for these block-like units provides restraint against displacement forces associated with (1) the weights of the blocks and (2) the torques on the blocks imposed by the eddy current interactions with the toroidal field.

In addition to satisfying these requirements, every effort was made to develop design concepts that permitted (1) assembly with reasonable tolerances, (2) a reasonable approach to handling and repair without major reactor disassembly, (3) thermal energy removal under conditions adaptable to a conventional power cycle, (4) ease of modification to incorporate experimental test modules, and (5) economic manufacturing processes. The selection of pressurized water as the blanket coolant afforded many advantages not attainable with pressurized helium; namely, low void fractions, small supply and return lines and manifolds, negligible pumping power, and sufficient adjustability to accommodate a range of operating conditions. Also considered in the mechanical design were the general features of the special shielding shrouds required around the major penetrations to compensate for nuclear radiation streaming emanating from the open ducts.

Two types of blanket/shield arrangements have evolved for the EPR: (1) a close-fitting contoured assembly as shown in Figure IX-23, and (2) a slab-type assembly as shown in Figure IX-25. Although the contoured assembly is taken as the reference design for the blanket and shield, both concepts have been explored and analyzed in parallel.

Figure IX-25. EPR Alternate Slab-Type Blanket/Shield Arrangement

1. Description of the Reference Blanket Design

The blanket structure for the reference design is made up of 28-cm-thick stainless steel blocks which cover regions of the first wall periphery ranging from 1 to 2.5 m² (see Figures IX-23 and IX-24). Each block is fabricated with a multiplicity of steps on all side faces to minimize line-of-sight radiation streaming between blocks and to augment the keying of adjoining blocks. Although the thermally induced dimensional changes in these blocks during heat up to reactor operating temperature remain to be examined, it is anticipated that the step configurations and shapes can be tailored to the deformations of each block in such a way that the interfaces between blocks are tightly sealed at operating conditions.

In all, 17 blanket blocks circumscribe the first wall in each of the 16 vessel segments, giving a total of 272 blanket blocks. Support of the blanket blocks is made through the shield blocks in the lower vertical midplane of the reactor and by a bolted arch support above the upper blanket assembly as shown in Figure IX-24. The weight of the blanket is transmitted through roller pads to the shield blocks to compensate for the differential thermal expansion anticipated during blanket heat-up to operating temperature.

The size of the blanket blocks is determined mainly by the requirements for equilibrium field penetration and assembly maneuvering space. The amount of subdivision needed to permit satisfactory equilibrium field penetration was obtained from the analyses presented in section C of this chapter. Thereafter, a tractable (from a mechanical viewpoint) segmented design was developed and the torques produced by the eddy currents were evaluated for each block. The design was considered to be acceptable when no torques exceeded 3×10^5 ft. lbs. The restraint required to handle torques of this magnitude is provided by the stepped keyways, support bolting, and sets of shear pins as needed. (None of the required shear pins exceeds 15 cm in diameter.) Prior to assembly, the outer surfaces of each blanket block are covered with a non-conductive coating to maximize the interblock electrical resistance. (This coating procedure is paramount to the entire segmented blanket concept described herein, in that the field penetration and torque limiting features cannot be realized without it.) A

preparation involving either alumina, silicon carbide, or a related refractory insulator will be used in this application, as described in Appendix E.

The blanket blocks are cooled with a pressurized water circuit operating at temperatures up to 310°C and a maximum operating pressure of 2000 psig (the design limit pressure is 2400 psig). The water is carried by a network of drilled channels within each block. The channels are arranged in layered rows paralleling the magnetic axis as shown in Figure IX-26. The rows are grouped into four radial zones. Channels within each zone are connected in series to a common inlet and outlet. Moving in a direction radially outward from the first wall, the first and second zones are cooled independently, while the third and fourth zones are connected in series. The zones within each block share common inlet and outlet manifolds that are built into the block. Two lead lines (~ 2 cm inside diameter) form the supply and return ducts that connect with the corresponding manifolds on each block. The size and number of channels in each zone of a particular blanket block are determined by the thermal-hydraulic response of the block. Calculations for a typical blanket block are given in section E of this chapter.

Fabrication of the blanket blocks described in this section appears to present no major technical problem; however, there is a clear need to analyze in much greater detail the economic factors associated with production of several hundred such blocks. In simplest terms, the fabrication of a single block would involve casting and shaping the stainless steel billet; gun boring or possibly casting the cooling channels; welding the interchannel connectors into place; preparing the manifold connections; installing instrumentation probes, bolt holes, etc.; and applying the coatings.

There are also four experimental ports, each 1.5 m in diameter, included in the reference design. These ports are intended to accommodate (1) essential early verifications of nuclear performance, (2) as yet unspecified blanket diagnostic probes, and (3) experimentation. They also serve as access penetrations for remote inspection and maintenance of the vacuum vessel interior. Furthermore, because of the independent nature of the standard blanket blocks it is quite conceivable that in later

Figure IX-26. Typical Blanket Block

stages of operation, one or more of these blocks could be replaced with a variety of experimental blanket assemblies to test, for example, tritium breeding, helium cooling, or liquid metal interfacing.

2. Description of the Slab-type Blanket Design

The alternate blanket/shield design shown in Figure IX-25 depicts a slab-type configuration having 10 blanket pieces per vessel segment or 160 pieces in all. This concept has the advantages of less costly fabrication, a simplified support structure, improved access for coolant manifold, and reduced complexity associated with assembly and replacement. It does however make less efficient use of the space available within the toroidal field coils and as a result requires a larger materials inventory than the reference design. Considerations related to equilibrium field penetration, torque restraint, coolant system features, experimental modules, and access have been evaluated for the slab and are found to be much the same as those described for the reference design.

3. Description of the Reference Design Shield System

The EPR magnet shield system consists of the bulk shield blocks depicted in Figure IX-23 and the specially contoured shielding shrouds for the penetrations. The bulk shield surrounds the blanket and consists of two distinct regions, an inner shield and an outer shield. (The materials compositions of the inner and outer shield regions are given in Table IX-2 and IX-3, respectively.) The shield region surrounding one of the 16 vacuum vessel segments consists of a single inner shield block and 25 outer shield blocks, assembled as shown in Figure IX-24. There are 416 shield blocks in all, their degree of subdivision again being determined by equilibrium field penetration, assembly and torque limiting considerations. Each block has its own independent water cooling system with a single supply and a single discharge line. The performance features of the shield cooling system are described in section E of this chapter.

Each shield block is a self-contained (canned) unit. Moving in a direction radially outward from the blanket, a typical outer shield block consists of a 3 cm layer of stainless steel, a 15 cm region of graphite containing ~ 1% boron, 5 cm of stainless steel, 60 cm of lead mortar, and 9 cm of aluminum. Like the blanket blocks, all shield blocks are

stepped and keyed on the four sides that adjoin other shield blocks to minimize radiation streaming and to augment alignment and support. Stainless steel coolant lines are located along the surfaces of the stainless steel layers and are interspersed within the bulk of the graphite and lead mortar slabs. Cooling requirements generally depend on the location of a given block and are determined by considerations of thermal expansion effects, dehydration limitations on the lead mortar, and the degree of insulation required to reduce heat input to the magnet cryostats. An analysis of the thermal performance of a typical shield block is presented in section E of this chapter. With the exception of regions in the vicinity of penetrations (like the neutral beam ducts), there is little incentive for trying to recover sensible heat from the shield.

The inner shield block (of which there is one per reactor segment or 16 in all) covers an arc of $\sim 50^\circ$ along the inside edge of the toroidal field coil as shown in Figure IX-23. This block is designed as a single piece, a relatively permanent part of the blanket/shield system, in order to facilitate assembly, coolant manifolding in a limited access region, and subsequent handling at end of life. It is composed of alternating layers of stainless steel and B_4C as shown in Figure IX-1 and Table IX-2. These materials are enclosed in a stainless steel can with water-carrying coolant tubes placed along the B_4C /stainless steel interfaces and within the B_4C where needed.

Support of the shield blocks is achieved through the strutted frame shown in Figure IX-24. Blocks A and B in Figure IX-24 rest on the base of the support frame and are fastened together with blocks C, E, and F to form a structural arch which in turn provides the major support foundation for the blanket blocks and the first wall assembly. Blocks C and F have five additional column supports to the base of the frame which carry most of the weight of the upper shield blocks. The entire shield block system is joined and keyed together to form a rigid load-bearing and torque restraining structure. As in the case of the blanket blocks, the shield blocks are electrically insulated from one another by using thin, nonconductive interface coatings, in order to limit impedances to equilibrium field penetration and to eddy current torques.

All outer shield blocks are outfitted with access ports which accommodate coolant and instrumentation lead lines to the blanket blocks and first wall assembly.

4. Penetration Shield System

In addition to the bulk shield blocks described above, several special radiation shielding geometrical arrangements for the major penetrations have been analyzed. These major penetrations include the ducts for vacuum pumping, neutral beam injection, and radio frequency heating.

A movable shield plug is used for each vacuum port as shown in Figure IX-23. The entire plug is pneumatically operated, opening during the pump down phase, and closing during the plasma burn cycle. The shield plug has a diameter at the first wall of 95 cm and is keyed sideways in steps with a diameter at the outer periphery of the shield of 110 cm. The shield plug is constructed from two blocks. The lower block is 32 cm high and is made of stainless steel. This block is fabricated and cooled in much the same way as a blanket block. The second outer portion of the shield serves the same function as a regular bulk-shield block. This block can be constructed of graphite and lead mortar to be 97 cm high with the same material arrangement as the outer shield. However, because the shield plug has to be moved completely out of the vacuum duct during the pump down phase it is important to reduce the total height of the shield plug. Therefore, the outer shield block in the shield plug is 58-cm high with a material configuration similar to that of the inner shield (SS/B₄C).

The special shield for each of the neutral beam ducts surrounds the duct as it emerges from the bulk shield and extends to the beam injector chamber. This shield is also divided into blocks (four along the circumference and three in the direction of the beam axis). Cooling (pressurized water) is provided for each block as required.

5. Maintenance and Repair Scenario

Most maintenance operations involving inspection or repair of the shield systems will require removal of one or more shield blocks and/or special shielding assemblies. In most cases faulty blocks will be replaced with a stand-by unit and either repaired in the shielded hot cell facilities or partially decontaminated and interned for disposal. Removal and replacement of blanket blocks will also require the removal of several shield

blocks and perhaps one or in some cases several adjacent blanket blocks. It is estimated that the replacement of a relatively accessible blanket block (L, M, N, R, or J in Figure IX-24) might be accomplished in 2 days, whereas replacement of the less accessible lower blanket blocks (A, B, C, or D in Figure IX-24) could take as long as 11 days. A replacement plan and schedule is given in Section C-3 of Chapter XII.

E. Thermal Analyses

1. Blanket

As described in section D of this chapter, the blanket configuration for the EPR consists of an interlocking array of modules that form a 28-cm-thick shell immediately surrounding the first wall assembly. For the purposes of the EPR thermal analysis studies, a modular unit 1 m x 1 m x 28 cm was taken as being representative of the nominal blanket building block. This unit consists of a monolithic piece of stainless steel into which cooling channels have been drilled (see Figure IX-26). Because each unit is cooled independently (all units in parallel), analysis of the thermal performance of a single unit is assumed to provide a reasonable approximation to the overall thermal performance of the entire blanket.

The present design is based on the use of pressurized water reactor technology for the primary (blanket) coolant circuit and the steam conversion system. The blanket is divided into four radial zones of interconnected coolant passages as shown in Figure IX-26. The first and second radial zones are cooled independently while the third and fourth are connected in series. The coolant exiting the first, second, and fourth zones is directed sequentially to the secondary heat exchanger (steam generator), to a dump heat exchanger, and then back to the entrance manifolds for the first, second, and third radial zones. Rather than varying flow rates to match prescribed outputs from each of the independent modular blanket units, the flow rates are held constant with the assumption that mixing in the exit headers would result in the nominal conditions of 309° C and 2000 psi for the pressurized water entering the steam generator.

The cooling concept described above and diagrammed in Figure IX-26 was analyzed with a one dimensional heat transfer code using the energy deposition parameters given in Figure IX-10. The flow rate in each radial

zone or series of zones was adjusted to conform to an inlet temperature of 40°C and an exit temperature of 309°C. Values for the coolant channel dimensions and the thermal fluid parameters determined in the calculations are listed in Table IX-9, with the zones number radially outward from the first-wall. The nominal conditions presented in Table IX-9 are representative of a unit that is intermediate in performance between an inner blanket module and an outer blanket module, the inner modules requiring slightly more cooling and the outer modules requiring slightly less cooling. Local hot spots near penetrations have not been treated explicitly in the analysis.

Table IX-9. Thermal Fluid Analysis for a Pressurized Water Cooled EPR Blanket Module^a

Zone	1	2	3	4
Zone Thickness, cm	5	6	8	9
Average Heating Rate, W/cm ³	2.93	1.48	0.79	0.47
Water Inlet Temperature, °C	40	40	40	198
Water Outlet Temperature, °C	309	309	198	309
Coolant Channel Diameter, cm	1	1	1	1
Coolant Velocity, m/sec	2.4	1.4	1.7	1.7
Coolant Inlet Pressure, atm	136	136	136	136
Coolant Pressure Drop, atm	1	0.62	0.74	0.72
Number of Coolant Channels	26	22	16	15
Total Length, cm	2600	2200	1600	1500
Flow Rate, gm/sec	162	101	120	120
Coolant volume, %	4	2.7	1.5	1.2

^a Maximum Blanket Temperature = 500°C
 Section Dimension = 1 m x 1 m x 28 cm
 Mean Wall Loading = 0.5 MW/m²

2. Magnet Shield Thermal Analysis

Table IX-10 presents thermal performance data for the magnet shield modules, with the zones numbered radially outward. As in the case of the blanket modules, the shield blocks are divided into four sub-sections. The energy deposition rates for each zone were taken from Figure IX-14.

The philosophy used to design the heat removal system for the shield differs from the one used for the blanket in that no attempt is made to remove sensible heat from the shield. (The sole function of the shield coolant is to keep the shield materials below 90°C.) Water at approximately atmospheric pressure is used as a coolant and the shield module is assumed to be built of layers of materials as indicated in Tables IX-2. The entire module is encased in stainless steel. The coolant is piped between the layers of materials and is ducted in and out of the modules using a manifolding arrangement similar to the system devised for the blanket.

Table IX-10. Thermal Fluid Analysis for a Water Cooled EPR Shield Block^a

Zone	1	2	3	4
Zone Thickness, cm	11	13	29	32
Average heating rate, W/cm ³	0.208	0.089	0.048	0.0002
Water inlet temp. °C	30	30	30	30
Water exit temp. °C	70	70	70	70
Coolant channel width, cm	2	4	6	8
Coolant velocity m/sec	1	1	1	1
Coolant inlet pressure, atm	1	1	1	1
Coolant pressure drop, atm	0.2	0.1	0.01	0.01
Void, %	5	5	5	5

- ^a Total Pumping Power = 8 kW
Maximum Shield Temp. = 90°C
Flow Rate = 164 kg/sec

F. Research and Development Requirements

A substantial, properly-focused, technology-development program will be required to support the design of a credible primary energy-conversion system for the EPR. The Stage I EPR is based, as much as possible, on available technology, with a minimum extrapolation of existing data and a minimum of additional development. R & D needs for the Stage I EPR are further reduced by the omission of a tritium-breeding blanket. However, a sizeable effort will still be required to verify critical design decisions for the Stage I EPR. The advanced blanket concepts to be tested in Stage II will require a substantial development program. This effort will include development of alternate structural materials and design concepts and will interface with the longer range blanket and shield technology research and development program for demonstration and commercial reactors. Descriptions of program content and direction for the major R & D items in support of the EPR are summarized below. A more comprehensive description of this program plan, with man-power and cost estimates included, has recently been published.¹⁸

1. Neutronics

Concentrated efforts are required in three principal areas: (1) basic nuclear data, (2) methods and code development, and (3) integral experiments. A comprehensive nuclear data base for materials applicable to the construction of an EPR should be established through measurements and evaluations. This nuclear data base should include the data parameters necessary for calculation of nuclear heating, gas production, atomic displacements, tritium breeding, neutron and gamma-ray transmission probabilities, and induced-activation. The methodology and computational tools needed to perform accurate neutronic/photonic analyses should be developed. Multidimensional neutron and photon transport prediction capabilities should be upgraded to treat deep radiation penetration problems in realistic EPR engineering configurations having irregular toroidal geometries and large access openings. Other capabilities for calculating nucleonic response functions (e.g. for nuclear heating and induced activation) must be extended and improved. The nuclear data and computational methods used in the EPR design should be supported and verified in bench-mark and mockup integral experiments.

2. Coolant Technology

Pressurized water (primary option) appears to be a suitable primary coolant for Stage I operation of an EPR, while liquid lithium and pressurized helium are prime candidates for the Stage II modular inserts. Important early needs include development of computational methods and experimental assemblies to quantify the thermal-fluid behavior of each coolant (e.g. heat transfer coefficients, effective thermal conductivities). Of special importance to Stage II operation is experimental confirmation that circulating liquid lithium in tokamak-type magnetic field configurations will not compromise either plasma stability (by perturbing the magnetic field) or the coolant performance of the lithium. It will be essential to establish an understanding of the interactions between each candidate coolant and likely containment materials. Included in the compatibility program are (1) effects due to impurities, temperature gradients, thermal and mechanical stresses, short-cycle fatigue phenomena, and magnetic fields; (2) development of the required impurity monitoring and remote-sensing techniques and instrumentation, and (3) establishment of an extensive physical-, chemical-, and mechanical-property data base. Ultimately the coolant technology program will require a number of major facilities to investigate promising coolant geometries, test coolant compatibility in realistic environments, support the development of key components, and verify overall coolant-system performance.

3. Mechanics

Unique fabrication and remote maintenance procedures are very likely to be essential to the sustained operation of EPR. A program is needed to develop and test methodology for remote assembly and disassembly of the PECS and other major reactor-support systems (e.g. vacuum pumps, neutral beams). The impact of fabrication and maintenance requirements on the reactor and overall plant designs should be established at the earliest possible time to permit reliable design descriptions for all major components. Computational methods (including code development) and other categories of engineering support (e.g. mechanical-property testing, major system layout, component assembly) should be constantly upgraded to guarantee a firm technology base in advance of the final

stages of the EPR design. In particular, the response of first wall and blanket materials to the stress, strain, and fatigue phenomena that result from the nature of the EPR burn cycle must be evaluated both computationally and experimentally. The analytical formalisms for modeling thermal, mechanical, and kinematic behavior in toroidal geometry must be developed and tested.

4. Shield Technology

Aspects of the shield design (other than its radiation-attenuation properties) that relate to thermal energy removal, performance monitoring, assembly, processing, coupling with penetrations, etc. must come under study. Verification of shield design methods for the very complicated geometry of the EPR will require a series of carefully conceived radiation shielding experiments. Methods of interfacing the shield with the blanket must be developed and tested.

5. Electromagnetics

Detailed analyses and supporting test programs must be initiated to fully evaluate the electromechanical interactions of the pulsed and steady state magnetic fields with the blanket and shield blocks. Blanket/shield design solutions that (1) permit pulsed field penetration, (2) minimize eddy current perturbations on plasma equilibrium, and (3) lead to tolerable mechanical and thermal reactions on materials within the blanket/shield region must be sought. This work will involve (among other things) materials-selection optimization, electromagnetic decoupling technology, insulator development, and simulation tests on a scale of sufficient size to permit verification of blanket/shield compatibility with confinement systems.

-
1. W. M. Stacey, Jr., et al., "Tokamak Experimental Power Reactor Studies," ANL/CTR-75-2 (June 1975).
 2. Mohamed A. Abdou, "Nuclear Design of the Blanket/Shield System for a Tokamak Experimental Power Reactor," *Nuclear Technology*, 29, 7-36 (April 1976).
 3. W. Engle, Jr., "A Users Manual for ANISN," K-1693, Oak Ridge Gaseous Diffusion Plant (1967).
 4. W. A. Rhodes and F. R. Mynatt, "The DOT III Two Dimensional Discrete Ordinates Transport Code," ORNL-TM-4280 (1973).

5. K. D. Lathrop and F. W. Brinkley, "TWOTRAN SPHERE: A FORTRAN Program to Solve the Multigroup Transport Equation in Two-Dimensional Spherical Geometry," Los Alamos Scientific Laboratory, LA-4567 (1970).
6. E. M. Gelbard and R. E. Prael, "Monte Carlo Work at Argonne National Laboratory," Proceedings of the NEACRP Meeting of a Monte Carlo Study Group, ANL-75-2 (1974); also R. E. Prael and L. J. Milton, "A User's Manual for the Monte Carlo Code VIM", an ANL internal memorandum FRA-TM-84 (1976).
7. M. A. Abdou, C. W. Maynard, and R. Q. Wright, "MACK: A Computer Program to Calculate Neutron Energy Release Parameters and Multigroup Neutron Reaction Cross Sections from Nuclear Data in ENDF Format," UWFD-37 and ORNL-TM-3994 (1973).
8. D. Garber, C. Dunford, and S. Pearlstein, "Data Formats and Procedures for the Evaluated Nuclear Data File, ENDF," Brookhaven National Laboratory, BNL-NCS-50496 (ENDF 102), also TID-4500 (October 1975).
9. J. Jung, "Finite Difference Equations for Transport Equation in Toroidal Geometry," *Nucl. Sci. Eng.*, 60, 74 (1976).
10. M. A. Abdou, "Calculational Methods for Nuclear Heating and Neutronics and Photonics Design for CTR Blankets and Shields," PhD Thesis, University Microfilms Inc., 74-8981; also issued as reports UWFD-66 and UWFD-67, University of Wisconsin, Nuclear Engineering Department (1973).
11. D. L. Chapin and W. G. Price, Jr., "A Comparison of the D-T Neutron Wall Load Distributions in Several Tokamak Fusion Reactor Designs," Plasma Physics Laboratory, Princeton University, NATT-1186 (December 1975).
12. M. A. Abdou and R. W. Conn, "A Comparative Study of Several Fusion Reactor Blanket Designs," *Nucl. Sci. Eng.*, 55, 256-266 (1974).
13. M. A. Abdou and C. W. Maynard, "Neutron Source Geometry Effects on Fusion Reactor Blankets," *Trans. Am. Nucl. Soc.*, 15, 34 (June 1972).
14. Neil N. Ault, Norton Company - Private communication (1975).
15. William Connellus Hall, Chemtree Corporation - private communication (1976).
16. C. A. M. van der Klein, "The Organic Insulation in Fusion Reactor Magnet Systems," Reactor Centrum Nederland, RCN-240 (December 1975).
17. H. Brechna, "Effect of Nuclear Radiation on Organic Materials; Specifically Magnet Insulation in High-Energy Accelerators," Stanford Linear Accelerator Center, SLAC-40 (March 1965).
18. F. Mills, et al., "Research and Development Required for the Tokamak Experimental Power Reactor," ANL/CTR/TM-65 (May 1976).

CHAPTER X

TRITIUM HANDLING FACILITY

A. Introduction

Chapter VII of reference 1 describes the results of initial scoping studies of the tritium handling requirements for a tokamak Experimental Power Reactor (EPR). Although many features of the ANL/EPR design have been modified during the FY-1976 conceptual design activity, the general conclusions reached in reference 1 regarding the nature of the tritium facility have remained relatively unchanged. Efforts during FY-1976 have focused on more detailed analyses of those tritium facility systems and requirements for which need of a greater understanding was indicated during the FY-1975 scoping studies. Major EPR tritium facility operating parameters (including tritium inventories, burnup rates, etc.) were redetermined for the FY-1976 design and are listed in Table X-1. An analysis of existing technology relative to tritium transfer operations in the EPR gave evidence that the required fuel circulation and control hardware (valves, pumps, compressors, etc.) was close to state-of-the-art achievements and that only modest development and proof testing were necessary. An in-depth study of hydrogen isotope enrichment requirements for the EPR and other near term fusion devices led to the development of a multi-column isotope separations and analysis code which in turn was used to evolve, analyze, and refine a conceptual design for the EPR cryogenic distillation cascade. Analysis of reactor hall atmospheric clean-up requirements following massive accidental tritium releases of various magnitudes highlighted a number of problem areas that will require modest development beyond present state-of-the-art in order to limit equipment size, cleanup times, and cost. The selection of water as a coolant for the first wall, blanket, and shield raises questions regarding (1) tritium entry and buildup in the water circuits, (2) methods of handling the water, and (3) likelihood of large tritium releases to the environment. Practices currently employed in handling the primary coolant of pressurized heavy water reactors appear to be more than adequate to meet the projected needs of an EPR.

Table X-1. Summary of Tritium Handling Facility Parameters

<u>General</u>	$B_{\text{max}}^{\text{TFC}}$	
	<u>8 T</u>	<u>10 T</u>
Power During Burn (MW) ^a	200	500
Burn Cycle Duty Factor (%)	75	
Plant Availability Factor (%)	67	
Tritium Burnup (gm/day)	26	64
Throughput/Burnup Ratio	50	
Tritium Delivery Rate (gm/hour)	60	150
Fuel Cycle Turnaround Time (hours)	4	
Plant Inventory (kg)	0.6	1.5
Annual Tritium Consumption at 50% Duty Factor (kg)	6.4	16
<u>Tritium Inventory Disposition</u>		
Cryosorption Pumps (gm)	240*	600*
Getter Beds (gm)	240*	600*
Distillation Columns (gm)	10*	25*
Fuel Cycle Hardware (gm)	10*	25*
Storage (gm)	~500*	~1300*
Anticipated Mean Inventory (gm)	600	1500
<u>Fuel Cycle</u>		
Nature of Fuel Processing and Recycle Systems	{ Nonmetallic Element Removal	
	{ Debris Removal	
	{ Isotopic Enrichment	
	{ Fuel Storage	
	{ Fuel Delivery	

* Maximum value at any single time.

^aThe power during burn depends upon a number of factors involved in the details of the burn pulse. These values are representative of the range of burn cycles studied.

Table X-1. Summary of Tritium Handling Facility Parameters (Cont.)

<u>Isotopic Enrichment</u>	$B_{\max}^{\text{TFC}}$	
	<u>8 T</u>	<u>10 T</u>
Type of Mainstream Enrichment	Cryogenic Distillation	
Number of Columns	6	
Number of Equilibrators	1	
Operating Pressures (Torr)		
Column 1	500	
Columns 2,3,4,5,6	1000	
Reflux Ratio		
Column 1	20	
Columns 2,3,4,5,6	15	
Feed to Column 1 (gm/hr)	~100	~250
Height Equivalent Theoretical Plate (inch)	1	
Liquid Holdup (cc/cc of packing)	0.18	
Superficial Vapor Velocity (ft/s)	0.2 - 1.0	
Number of Theoretical Stages per column	30	
Maximum Column Diameter (inches)	< 2.0	
<u>Atmospheric Clean up System</u>		
Reactor Hall Volume (m ³)	~2.8 x 10 ⁵	
Baseline Tritium Level (μ Ci/m ³)	5	
Clean-up Time, (days)	≤ 2	
Maximum Decontamination Factor	10 ⁶	
Air Circulation Rate at Emergency (cfm)	$\geq 10^5$	
Volume of Catalyst (m ³)	20	
Catalyst Operating Temperature (°C)	27	
<u>Miscellaneous</u>		
Tritium Level in First Wall		
Cooling Water (Ci/l)	≤ 2	
Tritium Release Rate to Environment (Ci/day)	100	
Volume of First Wall Cooling Water (l)	~10 ⁵	

In addition to the more definitive studies mentioned above, tritium handling aspects of the small scale experimental breeding test experiments and full scale breeder blanket mock-up modules planned for later stages of EPR operation were reassessed in the light of the present understanding of power reactor blanket design criteria and constraints.

B. Tritium Inventory and Reactor Fueling (Logistics)

Tritium fueling, burnup, and inventory parameters for the 8 and 10 T EPR designs are listed in Table X-1. The principal assumptions applied in determining these parameters are (1) that the throughput/burnup ratio will not be greater than 50, (2) that the fuel cycle turnaround time (fuel hold-up time) will be 4 hours or less, and (3) that the initial tritium inventory will be supplemented as required to meet fueling requirements during periods of continuous operation at the projected overall duty factor of 50%. The fuel cycle turnaround time is determined mainly by the regeneration cycle on the cryosorption pumping system for the toroidal plasma chamber. (As indicated in Chapter VII-A, this regeneration cycle is carried out on a four hour basis.)

An alternative to the continuous delivery approach is to stock the plant with a considerably larger initial inventory -- perhaps a one year supply. Although most of this additional inventory would be repositied in the storage vault, the overall hazard potential of the plant must be considered to increase in proportion to the total tritium inventory. Furthermore, even a 600 gm initial inventory would more than likely be delivered in partial shipments not exceeding 200 gm per shipment; hence, there is no apparent reason to consider stocking any more tritium than is needed for a few weeks of continuous operation. Subsequent planning exercises to determine (1) operating schedules for various hypothetical burn sequence tests and (2) the length of down time between test programs are expected to shed more light on the inventory question. Ultimately, the safest way to gauge the inventory requirements may be to determine the quantity of tritium needed for the duration of an individual test program (to be conducted without substantial intermediate down times) and make provision for the delivery of that inventory prior to initiation of the test program.

C. Fuel Circulation and Processing

Chapter VII of reference 1 contained a description of the scope of fuel circulation and processing requirements for the EPR. Although only a cursory reassessment of these requirements was made during FY-1976, no compelling findings were uncovered to give cause for significant revision of the fuel cycle scenario outlined in reference 1. The major processing requirements for the EPR fuel cycle that have come to light thus far are (1) separation of solid (potentially radioactive) debris from the plasma chamber, (2) removal of nonmetallic impurities (e.g., He, O, N, C) from the fuel stream leaving the torus, (3) reduction of protium levels in the D-T mixture prior to recycle, and (4) adjustment of feed compositions for the cold fuel supply and the neutral beam injector supply. The remainder of this section and Section X-D summarize the status of design and analysis work on the fuel circulation and processing systems for the EPR.

1. Debris Separation and Handling

The harsh radiation environment anticipated for the first wall of the EPR is likely to lead to a dislodging of sizeable quantities of fine particulate metal and metal compounds. Although most of this debris is expected to either settle out or reattach itself to the first wall, plans must be made to accommodate the migration of some particulate material into the vacuum pumps and beyond them. Because the first wall will become highly radioactive after only a few days of operation at modest wall loadings (0.2 to 1.0 MW/m²), the debris will also be highly radioactive and maintenance of the vacuum pumps and the equipment immediately downstream of them will be subject to increased complexity. Large quantities of debris in the torus exhaust gases will undoubtedly have an adverse effect on pump lifetime and performance, but any attempt to microfilter the debris in advance of the vacuum pumps (between the plasma chamber and the pumps) will more than likely lead to unacceptably large conductance losses.

During clearing (regeneration) of the cryopanel, the escaping gases will probably fluidize some of the debris that entered the cryopanel in the torus pumping cycle. (Hopefully, most of the debris will be carried

out of the cryopumps in this way, so that pump lifetimes can be extended.) Removal of this debris from the fuel recycle stream can probably be done with some combination of cyclone separators, electrostatic precipitators, or millipore filters. If possible, the debris separation system should be located immediately downstream of the cryopumps (to the extent that this location does not adversely extend regeneration times). It is difficult to make accurate predictions of the particle size, size distribution, and quantity of debris generated by a prototypal EPR; hence, some experimentation will ultimately be required in order to test debris removal methods in flowing hydrogen streams that simulate the mainstream of the EPR fuel cycle.

2. Nonmetallic Impurity Removal

The perpetual presence of helium, oxygen, carbon, nitrogen, and other nonmetallic impurities in the torus exhaust of an operating EPR will for all practical purposes be unavoidable. In keeping with the concept of fuel recycle and in order to reduce impurity levels in the preburn fuel mixture as far as is possible, it will be necessary to provide for continuous removal of nonmetallic elements in the cryopump exhaust. This should certainly be done in advance of the isotopic enrichment step and perhaps done again in advance of fuel storage so that the capacity of the storage material is not reduced by reaction with impurities.

Impurity removal can probably be carried out in a relatively straightforward manner using an appropriately selected catalytic and/or getter-type bed designed to (1) crack water, hydrocarbons, and other hydrogenous compounds and (2) actively remove the impurities by reaction to form stable, nonvolatile, nonhydrogenous compounds. The getter bed should be designed to sorb and release hydrogen over a relatively small temperature range, should be reasonably compact, and should be readily disposable. It is expected that these getter beds will reduce O, N, C and related nonmetallic impurities in the fuel stream to the sub ppm range. The helium present in the cryopump exhaust could be allowed to carry over into the cryogenic distillation columns and be removed therefrom as a noncondensable, providing this carryover does not compromise column operation. In addition

to the need to identify and test getter bed materials (or combinations of materials) for broad spectrum nonmetallic impurity removal, the relative merits of continuous versus various degrees of batch processing of the cryo-pump exhaust remain to be evaluated.

3. Isotopic Enrichment

Isotopic enrichment of (1) the main fuel stream (to remove protium) and (2) various levels of tritiated effluent from jacketed and whole room purges (to recover and recycle tritium) is currently being carried along as a major goal for the EPR tritium handling facility. Some modifications have been made to the enrichment scenario presented in Chapter VII of reference 1. The scenario currently under study is described in Section X-D and a more detailed discussion is given in Appendix I. The major functions of the enrichment system (insofar as the fuel cycle is concerned) are (1) to provide a tritium rich stream (T/D ≥ 1.1) of sufficient isotopic purity for cold fuel injection, (2) to generate a ≥ 98 atomic % deuterium stream for the neutral injection system, (3) to perform a maximum separation of protium from tritium so that as little tritium as possible is carried over with the protium discharge, and (4) to provide enough levels of enrichment to allow maximum recycle of tritium that is scavenged by the atmospheric and purge cleanup systems during normal operation and in cases of accidental release. The location of the enrichment system in the fuel cycle is described in part 4 of this section.

4. Fuel Cycle Design and Hardware

With several minor exceptions, the fuel circulation and processing system design has remained much the same as was described in Chapter VII of reference 1. A schematic drawing of the currently conceived system is shown in Figure X-1. Although the roughing system is not used during burn cycle pumping operations, it is designed to handle modest tritium levels in the exhaust gases that leave the torus during roughdown and bake out to the pre-burn operating pressure range. The remainder of the system (from the cryopumps on) is designed for high level tritium operations.

The operational features of the cryosorption pumping stations are described in Chapter VII-A. The two pumps at each station are alternately

Figure X-1. Fuel Cycle Scenario for ANL/EPR

regenerated on a 4 hour basis. During regeneration the pressure inside the pump being regenerated is not allowed to rise above a few microns in order to limit refrigeration requirements. As presently planned, the gases exiting the regenerating pump are first passed through a fine debris separator. Instead of using the mechanical clearing pump included in the previous design,¹ the cryopump exhaust gases are pumped with a hydrogen getter bed as suggested by Watson et. al.³ The bed should be prepared so that (1) it has a high effective pumping speed for hydrogen at modest temperatures (a few hundred degrees centigrade), (2) it is regenerable with only a modest temperature increase in order to conserve on regeneration power requirements (i.e. the getter material must have a relatively large heat of solution for hydrogen isotopes), (3) it getters and retains the major anticipated nonmetallic elements (e.g., O, N, C), and (4) its active materials are easily replaced and disposed of.

During both pumping and regeneration cycles the getter pump is cleared by a mercury ejector pump followed by a multistage arrangement of wobble pumps of the type described by Folkers and Gede.⁷ The mercury ejector pump is capable of pumping at inlet pressures of < 1 micron against a substantially higher backing pressure.⁷ A wobble pump is a completely sealed unit, in which compression is achieved by the wobble motion of a metal bellows assembly that operates much like a nutating piston. These pumps perform effectively at inlet pressures as low as 10 microns and can be staged in series to achieve pressures approaching 1 atmosphere. They are generally regarded as being suitable for service in a high level gaseous tritium environment.⁷

The hydrogen isotope stream leaving the wobble pumps (mostly D_2 , $D-T$, and T_2) is forced through a high capacity, low pressure drop, packed liquid nitrogen trap to remove any condensible impurity vapors in advance of the cryogenic distillation cascade. This trap would be designed to accommodate regeneration and maintenance access and would be equipped with a thermal economizer to limit nitrogen boil-off.

The function of the cryogenic distillation cascade is described in Section D of this chapter and, therefore, will not be discussed further here. The return streams from the distillation cascade consist of (1) a tritium

enriched cold fuel supply ($T/D \approx 1.1$) and (2) an ~ 98 atomic % deuterium supply to feed the neutral injectors. Essentially all of the tritium admitted to the fuel cycle remains in the cold fuel supply where it is either recycled to the reactor or returned to the storage reservoir. The deuterium supply for the neutral injectors is delivered as needed with any excesses being returned to an appropriate stage of the distillation cascade. Details of the fuel circulation requirements downstream from the distillation cascade remain to be evaluated. The general concept of the storage reservoir for the tritium rich streams has not changed from that described in Chapter VII of reference 1.

All piping and the majority of the hardware (pumps, valves, etc.) will be constructed from 18-8 stainless steels as recommended in references 6 and 7. All joints will be welded except where integrated sections of the fuel circulation system join together. In order to facilitate maintenance operations, these integrated sections will be coupled through a safety box system as shown in Figure X-2. The safety box is designed to provide jacketed containment around the mechanical section-to-section seal and to permit isolation, removal, and replacement of a failed section with minimum escape of tritium. The boundary pipe (between isolation valves in Figure X-2) is bakeable and evacuable to verify valve seating and to allow for detritiation prior to any decoupling operations.

All valves will be the welded bellows seal type and will be outfitted for air actuation as well as emergency manual operation. Valves that are not located in a safety box and all other high risk components (e.g. pumps, compressors) will be located in a secondary container^{6,7} which is connected to a purge cleanup system of the type described in reference 2.

D. Design Description of the Cryogenic Distillation Cascade

Isotopic enrichment of (1) the main fuel stream (to remove protium) and (2) various levels of tritiated effluent from jacketed and whole room purges (to recover and recycle tritium) is still considered to be a major goal for the EPR tritium handling facility. Some modifications have been made to the enrichment scenario presented in Chapter VII of reference 1.

Figure X-2. Fuel Cycle Module Connection Housing

1. Summary Design Description

The scenario currently under consideration is illustrated in Figure X-3. The cascade of columns in Level A is designed to handle various molecular hydrogen streams (from the purge cleanup systems) with T/H ratios greater than 10^{-4} . The appropriate feed plate location in the Level A cascade will be determined by the T/H ratio of the incoming stream. Because the major source of tritium entering the purge circuits is from the mainstream of the fuel cycle, the D/T ratio in all Level A feeds (except for the feed from Level B) is expected to be near unity. The Level B cascade handles the top fractions from the mainstream enrichment cascade (Level C). These fractions are predominantly deuterium but will contain essentially all of the protium that enters the mainstream and a small but non-negligible quantity of tritium. The purpose of the Level B cascade is to affect a maximum separation of protium from tritium. The protium fraction is directed to the Level A cascade and the tritium fraction is returned to the Level C cascade. In order to maximize the H/T separation in the Level B cascade, equilibration with excess D is included as shown. The Level C cascade receives the flow from the mainstream of the fuel cycle. This flow is expected to consist of a roughly equi-molar mixture of D and T containing at most 1 to 2 atm % H. The first column (column 1) of this cascade is designed to give a large H/T separation factor. The stream from the top of the first column (containing mostly D together with > 99% of the H that entered the column and some T) is sent to a second column which further separates the H and T. Approximately 80% of the mainstream fuel flow leaves the Level C cascade through the bottom of the first column. The bottoms from the second column are subjected to additional enrichment to produce D of sufficient purity for neutral beam injection.

2. Analysis of the Distillation Cascade

A computer code (MISRA) has been written to model six component (H_2 , HD, HT, D_2 , DT, T_2) distillation in cascades of single columns. Appendix 1 contains a description of the analytical algorithm used in building the code and a comprehensive summary of all modeling results that are generally pertinent to fusion devices. A summary of key operating parameters for the first six columns in Figure X-3 is given in Table X-2. The bottom product from

Figure X-3. Cryogenic Enrichment Scenario for the ANL EPR

Table X-2. Summary of Operating Parameters for the EPR Cryogenic Distillation Cascade*

Column Number	Location of Feed Plates	Total Feed (moles)	Nominal Gross Feed Composition (at %)			Top Product				Bottom Product			
						Total Moles	Atomic Percentages			Total Moles	Atomic Percentages		
			H	D	T		H	D	T		H	D	T
1	10 15 20	100.0	4.1	49.7	46.2	18	22.5	62.3	15.2	82	0.07	46.8	53.1
2	15 20	23.0	18.2	69.4	12.4	13	31.2	48.2	20.6	10	1.3	96.9	1.8
3	10 15 20	23.0	38.9	29.6	31.5	8	51.0	17.9	31.1	15	32.5	35.8	31.7
4	15	8.0	50.9	18.9	31.1	3	98.8	1.2	0	5	21.1	28.5	50.4
5	15	15.0	32.5	35.8	31.7	5	48.2	6.0	45.8	10	24.7	50.7	24.6
6	15	10.0	1.3	96.9	1.8	5	2.5	95.1	2.4	5	0	98.8	1.2

* 30 theoretical stages per column; column pressure = 500 torr for Column No. 1 and 1000 torr for Columns 2 through 6.

† See Fig. X-3 for location in cascade.

column 1 (the cold fuel supply) is enriched in tritium ($T/D \approx 1.1$) and contains less than 0.1 atomic % protium. The bottom product from column 6 is sufficiently rich in deuterium (~ 98 atomic % D) to provide an adequate supply for the neutral injection system. Because the probability that tritium will enter the neutral injection system via the torus/beam line interface is considered to be very high, the residual tritium in the neutral injection supply should present no serious problems. (In the presently conceived neutral beam injection concept for the EPR, the entire injection system will be designed on the basis that tritium levels will indeed be on the order of a few percent of the deuterium content.)

A single stage of isotopic equilibration is provided in advance of column 4 to reduce tritium carryover with the protium rich top product of column 4. In this equilibrator excess deuterium is added to the stream that emerges from column 3 in order to separate the HT present into HD and DT ($D_2 + HT \rightarrow DT + HD$). The less volatile DT is then concentrated in the bottom product of column 4 and is returned to a lower level of the B cascade. With this equilibrator in line the tritium in the top product of column 4 is reduced by a factor of ~ 50 (see Appendix I).

3. Column Dimensions and Inventories

Some of the important parameters involved in the design of the distillation columns are (1) the number of theoretical plates or stages; (2) plate spacing or height equivalent to a theoretical stage, HETR, for packed columns; and (3) the superficial vapor velocity. For very small diameter columns (approximately 0.5 to 2 inches), only packed columns need be considered (plate-type columns are impractical in such small sizes). While parameters (1) and (2) are easily determined, the estimation of superficial velocity, which determines the column diameter, presents a more formidable task. For conventional fluids (hydrocarbon mixtures and aqueous solutions) the estimation of superficial velocity is based on laboratory data and empirical correlations. However, such data and/or correlations for the isotopes of hydrogen are largely non-existent. Estimation of the liquid holdup rate, which is a function of the superficial velocity, in addition to other factors, presents similar difficulties; hence, there is some inherent uncertainty involved in the calculations that follow.

The following calculations of column size and liquid holdup are based on limited experimental data^{12,13} for the ternary system $H_2/HD/D_2$. Reference 12 mentions the use of a liquid velocity of 2.6 inch/second where as flooding conditions were observed in Reference 13 in the superficial velocity range of 1.4 to 1.7 inch/second. Reference 12 uses an HETP of 5/8 inch while, in contrast, reference 13 suggests HETP values of 1 to 4 inches. The column size and the liquid holdup are based on the following assumptions for the 6-column cascade in Figure X-3:

Operating Pressure

Column 1	500 Torr
Columns 2, 3, 4, 5, 6	1000 Torr

Reflux Ratio

Column 1	20
Columns 2, 3, 4, 5, 6	15

Feed to Column No. 1	100 gms/hr
HETP	1 inch (all columns)
Liquid Holdup	0.18 cc/cc packing
Superficial Vapor Velocity	0.2, 0.5, and 1.0 ft/sec
Number of Theoretical Stages	30

The summary of data presented in Table X-3 is intended to show the approximate size of the columns and the quantities of liquid holdup in them. Since the calculations are based on limited published data, these values should be considered preliminary. Also, the results in Table X-3 are based on the use of a single cascade to process all of the mainstream flow. In actual operation the enrichment would probably be done with from 2 to 4 independent cascades in parallel, making the column sizes even smaller than those indicated in Table X-3. Whether the mainstream enrichment is carried out with a single cascade or with several cascades in parallel, sufficient redundancy would be provided to accommodate maintenance and repair operations.

4. Comments on the Level A Cascade

No detailed modelling was carried out during FY-1976 on the level A cascade in Figure X-3. The design of this cascade requires a better understanding of the quantity and isotopic distribution of the hydrogen isotopes present in purge streams. Up to now the assumption has been made that the hydrogen isotopes present in these streams can be isolated from other impurities

Table X-3. Column Diameters and Liquid Holdup for Selected Superficial Vapor Velocities

Superficial Vapor Velocity ft/sec	0.2		0.5		1.0	
Column Number*	Diameter, in.	Liquid Holdup, cc†	Diameter, in.	Liquid Holdup, cc†	Diameter, in.	Liquid Holdup, cc†
1	1.53	163	0.97	66	0.86	32
2	0.84	50	0.53	19	0.38	10
3	0.71	35	0.45	14	0.32	6
4	0.40	11	0.25	5	0.17	3
5,6	0.55	21	0.35	8	0.25	5

* Referred to Figure X-3.

† Tritium concentration $\sim$ 0.036 gm/cc.

and reconstituted in the molecular form so that they can be fed to a cryogenic distillation system. The presently conceived plan is to equilibrate the molecular hydrogen effluents from the clean-up systems with a large excess of deuterium and to feed the resulting HT, DT, D₂ mixture to the appropriate level A column (see Figure X-3) depending on the required separation factor. The extension of work on the enrichment systems, planned for FY-1977, will include a more comprehensive study of the low level tritium recycle scenario, together with the development of analytical models for predicting the volume and the composition of clean-up system effluents.

E. Atmospheric Detritiation Systems

Preliminary assessments showed that the requirements imposed by the need to provide for large scale atmospheric detritiation of the EPR reactor building are a major concern from the standpoint of (1) maintenance access during reactor down periods, (2) compactness of tritium handling equipment, (3) limitations to the spread of tritium contamination, and (4) overall tritium facility costs. During FY-1976 an effort was made to consider the scope of these requirements with respect to gas handling rates, exigencies of the cleanup schedule, and the dominant features of the cost/benefit algorithm. These studies were aided by a number of recent reports^{4,5} on the subject and by helpful discussions with members of other ERDA laboratories^{16,17} and industry¹⁸ who are directly involved in development work on detritiation systems.

1. Estimates of Gas Handling Requirements

Consider a large reactor hall of volume V_{TOT} having a baseline tritium level, N^0 . Assume that the room air is processed at a volumetric flow rate $\dot{V}$ and that the processor efficiency is ϵ . The rate of tritium removal is given by

$$\frac{dN}{dt} = \frac{\epsilon \cdot \dot{V} \cdot N}{V_{TOT}} \quad (X-1)$$

The amount of time, t , it takes to reduce a massive tritium release from the maximum value following the incident, N' , back to N^0 is obtained by integrating Equation (1) to produce

$$\ln \frac{N'}{N^0} = \epsilon \frac{\dot{V}}{V_{TOT}} t \quad (X-2)$$

Values of $\dot{V}$ for selected values of N^1/N^0 , t , and t_0 are given in Table X-4 for a room with $V_{TOT} \sim 10^7$ cu. ft. ($\sim$ EPA size).

According to Engelhard Industries,¹⁸ the largest unit they have evaluated to date called for 6×10^3 cfm at a cost of $\sim \$ 10^6$ for the equipment alone. Because the tritium released to the hall will rapidly soak into the surfaces of the reactor hardware and the building itself, it is advantageous to have the capability for cleaning up spills within hours after a release. If, for example, the limit is set at ≤ 5 hours, then the $\dot{V}$ requirements would be ~ 10 times those for the two-day case in Table X-4 and would be 100 times greater than the maximum size unit upon which Engelhard has made a quote.

A second approach to the massive release problem might be rapid cyclic flushing of the reactor hall by alternately compressing its contents (reducing room pressure by a factor η) and back filling with clean air or an alternative cover gas. If the compressed gas could be stored at 14,000 psi in tanks whose total volume is 10^5 cu. ft., the tanks would then hold 10 room volumes of gas which could be cleaned up over an extended time period (several weeks) by a state-of-the-art sized scrubbing system. The cleaned up gas could then be stored for subsequent flushing operations. For the case where the room is evacuated to $1/\eta$ of its normal operating pressure and back-filled every t_0 hours, one obtains Equation (3).

$$\frac{dN}{dt} = \frac{\eta}{t_0} N \quad (X-3)$$

Integrating Equation (3) for the cases that were considered in Table X-4 gives

$$\ln \frac{N^1}{N^0} = \frac{\eta}{t_0} t \quad (X-4)$$

The results for this type of atmospheric cleanup are summarized in Table X-5. (The ejector/compressor velocities, $\dot{V}^1$, are based on pump-out times that are equal to $t_0/2$.) In order to keep compressor requirements within reasonable limits, it is necessary that t_0 not be much in excess of a few hours. A value of $t_0 = 4$ hours was selected for the study shown in Table X-5. This is considered to be reasonable based on existing experience at several NASA space testing installations.¹⁹

Table X-4. Analysis of Recycle Flow Scenario

Amount Released	$\left(\frac{N'}{N^0}\right)^*$	$\ln\left(\frac{N'}{N^0}\right)$	$\dot{V}$, cfm			
			$\frac{t = 2 \text{ days}}{0.5 = \epsilon = 0.9}$		$\frac{t = 14 \text{ days}}{0.5 = \epsilon = 0.9}$	
1 gm	10^4	9.2	6.4×10^4	3.5×10^4	9.1×10^3	5.0×10^3
100 gm	10^6	13.8	9.6×10^4	5.3×10^4	1.4×10^4	7.6×10^3
10,000 gm	10^8	18.4	1.3×10^5	7.1×10^4	1.8×10^4	1.0×10^4

* Assuming $N^0 = 5 \text{ } \mu\text{Ci/m}^3$ and $V_{\text{TOT}} \simeq 10^7 \text{ cu. ft.} \simeq 2.8 \times 10^5 \text{ m}^3$.

Table X-5. Analysis of Cyclic Flushing Scenario

Amount Released	$\frac{N'}{N^0}$ *	$\ln\left(\frac{N'}{N^0}\right)$	$\dot{V}$ in cfm (for pumpout time = $t^0/2$)†			
			$\frac{t = 2 \text{ days}}{0.8 = \eta = 0.99}$		$\frac{t = 14 \text{ days}}{0.8 = \eta = 0.99}$	
1 gm	10^4	9.2	8.0×10^4	6.3×10^4	1.1×10^4	9.0×10^3
100 gm	10^6	13.8	1.2×10^5	9.7×10^4	1.7×10^4	1.4×10^4
10^4 gm	10^8	18.4	1.6×10^5	1.4×10^5	2.2×10^4	1.8×10^4

* Assuming $N^0 = 5 \text{ } \mu\text{Ci/m}^3$ and $V_{\text{TOT}} \simeq 10^7 \text{ cu. ft.} \simeq 2.8 \times 10^5 \text{ m}^3$.

† $t^0 = 4 \text{ hours.}$

Comparison of the results in Tables X-4 and X-5 shows that there is no practical advantage in terms of gas circulation requirements to the evacuation approach as compared to the more conventional continuous scrubbing approach. The major concern with respect to these massive tritium releases, i.e., soaking of tritium into the reactor hardware and building surfaces, will have to be investigated in considerable detail to determine what cleanup durations are acceptable. Clearly, the requirements of cleanup in two days or less imposes large gas circulation requirements and the associated equipment can be expected to scale accordingly.

2. Catalyst Requirements

A reasonably comprehensive analysis of large-scale atmospheric detritiation for fusion power plants has been presented by Galloway et al.⁴ In their study, they consider the question of large-scale cleanup from the standpoint of catalyst performance and cost. Instead of equating processing requirements with removal efficiency, ϵ , as was done above, they use first-order kinetic, plug flow reactor design equations to represent catalytic bed performance. This approach permits the evaluation of both performance and cost simultaneously. The equation developed by Galloway et al. is given by

$$\frac{N^0}{N^1} = \exp \left\{ - \left[1 - \exp\left(-k \frac{V_c}{V}\right) \right] \frac{\dot{V}}{V_{TOT}} t \right\} \quad (X-5)$$

where

N^0 = the tritium activity at any time t after cleanup has been started.

N^1 = the tritium activity at $t = 0$ (immediately after the release incident)

V_c = volume of catalyst in m^3

$\dot{V}$ = gas circulation rate in m^3/sec

k = the first-order kinetic rate constant in sec^{-1} [see Equation (X-6)]

V_{TOT} = the room volume in m^3

t = time after start of cleanup in sec

The rate constant k is taken from data developed at Mound Laboratory⁵ and is given by

$$k(sec^{-1}) = 2.27 \times 10^5 \exp(-7100/RT) \quad (X-6)$$

where T is the Kelvin temperature and $R = 1.987 \text{ cal/mole}^\circ\text{K}$.

For purposes of comparison with results in Tables X-4 and X-5, Equation X-5 was rearranged to solve for V_c , as given by Equation X-7.

$$V_c = -\frac{\dot{V}}{k} \ln \left[1 - \left(\frac{V_{TOT}}{\dot{V}t} \right) \left(\ln \frac{N'}{N^0} \right) \right] \quad (X-7)$$

Results of a study using the cases in Tables X-4 and X-5, i.e., 2 days and 2 weeks, $\dot{V} = 10^5 \text{ cfm}$ for the 2-day case and 10^4 cfm for the 2-week case, and $V_{TOT} = 10^7 \text{ cu. ft. (283,000 m}^3\text{)}$, are shown in Table X-6 for selected catalyst temperatures.

3. Atmospheric Clean-up Scenario and Associated Costs

The cost algorithm used in Table X-6 is based on information from a number of sources. Golankiewicz¹⁸ has recommended a catalyst cost of \$0.06 M/m^3 where large quantities are involved. Galloway¹⁶ suggests that the equipment cost be obtained by taking a factor of ten times the catalyst cost. In house studies have indicated that the building needed to house the clean-up equipment should not cost more than 50% of the equipment outlay. The costs indicated in Table X-6 were computed on this basis and were found to be in reasonable accord with the previously mentioned estimate of $10^6 \text{ \$}$ for a $6 \times 10^3 \text{ cfm}$ Engelhard system¹⁸ (which scales to $\sim \$ 17\text{M}$ for a 10^5 cfm unit or $\$ 1.7\text{M}$ for a 10^4 cfm unit). Costs for the elevated temperature catalyst cases in Table X-6 were not computed with this algorithm because (1) the algorithm ignores the gas heating and recooling requirements and (2) it is not clear that the small quantities of catalyst indicated by the calculations could actually service the large gas flow rates involved and retain their efficiency for the duration of the cleanup operation.

In light of the results in Table X-6 the following atmospheric clean-up scenario is tentatively proposed for the ANL/EPR. Provision is made to handle the 10^5 release case ($N'/N^0 = 10^6$) on the basis that the escape of 100 gm of tritium represents the largest credible event that could occur in the reactor building without a simultaneous breach of the building containment boundaries. It is further postulated that the cleanup must be completed within 2 days using room temperature catalyst. This leads to a catalyst requirement of 20 m^3 and a resulting cleanup facility cost (with associated building) of ~ 20 million dollars. The facility will be designed to accommodate

Table X-6. Scrubber Plant Catalyst Cost Versus Clean-up Requirements
Catalyst Operating Temperature and Flow Rate

$k(s^{-1})$	1.525			80.82			588.4		
Catalyst Operating Temperature	27°C(300°K)			177°C(450°K)			327°C(600°K)		
Reactor Hall Volume (cu. ft.)	10^7			10^7			10^7		
Minimum Flow Rate (cfm)	10^5	10^4		10^5	10^4		10^5	10^4	
Required Clean-up Time (days)	2	14		2	14		2	14	
R^*/R^* for $R^* = 5\mu\text{Ci}/\text{m}^3$	10^4 10^6 10^8	10^4 10^6 10^8	10^4 10^6 10^8	10^4 10^6 10^8	10^4 10^6 10^8	10^4 10^6 10^8	10^4 10^6 10^8	10^4 10^6 10^8	10^4 10^6 10^8
Volume of Catalyst Required (m^3)	12 20 32	1.9 3.6 7.2	0.22 0.38 0.59	3.6 6.7 1.4 E-2 E-2 E-1	3.1 5.2 8.2 E-2 E-2 E-2	4.9 9.3 2.0 E-3 E-3 E-2			
Catalyst Cost \$0.06 M/m^3 (\$M)	0.7 1.2 1.9	0.12 0.22 0.43	--	--	--	--	--	--	--
Equipment Cost ^(a) (\$M)	7 12 19	1.2 2.2 4.3	--	--	--	--	--	--	--
Building Cost ^(b) (\$M)	3.5 6 9.5	0.6 1.1 2.2	--	--	--	--	--	--	--
Total Cost (\$M)	12 20 31	2 3.5 7	(c)	(c)	(c)	(c)	(c)	(c)	(c)

(a) Assuming Equipment Cost = 10 x Catalyst Cost.

(b) Assuming Building Cost ~ 0.5 x Equipment Cost.

(c) Costing Algorithm is assumed to be inaccurate for this case (see text).

the hardware necessary for elevated temperature operation in the event that subsequent analysis or early operating experiences give evidence of the need for increased catalyst efficiency. A block diagram of the proposed detrinitiation system including some of the more important operating parameters is shown in Figure X-4. The locations of the additional equipment required for catalyst operation at elevated temperature are also shown in this figure. The estimates of the fan requirements (2 fans, each 84 inch diameter, ~ 650 rpm, ~ 200 HP) were based on a 50 ft^2 (cross-section) duct system, a linear air velocity of 2000 ft/min. , and a pressure drop of less than 0.05 atmospheres. The indicated fans are within the reach of existing technology; however, a more restricted duct system, a higher linear velocity, or a larger pressure drop would create a need for additional and/or larger fans. The combined power requirements for the heating and cooling phases of elevated temperature catalyst operation are conservatively estimated to be $\sim 10 \text{ MW}$ for 177°C air inlet and $\sim 20 \text{ MW}$ for 327°C air inlet. No credit was taken in these estimates for the exothermicity of the catalyst bed.

It has been suggested¹⁷ that the initial rate of tritium reduction in the reactor hall can be increased by designing the air flow patterns within the hall so that the mixing of clean return air with the residual air is minimized. This would lead to a higher mean tritium level in the air initially leaving the hall and would increase the gross tritium scavenging rate in the early stages of a clean-up operation.

F. Pressurized Water Handling Practices

The selection of water as a coolant for the first wall blanket and shield of the EPR raises concern regarding the consequences of tritium migration into and through the cooling circuits. The problem of tritium permeation through austenitic construction materials has long been recognized as a major area of concern for fusion power plants. Although the principal focus of attention to this problem has been on migration of blanket tritium through heat transfer circuits and eventually to the environment, the tritium in the plasma chamber is also subject to migration; hence, the absence of a breeder blanket would not necessarily obviate concern in the case of the EPR described in this report. In this section, an attempt is made to quantify the tritium permeation losses to the first wall coolant and to examine these losses in the context of existing experience with the primary (D_2O) cooling circuit of heavy water reactors.

Figure X-4. Block Diagram of the Detritiation System

The preliminary analysis of tritium permeation into the first wall coolant was based on the cooling panel design and related operating parameters described in Chapter VIII of this report. Permeation was assumed to occur through the front surface of the panel only (the surface facing the plasma). Because the back surface is at a significantly lower temperature and may either be differentially pumped to keep tritium pressures lower than in the plasma chamber or completely sealed off from the plasma chamber by welding of the entire panel perimeter, permeation through it was neglected. The front surface of the panel was taken to be 0.5-cm-thick, 316-SS and the area available for permeation (see Appendix A) was taken to be $6 \times 10^6 \text{ cm}^2$. For purposes of conservatism, (1) two temperatures that were believed to bracket the upper limit value for the average peak temperature reached in the 316-SS panel during the burn cycle (i.e., 450 and 350°C) were used in the calculations, (2) the average value of the tritium partial pressure seen by the panel was assumed to be 10^{-4} Torr, and (3) a zero tritium back pressure was assumed to exist in the coolant circuit. Permeation rates were determined using the formula:

$$\phi = \left(\frac{A}{X}\right) (\sqrt{P_{T_2}}) (\phi^0 \exp(-Q_p/RT))$$

where

- ϕ = permeation rate in cc(STP)/hr
- A = surface area ($6 \times 10^6 \text{ cm}^2$)
- X = panel thickness (5 mm)
- P_{T_2} = tritium driving pressure (10^{-4} Torr)
- ϕ^0 = permeation constant ($664 \text{ cc(STP)} \cdot \text{mm/cm}^2 \cdot \text{hr} \cdot \text{atm}^{1/2}$)
- Q_p = activation energy (15,600 cal/mole)
- T = Kelvin temperature (723 K in Case A and 623 K in Case B)

(The values of ϕ^0 and Q_p for 316-SS were derived from data in reference 11 for hydrogen. The value of ϕ^0 from reference 11 was divided by $\sqrt{3}$ as a correction for H/T isotope effects.)

Results of the permeation calculations are summarized in Figure X-5. The concentrations of tritium in the first wall cooling water circuit (capacity = 10^5 liters) for the EPR are found to be in the range of those reached in the primary (D_2O) circuit of typical experimental heavy water reactors,⁸⁻¹⁰ after a period of about two years. It is, therefore, reasonable to assume that the inventory of tritium in the EPR cooling water could be managed using practices employed in existing heavy water reactors.

MATERIAL = 316 SS
 WALL AREA = 6×10^6 cm²
 WALL THICKNESS = 5 mm
 COOLANT VOLUME = 10^5 liters
 $P_{T_2} = 10^{-4}$ Torr

Figure X-5. Tritium Accumulation in the EPR First Wall Cooling Water Circuit

The drainage, consolidation, and disposal of the entire first wall cooling water inventory (10^5 liters) every two to three years should pose no insurmountable problems, and is probably preferable (from an economic viewpoint) to installing a tritium extraction plant⁸ for the first wall coolant.

G. Comments on Blanket Processing Test Modules for Stage II

Only limited consideration was given to the tritium handling requirements for breeder blanket test modules during FY-1976; hence, little or no detail is available on the design features of the module processing systems. The report of the tritium systems subcommittee of a recent workshop²⁰ on blanket technology contains some substantive discussions of the role of breeding modules in EPR and of extraction technology in general.

The question of the meaningfulness of experiments performed under the lower wall loading and duty cycle conditions anticipated for EPRs has been considered as has the extent to which blanket processing technology could be developed outside the scope of EPR operations. The general conclusions reached on these questions are that while most of the design criteria and systems development for demonstration plant and commercial reactor blanket applications could be obtained in separate facilities, some breeding module experiments in EPRs would be prudent to assure that no important interactions had been overlooked.

With regard to tritium extraction technology for breeder blanket concepts, the status of work on a variety of materials, i.e., liquid lithium, lithium-containing alloys (Li-Pb, Li-Al, Li-Si), ceramics (Li-Al-O, Li-Be-O, Li-Si-O), and molten salts (Li-Be-F), was reviewed. Design options that employ liquid lithium with (1) stainless steel at temperatures $\leq 500^\circ\text{C}$ and (2) niobium- or vanadium-base alloys at $\leq 1000^\circ\text{C}$ were considered as were the ceramic and alloy options used with (1) minimum activation structures (graphite and aluminum), (2) stainless steel structures, and (3) refractory metal (niobium alloy and tungsten alloy) structures. Although molten salts have not received specific attention in any EPR design studies, they were found to have attractive characteristics, and the technological implications of their ultimate use in commercial reactor blankets is deserving of further

study. Comparisons of the tritium inventory in the blanket, the state of blanket tritium recovery techniques, and the ease of containing tritium within the blanket structure indicate that molten salts and ceramic materials offer prospects for meeting anticipated blanket requirements with less development than will be required for liquid-lithium blankets. Few facts are available on solid-alloy blankets, but these materials appear to approach the ceramics in terms of inventory and recovery characteristics. Although some encouraging progress has been made in recent years, the development of practical steady-state tritium-recovery techniques for low concentrations remains a major technical uncertainty for liquid-lithium blankets. The specifics of recent progress in bench scale studies of tritium extraction from promising blanket materials are more thoroughly described in reference 20.

Insofar as the ANL/EPR breeder blanket test program is concerned, the deliberations contained in the report of the tritium subcommittee at the BNL Workshop¹⁹ are expected to provide a useful point of departure for the more detailed design analysis of the EPR blanket modules planned for FY-1977.

H. Research and Development Requirements

Chapter VII of reference 1 and the previous sections of this chapter presented a preliminary design description for the EPR tritium-handling facility. It is expected that this conceptual-design activity will continue through FY-1977 and will culminate in the achievement of a detailed conceptual design for a fully integrated network of systems to contain and control tritium in a EPR. However, it is very likely that even after two to three years of rigorous parameter analysis and design study there will still remain a significant level of uncertainty concerning the actual operating characteristics of many of the tritium-handling systems and components. In order to minimize this uncertainty in the coming years and to insure that the final EPR design description has a sound technological basis, it is essential that an intensive technology-development program, amply funded and properly focused, be initiated immediately in parallel with the conceptual-design effort. Some consideration has been given to a balanced and properly-escalated program plan for tritium-handling technology to be executed over the next five years. This program

plan, which is directed primarily at R & D in support of the EPR, was described in a previous report.²¹ A summary of program content and direction for the major tritium related R & D items presented in reference 21 is given below.

1. Fueling Technology

The technology for fueling tokamaks prior to and during the burn cycle is important to operation of an EPR with good power-performance characteristics. Development of gas-injection methods to provide uniform and timely delivery of D-T mixtures in a gas-blanket fueling mode should be undertaken. In the event that gas-blanket fueling is not workable for an EPR, the technology required to form, accelerate, steer, and deliver frozen D-T pellets should be developed in parallel with the gas-blanket fueling mode. Alternative fuel schemes for small tokamaks should be identified and investigated. It seems imperative that fueling of tokamak plasmas during burn cycles be demonstrated in advance of the final fueling-system design for an EPR.

2. Tritium Storage and Transportation

There is obvious need, from the standpoint of fuel-cycle economy and overall plant safety, to develop fail-safe shipping and storage procedures for tritium. Included in this development program should be some emphasis on concentration, immobilization, and long-term storage of tritiated waste from an EPR. Implications with respect to long-term safeguards limitations must be evaluated.

3. Fuel Conditioning and Purification

Development of technology to remove all non-hydrogenous impurities from the fuel stream under closed-cycle conditions is needed. (Minimum tritium inventory in the impurity-removal system would be desirable.) Isotopic enrichment technology capable of upgrading fuel quality (increasing T/R ratios) by four to five orders of magnitude appears desirable from the standpoint of fuel cycle economy and maximized tritium recycle. Accomplishing this magnitude of enrichment will probably require development of rapid isotopomeric equilibration methods coupled with advances

in state-of-the-art cryogenic distillation technology. Methods to minimize protium insertion into the fuel stream and all levels of purge operation should be identified and developed.

4. Tritium Circulation Technology

Detailed assessments of state-of-the-art tritium-handling and transfer technology should be completed. Experimental evaluations of equipment and construction materials that are unique to fusion reactors should be initiated. The technology for complete closed-cycle operation of high-level tritium systems should be firmly established. Leak-tight pumps for tritium transfer operations over a range of pressures must be developed. Reliable compressor, valve, structural containment, and maintenance methods should be verified. Programs to supplement the existing data base for tritium-related phenomena should be expanded and upgraded.

5. Tritium Control Technology

State-of-the-art assessments in this area should be completed at the earliest possible time. Efforts currently aimed at the development of barriers to tritium migration (by permeation, leakage, etc.) should be continued and should increase as the tritium containment requirements of an EPR are more clearly established. The pertinent data base, which includes measurements of hydrogen isotope permeation rates, solubilities, effects on structural and mechanical properties, and chemical interactions, should continue to be expanded. Optimization of the kinetics of tritium trapping reactions should be carried out at an early stage so that costs associated with large scale atmospheric cleanup operations can be minimized. The technology needed to operate and process whole-room purge streams should be established, and methods for efficient recycle of tritium recovered from these streams should be identified. Computational models to predict the migratory behavior of tritium for a range of normal and accident conditions must be developed and tested at an early stage to provide guidance in establishing effective maintenance practices.

6. Blanket Processing Technology

Optimum methods for tritium recovery from liquid and solid breeder blanket materials must be identified, developed, and verified. This program should include considerations related to (1) minimum tritium-inventory

blankets, (2) minimum protium insertion into the blanket system, (3) impurity effects on tritium recovery, (4) monitoring of recovery process operation, (5) minimum impact on reactor neutronics, and (6) minimum impact on thermal performance. In parallel with the development programs, there should be an extensive effort to provide needed physical- and chemical-property data related to tritium recovery and to overall breeder-blanket performance.

7. Instrumentation and Control Technology

Instrumentation must be developed to monitor the high concentrations of tritium anticipated in the process streams and the considerably lower concentrations in purge streams. In many cases, tritium monitoring equipment will have to operate reliably in the presence of a high neutron or gamma radiation background. Design criteria and control logics for the tritium facility control center must be formulated and evaluated.

-
1. W. M. Stacey, Jr., et al., "Tokamak Experimental Power Reactor Studies", ANL/CTR-75-2, Argonne National Laboratory (1975).
 2. L. J. Wittenberg, et al., "Evaluation Study of the Tritium-Handling Requirements of a Tokamak Experimental Power Reactor," MLM-2259, Mound Laboratory (1975).
 3. J. S. Watson, et al., "Scoping Studies of Tritium Handling in a Tokamak Experimental Power Reactor," ORNL-TM-5080, Oak Ridge National Laboratory (1976).
 4. T. R. Galloway, et al., "Mirror Reactor Blankets," UCID-17083, Lawrence Livermore Laboratory (1976).
 5. J. C. Bixel and C. J. Kershner, "A Study of Catalytic Oxidation and Oxide Adsorption for the Removal of Tritium From Air," Proceedings of the Second AEC Environmental Protection Conference, April 16-19, 1974, Albuquerque, New Mexico; WASH-1332 (1974), p. 261.
 6. S. L. Hagel, "Quality Assurance Guidelines for High Pressure Gas Systems," UCRL-77864, Lawrence Livermore Laboratory (1976).
 7. C. L. Folkers and V. P. Gede, "Transfer Operations with Tritium -- A Review," UCRL-76729, Lawrence Livermore Laboratory (1975).
 8. M. Damiani, R. Getraud, and A. Senn, "Tritium and Hydrogen Extraction Plants for Atomic Power Reactors," Reprint from Sulzer Technical Review Number, Nuclex 72, Sulzer Brothers Limited, Wintersthrü, Switzerland.
 9. W. R. Bush, "Assessing and Controlling the Hazard from Tritiated Water," AECL-4150, Chalk River Nuclear Laboratories (1972).

10. J. J. Hartig, Argonne National Laboratory (personal communication).
11. V. A. Maroni, et al., "Experimental Studies of Tritium Barrier Concepts for Fusion Reactors," Proceedings of the International Conference on Radiation Effects and Tritium Technology for Fusion Reactors, October 1-3, 1975, Gatlinburg, Tennessee.
12. I. M. Flynn, et al., "Advances in Cryogenic Engineering", 1, 56(1960); 4, 464 (1961).
13. B. S. Johnson and F. Rust, "Low Temperature Still for Separation of Hydrogen Isotopes," Savannah River Plant Report, DP 732, October, 1962.
14. A. A. Moghissi and M. W. Carter, Eds., Tritium, Messenger Graphics, Phoenix, Arizona (1975).
15. N. W. Golchert and J. Sedlet, "Airborne Tritium Monitoring Near a Heavy Water Nuclear Reactor," in Reference 14, pp. 579-585.
16. J. R. Galloway, Lawrence Livermore Laboratory (personal communication).
17. L. J. Wittenberg, Mound Laboratory (personal communication).
18. E. Golankiewicz, Engelhard Industries (personal communication).
19. See for example, descriptions of the Space Power Facility, NASA Lewis Research Center, Plum Brook Station, Sandusky, Ohio
20. J. Powell, et al., "Proceedings of the Workshop on Blanket/Power Systems for Fusion Reactors," Brookhaven National Laboratory, March 29 to April 2, 1976 (in press).
21. F. E. Mills, et al., "Research and Development Required for the Tokamak Experimental Power Reactor," ANL/CTR/TM-65, Argonne National Laboratory (May, 1976).

CHAPTER XI

INSTRUMENTATION AND CONTROL

The Instrumentation and Control System for the EPR is divided into four major subsystems which are:

1. Data Acquisition System - to acquire, record, time correlate, analyze and display pertinent data.
2. Reactor Control System - to control the processes required for startup, heating, normal burn, and shutdown.
3. Reactor Protection System - to protect the public and the EPR plant by shutting the reactor down and initiating those processes which will limit the consequences of an abnormal event.
4. Instrument Integration System - to connect the data acquisition, control and protection systems with one another.

Instrumentation and control for specific processes may belong to all four subsystems, e.g., a blanket temperature sensor output will be sent via the instrument integration system to the data acquisition system and the reactor control (blanket coolant) system and if it passes a maximum limit will activate the reactor safety system. Before these systems can be designed, specific instrumentation and control requirements for each process system must be specified.

A scoping study was undertaken to identify the requirements for instrumentation and control. These requirements are discussed below for some selected process systems.

A. Tokamak EPR Plasma Diagnostics

Plasma diagnostics will pose many problems for EPR because of the high accuracy required for plasma measurements and the many constraints imposed. Some of these constraints are: (1) high radiation levels will interfere with the operation of ionization-sensing devices such as scintillators and solid state detectors, (2) high radiation levels will activate diagnostic equipment and make adjustment, calibration, and repair difficult, (3) high magnetic fields will necessitate shielding, and (4) the dimensions of the toroidal magnets require most equipment to be physically far from the plasma. In

addition to these considerations, all diagnostics should be fully operational when the EPR is first turned on, so that valuable time is not wasted because of these (comparatively) inexpensive elements.

Although the experience gained in IFTR experiments will be essential in developing diagnostics for EPR, integrated radiation levels will be considerably higher in EPR, and this will impose additional problems. The most serious seems to be that x-rays from sustained running at high temperatures ($T_e = 5$ keV, Dose $\sim 10 \frac{J}{cm^2}$) will cause browning of glass, which could make the Thomson scattering optics useless.¹ Thus, although measurements can be made during startup, this system must be carefully shielded from hot plasma during the burn.

A preliminary survey of the basic plasma measurements shows that it will be difficult to obtain much information from the interior of the plasma during a thermonuclear burn, other than that given by neutron and x-ray fluxes, and x-ray measurements will be difficult² due to the high neutron flux. This will make study of plasma stability, temperature profiles, etc., very difficult. Plasma diagnostic requirements are shown in Table XI-1.

The plasma position could be sensed and controlled by means of a system similar to that used in the CLEO tokamak.³ The position of the plasma can be sensed both by magnetic loops and x-ray measurements, and this information can be processed by a computer which controls trimming coils, which are able to provide a small vertical or horizontal magnetic field in the plasma. All these coils should be close to the first wall so that the magnetic field can penetrate quickly from the plasma to the sensing coils and from the control coils back to the plasma.

Eddy currents in the blanket/shield (which has a magnetic time constant of ~ 50 ms) will also cause some problems with magnetic probes; and the first wall and blanket near these probes may have to be designed to minimize eddy current effects.

Data collection and handling from all diagnostics and control circuits will also be a major problem, as all data reduction will have to be done on line. Recent experience in high data rate experiments in high energy physics

Table XI-1. Plasma Diagnostic Requirements

Plasma Parameters	Start-up	Burn	Diagnostic Techniques
Plasma Current (Total)	o	o	Rogowski Coil
Current Density Dist.	o	o	Charged Particle Probes
Electron Drift Velocity	o	o	T
Poloidal Field	o	o	Magnetic Probes
Plasma Position	o	o	Cosine and Saddle Coils, X
Plasma Loop Voltage	o	o	Loop
Plasma Pressure	o	o	Diamagnetic Loop
Plasma Density (Average)	o	N	X, S, T, I, Injected Neutrals and Ions
Local Electron Density	o		T, I, Injected Neutrals and Ions
Density Fluctuations	o	N	Injected Neutrals and Ions
Average T_i (core)	o	N	M, C, P
Local T_i	o		M
Fluctuations in T_i	o	N	M, P
Average T_e	o	X	T, X
Local T_e	o	o	T
Fluctuations in T_e	o	o	X
Impurity Concentration $f(r,t)$	o	o	X
Impurity Identify	o	o	X
Space Potential	o	o	Injected Ions
Neutral Density (core)	o		Charge Exchange
Neutral Density (edge)	o	o	Charge Exchange
Neutral Gas Pressure	o	o	Fast Ion Gauge
Neutral Energy Spectrum (normal burn and injection)	o	o	Neutral Analyzer
Integrated Burn Intensity		o	Neutron Monitors
Burn Intensity		o	Neutron Monitors
Fluctuations		o	Neutron Monitors
Neutron Energy Spectrum		o	Neutron Monitors
Runaway Properties	o	o	Hard x-ray detectors, pulse height

o = All listed diagnostics give some information

N = Neutron production will be primary source of information

X = X-ray measurements

S = Synchrotron Radiation

T = Thomson Scattering

M = Microwave Scattering

I = Interferometers

C = Charge exchange

P = Production of neutrons from T + T

has shown that the most efficient control and monitoring systems generally require many small specialized mini-computers and hard-wired microcircuitry. These systems are generally superior to a few larger, general purpose computers because computation can proceed in parallel, and the individual systems can be designed to optimize specific types of data collection and computation. CAMAC systems seem to provide the required flexibility for handling a variety of monitoring, control, and data transfer problems. If the majority of control circuitry can be done with standardized CAMAC systems and with modest extensions of CAMAC, using microprocessors in control logic, then designs will be simplified.

In the next few years, it may be possible to incorporate large, fast-buffer memories in detectors. This might simplify problems such as reducing TV-type scans of optical information to a more manageable and useful digital form.

Considerable research and development is already underway towards developing diagnostics and data systems for TFTR, and the results of this program will apply directly to the EPR.

5. First Wall, Blanket and Shield Systems

The instrumentation and control components for the blanket and shield cooling system are comparable to related conventional pressurized light water reactor (LWR) components. The environment in which these components must operate is similar to that in LWRs, except that in some locations the EPR neutron flux may be several times greater than in an LWR. The presence of tritium will require equal or better sealing, and the vibration and mechanical shock may be many times that experienced in LWRs.

Thermocouples (room temperatures to 1200°C) are used to measure the temperatures of the first wall, blanket and shield walls, as well as the coolant water inlet and outlet temperatures at the individual block manifolds. Since there are over 500 first-wall coolant panels, 272 blanket blocks and 416 shield blocks, there will be a minimum of 5000 thermocouples for the direct control functions. Since access to the reactor is difficult, redundancy in all key instrument sensors is required. Additional thermocouples are needed for diagnostic data to help in locating failed components. Temperatures are measured at the large, 15 cm diameter coolant pipes

entering and leaving the reactor, and at heat exchangers, tanks and other primary and secondary coolant system components. The total number of thermocouples associated with the cooling system may well exceed 15,000. All the thermocouple output goes to the Data Acquisition System, while only key data will go to the Reactor Control System and the Reactor Protection System.

The major problems expected will be in the temperature sensing system. The principal uncertainty with respect to thermocouple response relates to effects on signal output of very large magnetic fields and of radiation damage. Induced currents in the wires may give unreliable temperatures and calibration could change as a result of neutron damage to the alloy at the hot junction.

To measure flow rate of coolant, venturi or turbine flow meters are used. It is desirable to measure flow at the inlet manifold and outlet manifold of each block in the first wall, blanket and shield to diagnose leaks and to provide a redundant control element in addition to temperature monitoring. A minimum of 1200 flow sensors is required. The output from all sensors goes to the Data Acquisition System. Outlet flow data goes to the Protection System and inlet manifold flow data goes to the Control System. Loss of flow as measured by the outlet manifold may require protective action, while low inlet flow may require just flow control adjustment.

Coolant pressure measurements on both the primary and secondary coolant systems are required. Because the systems must be pressurized to prevent boiling during operation, the absolute pressure must be monitored continuously. Since pressure drop measurements are important as diagnostic data as well as in the control function, reliable pressure sensors must be used. Many types have been used successfully, including eddy current devices and piezo-electric devices. If these devices can be placed in regions of moderate magnetic field and low neutron flux, as expected, then no major problem should be encountered.

Conventional nuclear reactor sensors are required to measure neutron and gamma radiation at various locations in the reactor. Radiation flux mapping is used to evaluate actual vs calculated dose for component lifetime and induced activation. These data are used also to meet experimental

and diagnostic needs. Radiation measurements in the blanket and shield of the EPR, unlike fission reactors, are not as critical for control, but they could be used as a backup (or diverse) signal for the Reactor Protection System.

Conventional radiation sensors face the same problems as those presented for the diagnostics, viz. high radiation fields, high magnetic fields and difficult access, particularly at the inside of the torus.

Other instruments required are those for the measurement of liquid level, pH, mechanical displacement and radioactivity, as well as those to detect leaks and to measure the effects of vibration and mechanical shock.

The total number of sensors required for the first-wall, blanket and shield cooling system may exceed 25,000.

C. Tritium Facility

Reliable operation of the tritium handling facility for the EPR will require a wide variety of monitoring and diagnostic equipment. Atmospheric and purge jacket tritium detection systems with fast response characteristics will be used to effect fully-automated emergency shutdown procedures in the event of any significant unscheduled tritium releases. Because of their proximity to the reactor itself, many of these monitoring systems will have to operate with high sensitivity in a fairly intense nonuniform radiation background (mostly gamma). Other types of diagnostic equipment will be in continuous use to monitor fuel cycle processor performance. The fuel cycle instrumentation must be capable of quantitative discrimination to determine fuel stream composition, isotope ratios (H/D, H/T, T/D), non-metallic element concentrations, temperature, pressure, flow rate, etc. Because of the need for rapid fuel turnaround (to avoid a large tritium inventory), the fuel cycle control system will be a fully-automated programmable unit equipped with a high-speed feed-back logic network. Included in this network are all pressure, temperature and flow control hardware (valves, pumps compressors).

The cryogenic distillation columns, which comprise the isotope enrichment cascade, will be equipped with individual temperature and total pressure controls. Temperature control requirements are roughly $\pm 0.01^\circ\text{K}$ for temperatures in the range from 20 to 30°K . The adjustability of column

temperature profiles, total pressures and reflux ratios will permit retrofitting of column performance to accommodate variations in input stream compositions.

The nature of the failure response systems remains to be clearly defined, but undoubtedly these systems must be capable of detecting and mitigating all plausible fuel cycle malfunctions. This includes: (1) isolation and/or complete shutdown of the affected systems to minimize tritium releases to the purge circuits, the reactor hall and, ultimately, the environment; (2) start-up of or acceleration of appropriate clean-up systems; and (3) incident surveillance and analysis to evaluate the nature and extent of the malfunction and to provide guidance in selection of the requisite corrective actions.

D. Plasma Support Systems

For the plasma support systems the reactor control system performs two basic functions: (1) process control in which electrical and mechanical transducers are used to monitor temperatures, liquid levels, etc., for the purpose of making automatic or manual adjustments to maintain proper support for operation of equipment, and (2) functional command in which timing pulses, voltage and power levels, etc., are instantaneously controlled as part of the total fusion operating sequence. The reactor safety system is designed such that some form of fault protection triggers an immediate chain of events to bring equipment to a safe quiescent state in the shortest practical time.

The process control system would use standard measuring instruments such as pressure transducers, voltmeters, current shunts, thermistors, liquid level indicators, etc. Information would be collected with a sample-and-hold computer system updated at a rate consistent with suitable performance of operating equipment. Process-type control is needed on most plasma support systems. Some systems, such as gas pressure and arc current in the neutral beam source, need functional response from the monitoring system to improve operating conditions. Other systems, such as cryogenic liquid level in the vacuum pumps, require start-stop control based on threshold indications.

Timing and active feedback are two basic types of functional controls. Timing control for proper phasing of various functions during the fusion reactor cycle come from a master controller, usually in the form of logic level signals which can be manipulated within the controlled device. Active feedback control is needed on many systems. For instance, the switching frequency and relative phasing between bridges on the EFC inductor-converter are a function of the instantaneous plasma current and temperature. This type of control must be accomplished with instantaneous error correction between reference requirements and fast-acting transducers.

The emergency safety controls use the same type of measuring equipment, voltmeters, current shunts, etc., that are used for process control. Emergency safety controls respond immediately upon activation, rather than waiting for a computer update. Signals such as voltage transient in the EF inductor-converter or vibration detection on the homopolar generators must produce an immediate chain of events to avoid or minimize damage to equipment and personnel.

E. Magnet System

An EPR magnet system consists of the TF coil, OH coil and EF coil, the cryostats for these coils, the helium refrigeration/liquefaction and its associated cryogenic liquid/gas storage and handling system, the vacuum system, the power supply, and instrumentation and control systems. The instrumentation and control system will adjust process conditions in both transient and steady state operation, detect system malfunctions, trigger alarms, and interlock with the protection systems. The instrumentation and control system will serve to monitor and control the magnet and cryostat cool-down, the checkout of each subsystem, the normal operation of these systems, and the failure protection and the safety precautions for these systems.

The monitoring parameters for a superconducting coil are the temperatures of coils, the terminal voltage and current, the magnetic field, the strain in the conductor and reinforcing structural material,

the current lead potential, temperature and gas flow. The monitoring parameters for a magnet cryostat are the insulation vacuum, the pressure and temperature in the liquid nitrogen vessel and in the liquid helium vessel, the liquid level of nitrogen and helium, and the strain in the cryostat structure. The monitoring parameters for the refrigerator liquifier facility are the pressure and the temperature of the expansion engine and the Joule-Thompson expansion valve, the pressure and temperature indicators for compressor operation, the pressure in the high pressure gas tank, the pressure and liquid level in the liquid helium storage dewar. The monitoring parameters for the magnet energy dumping system are the voltage across the discharge resistor and the water-level of the dumping resistor. Table XI-2 lists the system component, the parameter monitored, the type of sensor and the sensor location.

For the magnet and cryostat cool-down, the monitored parameters required include the temperature at various locations within the coils, the pressure and temperature in the radiation shield vessel and the helium vessel, the insulation vacuum, the coolant transfer rate, the key normal parameters for operation of the cryogenic facility and liquid level of the liquid nitrogen and liquid helium in the cryostat and storage dewar.

In the magnet, cryostat and power supply protection system, some of the necessary alarm and interlocks in the control room are listed in Table XI-3. It is important to point out that the most dangerous hazards for both TF coils and poloidal coils are the open circuit, arcing and the coupling effect of the poloidal coil system. The latter requires additional study in order to offer reasonable protection and precautionary measures. The effect of poloidal fields on the TF coil must be thoroughly investigated before a meaningful instrumentation and control system can be designed.

1. J. F. Kircher and R. E. Bowman, Effects of Radiation on Materials and Components, Reinhold Publishing Corp., New York (1964), p. 372.
2. K. Young, PPPL, private communication, 1976.
3. J. Hugill and A. Gibson, "Servo-Control of Plasma Position in Cleo-Tokamak," Nuc. Fusion, 14, 611 (1974).

Table XI- 2. Instrument Requirements for the Magnet System

System Components	Monitoring Parameters	Sensors	Sensor Location
Magnet Coils	Temperature	Carbon Resistor/P Resistor/Thermocouple	Several Places in Winding
Magnet Coils	Terminal Voltage	Potential Taps	Magnet Terminal
Magnet Coils	Magnet Current	Magnet Current Transducer/Shunt	Current Lead
Magnet Coils	Magnet Field	Hall Probe	Any convenient location
Magnet Coils	Conductor Strain	Foil Strain Gauge	High Strain Locations
Current Leads	Temperature	Thermocouple	Top and Bottom Ends
Current Leads	Voltage	Potential Tap	Top and Bottom Ends
Current Leads	Gas Flow	Mass Flowmeter	Gas Manifold
Magnet Cryostat	Vacuum	Vacuum Gauges	Cryostat Outer Shell
Magnet Cryostat	Pressure	Bourdon Gauge	LN ₂ Reservoir
Magnet Cryostat	Temperature	Thermocouple	Radiation Shields
Magnet Cryostat	Liquid Level	Oxygen Gas in Bellow Enclosure	LN ₂ Reservoir
Magnet Cryostat	Pressure	Bourdon Gauge	LHe vessel
Magnet Cryostat	Temperature	Silicon Diode	LHe vessel
Magnet Cryostat	Liquid Level	NbTi wire	LHe vessel
Magnet Cryostat	Strain	Foil Strain Gauge	Magnet Structure Support
Cryogenic Facility	Pressure	Bridge Transducer/Boughton Gauge	Expansion Engine
Cryogenic Facility	Temperature	Silicon Diode	Expansion Engine
Cryogenic Facility	Pressure	Bridge Transducer/Boughton Gauge	J-T Valves
Cryogenic Facility	Temperature	Silicon Diode	J-T Valves
Cryogenic Facility	Pressure	Boughton Gauge	Compressor High and Low Pressure side
Cryogenic Facility	Temperature	Thermocouple	Compressor
Cryogenic Facility	Pressure	Boughton Gauge	Gas Storage Tank
Cryogenic Facility	Liquid Level	Nb-Ti Wires	Storage Dewar
Cryogenic Facility	Pressure	Boughton Gauge	Storage Dewar
Energy Dumping System	Voltage	Digital Voltmeter	Dumping Resistor
Energy Dumping System	Water-Level	Any Floating Substance	Dumping Water Tank

Table XI-3. Toroidal Magnet Alarm and Interlock Actions

I. Abnormal Function Alarm

1. Pressure of insulating vacuum exceeds 10^{-4} Torr.
2. Abnormal helium supply dewar pressure.
3. Liquid helium at cryostat "Emergency" low level.
4. Helium level below the coil end.
5. LN_2 shield temperature high - select the sensor at the hottest shield temperature region.
6. Low dump resistor water level.
7. Power supply series diode temperature exceeds a certain limit.
8. He gas mass flow rate for magnet leads below a certain amount after magnet is fully charged.
9. Voltage across magnet leads exceeds a certain limit.
10. Magnet lead temperature at room temperature end exceeds a certain temperature value, e.g. $30^{\circ}C$.
11. Resistance in uppermost conductors exceeds a certain value corresponding to conductor temperature of $15^{\circ}K$.
12. Magnet energy discharges through discharge resistor R_0 .

II. Conditions Which Warrant Magnet Energy Discharge

1. Pressure of insulating vacuum exceeds 1×10^{-3} Torr.
2. Magnet lead temperature at upper end exceeds $40^{\circ}C$.
3. Temperature in uppermost conductors exceeds a hot temperature limit e.g. $273^{\circ}K$.
4. Normal region developed in such a degree that V_R exceeds 1 V.

III. Conditions Which Require Power Supply Shutoff

1. Over operational current by 0.5%.
2. Power supply series diode temperature too hot.
3. Charging voltage exceeds certain maximum which will be established during magnet testing.

CHAPTER XII

REACTOR ENGINEERING SYSTEMS

Three major engineering aspects of the EPR are herein described; those associated with reactor component support and torque reactions; the assembly of the reactor, its requirements, sequence and schedule; and the remote maintenance systems. The development requirements for these systems are also described briefly. With exception of the magnet support and torque frame which was studied at some length, the systems descriptions are brief, mostly addressing a design approach that offers potential solutions to all major systems requirements.

A. Structural Support Systems

Two areas of structural support are described; those associated with the blanket, shield, vacuum vessel components; and those structures supporting the various magnet coils.

1. Blanket/Shield System Support

Support of the blanket/shield system is made through sixteen welded beam trapezoidal shaped structures mounted horizontally on adjustable columns (32 total) to a sub-basement area (see Figure XII-1). Each structural unit supports 1/16 of the weight of the reactor and can be independently lowered and raised approximately 2 meters to facilitate assembly and replacement of blanket and shield blocks. The upper portions of the support frame within the influence of the magnets are made of stainless steel.

Each support frame has five support struts which contact the lower shield blocks. The shield blocks are attached to the frame through electrically insulated fasteners. The lower portions of the blanket nests as a lining in the lower shield and, through insulated roller pads, supports the reactor vacuum vessel (Figure IX-24). Remaining blanket and shield blocks are joined in an arch-like support encasing the upper portion of the vacuum vessel. The total support of the blanket, shield and first wall system, (a total weight of ~ 8000 tonnes, is carried through these supports to the building foundation pad.

Figure XII-1. Shield Support Framework

Attached reactor ancillary components such as the cryosorption pumps of the vacuum system and the neutral beam injectors are supported independently of the shield. The upper vacuum pumps rest on supports attached to the magnet support frame. A bellows provides the flexure required for operating thermal expansion.

The neutral beam injectors are structurally separated from the reactor through an accordin shaped bellows mounted in the interconnecting duct with the injector weight being supported by the reactor floor. If necessary the injection apparatus may be programmed to follow the first wall vessel thermal expansion to minimize beam alignment problems.

2. Magnet Coil Structural Support

Two structural support concepts, a torque shell design and a torque frame design, were developed and analyzed.* Both provide support for the poloidal and toroidal field coil magnets. These concepts differ primarily in their method of restraining the large lateral forces induced on the toroidal field (TF) coil. Detailed definitions of these designs, component weight, induced loads, materials considered and the analyses conducted in developing these concepts are provided in Appendix D and in Reference 1.

a. The Torque Shell

The torque shell concept shown in Figure XII-2 uses 16 shear webs to provide continuous support for the TF coil and to accommodate the large torques induced on the TF coils. Other major features include an overhead tree for support of the upper poloidal coils, an outer support system for the large poloidal coils, a lower poloidal coil support frame with a build-in rewinding facility and four lateral stabilization fittings which attach to the shear panels to provide stability for any unsymmetric loads. The upper and lower outer poloidal coils can be raised or lowered respectively to permit the removal of a TF coil.

* Work performed by McDonnell Douglas Astronautics Company - East.

Figure XII-2. Torque Shell Structural Support

Each shear web consists of an upper, lower and central access panel. The panels have single curvature and attach to the center of the TF coil through a flange that is butt welded to the coil cryostat. These panels are bolted in place through match drilled holes. Normal maintenance will be accomplished by removing the central access panel. This removal is estimated to take ~ 4 hours. To minimize the impact on overall maintenance time, panel installation/removal will be performed in parallel with other maintenance operations. The level of radiation at the external surface of the shear panels is expected to be low enough to allow removal of the fasteners with a minimal amount of radiation protection if the blanket and shield are left intact. Support is provided for the vertical loads of the blanket/shield and TF coil using the same concepts as described for the torque frame. The outer poloidal coil support is also similar to the torque frame with supports provided on the columns for the vacuum pumps.

b. Torque Frame Concept

This concept was analyzed as a design option. It uses a frame and shear panel combination to accommodate both the large torque induced at the top of the TF coil and the large vertical forces induced in the upper poloidal coils. A similar frame and shear panel structure is used to support the lower poloidal coils and to accommodate the torques induced at the bottom of the TF coil. These frames are shown in Figure XII-3.

The upper torque frame consists of a shear plate to which concentric rings are attached that provide continuous support for the upper poloidal coils and support the TF coil lateral forces at 8 points on each TF coil. Support for the vertical loads of the poloidal coils is accomplished by radial beams which rest on the center concrete core and are supported around the periphery of the reactor by the outer support columns located at each TF coil. These outer support columns also provide support for the outer poloidal coils.

The blanket/shield and first wall are supported by a frame which is provided as part of the Primary Energy Conversion System (PECS). This frame is supported by 32 columns mounted in the floor of the lower access pit. The 4 lower poloidal coils are supported by a frame assembly that attaches to the blanket/shield support columns. This frame transfers the upward

Figure XII-3. EPR Torque Frame Structural Support

acting induced coil forces to the support columns during reactor operation and supports the dead weight loads of the coils. Since the coils are captured by the support columns, jacks are incorporated to allow lowering of the support beams and coils for repair. A separate support is provided for the four upper poloidal coils which consists of sixteen beams that span between the center core column and the outer poloidal coil support columns. Cross members provide continuous support for the poloidal coils. The two sets of outer poloidal coils are supported by fittings attached to the outer poloidal support columns. Each column incorporates a set of jacks (elevators) which allow raising and lowering the outer coil support fittings for additional access. The fittings also serve as a handling fixture, in that the overhead crane can be attached to pick up the assembly if coil removal is required for repair.

The 16 TF coils assemble into a common cryostat around the center column of the reactor. Support for the cryostat is provided by the use of 8 columns located around the center core and 16 additional vertical supports, one located under the outer portion of each coil.

Lateral TF coil forces are transferred into the torque frame by the lateral restraint fittings and are in turn transferred by the torque frame to four primary tension members. These four members are located in a horizontal plane approximately 15 m above the floor and extend 38 m to the outer wall of the reactor building. These tension members are pinned to the upper torque frame and can be readily disconnected and placed in storage positions against the building wall. Normal maintenance operations (those requiring less than 15 m overall height) can be performed without removing these members.

The lower torque frame is similar to the upper frame except it is located between the TF coil and the lower poloidal coils. This frame location allows lowering of individual coils to the rewinding turntable without disassembly of the structural frame. The shear plate for this frame is offset slightly to allow clearance of the TF and lower poloidal coils. This frame is supported in the vertical direction by the blanket/shield support columns and ties to the access pit wall. By releasing of these supports, the frame assembly and magnets can be lowered to allow magnet repair. Both the vacuum pump and brackets must be removed prior to lowering the magnets for repair.

c. Concept Comparison

The torque shell concept provides more continuous support for the TF coil which will allow use of thinner TF coil cryostat structural walls and result in less eddy current heating.

The radial beams used for overhead poloidal coil support were shown in different locations for the torque frame and torque shell concept to show the effect of TF coil electrical connector location on structure design. The straight beam aligned with the TF coil and other support column results in a simpler, slightly lower cost design for the torque frame concept.

All normal maintenance of the blanket and shield will be accomplished from the side of the reactor. Both concepts provide equivalent access to the magnets and blanket/shield components. The torque frame concept with the overhead tension ties allows direct access for normal maintenance. In comparison, the torque shell concept requires removal of 1 to 16 central access shear panels.

The total weight of the structural support is 240 and 358 tons, respectively, for the torque shell and torque frame designs. The heaviest item that must be lifted by the reactor overhead polar crane is the 245 ton upper torque frame and poloidal coil assembly when using the torque frame concept. The torque shell concept has a 191 ton outer poloidal coil and support frame assembly as its heaviest item. Weights (and costs) were determined based upon preliminary load data. As new, worst case loads are defined, these weights may increase somewhat, however no significant impact on the design is expected.

Analyses were conducted to establish the feasibility of the concepts and to provide a consistent basis for concept comparison. Structural analysis of support components was facilitated by use of finite element computer programs and computer graphics capabilities. Guidelines were established for allowable stress levels based on the Steel Construction Manual.²

<u>STRESS</u>	<u>ALLOWABLE</u>
Tension	$0.6 F_{ty}$
Shear	$0.4 F_{ty}$
Comparison	Buckling, $0.6 F_{ty} \text{ Max.}$

These stress levels were applied in conjunction with material yield stresses with the induced magnet loads and dead weight loads without additional factors of safety. The effect of fatigue for 1 million cycles did not require additional reduction in the allowable stress level. The stress levels used are compatible with the structural design criteria per ASME Codes III, Subsection NF - Component Supports,³ for both design concepts. The torque shell concept has been tentatively selected as the reference design option.

B. Reactor Assembly

A stepwise plan for assembly of the reactor was developed and the initial requirements and preassembly status were defined to aid in generating the assembly schedule. This was a valuable planning exercise for it provides insight in defining requirements and specifications for various types of remote handling equipment, maintenance operator skills, ancillary mockup apparatus, storage and assembly space allotments etc. as well as pertinent initial component design input.

1. Prerequisites - Pre-assembly Status

The reactor building is under roof with reactor pedestal installed, and 300 ton capacity overhead crane and sub-reactor assembly equipment readied. The remote maintenance building is under roof with a mock-up of the reactor ($\sim 1/5$ segment of torus) available for demonstrating actual procedures for installation and assembly of all reactor components. Three toroidal-field coil production lines capable of supplying one coil each, complete with cryostat, every 40-50 days are provided on site, with 225-ton crane access to the entrance to the reactor building. Inside the reactor building a coil winding apparatus is being set up for the 80 foot diameter equilibrium and ohmic-heating coils (weight to 150 tons).

On hand are the reactor center-core support, the inner and lower ohmic-heating and equilibrium coils, at least one of the finished toroidal-field coils and the prefabricated pieces of the center toroidal-field coil system, vacuum wall and various magnet restraint and support structures.

2. Assembly

a. Initial Assembly

The inner core support structure is positioned and secured (Step 1, Figure XII-4), and the lower-inner-EF coils and OH coils are lowered into a lower holding position below the support structure (Step 2, Figure XII-4a). There is no way to install these pieces after the TF coils are mounted without winding them in place.

The first TF coil is brought into position and mounted permanently against the inner support column (Step 3, Figure XII-4). This procedure is followed by the second, third and fourth TF coils installed at the rate of one every 40 days, which includes frequent interim tests and integrity checks to guarantee quality of every step of fabrication. The central core OH solenoid is installed and the central vacuum jacket is attached, and the TF coils are positioned as they are delivered (Step 4, Figure XII-4).

Delivery of the prefabricated sections of the first wall (1/4 segments) is made with accompanying pieces of the blanket and shielding blocks. The shield support structural frames and columns, along with the lower magnet support/torque frame are also on hand.

Procedures will be established for removal and reassembly of each component and assembly process (e.g., welding, fitting, adjustment, etc.) in preparation for any repairs or modifications necessary once the reactor has been operated and becomes inaccessible to direct personnel contact because of long life material activation.

b. Intermediate Assembly

After four of the TF coils have been installed, a 1/4 section of the first wall is threaded through the assembled TF coils and supported in place (Step 5, Figure XII-5). Assembly of the TF coils continues as rapidly as possible in parallel with the first-wall installation. With the first-wall section held in position, the support structures and lower portions of the blanket and shield are installed using procedures and apparatus developed from the mock-up. The lower sections are followed by upper section installation. All pieces are tested as they are installed.

Figure XII-4. Reactor Assembly Sequence

STEP 5 STEP 6 STEP 7 STEP 8

Figure XII-5. Reactor Assembly Sequence

With a 1/16 portion of the torus covered with blanket and shield, the vacuum port extensions with their associated valving are installed

The assembly continues by installing three to four TF coils, then a 1/4 section of the first wall, adding blanket and shield sections, and installing vacuum ports until only two TF coils and a final 1/4 section of the first wall remain. Each quarter section of the reactor will take approximately 8 months to assemble. The last sections are installed by placing the TF coils close together, threading the first wall section and repositioning the TF coils (Step 6, Figure XII-5).

Adding inner wall liners and connecting the first wall, blanket and shield coolant manifolding proceeds concurrent with the reactor assembly.

c. Final Assembly

With the last TF coil in place and all testing satisfactorily completed, the outer 80-foot diameter EF and OH circumferential coils, which have been fabricated within the reactor building, are positioned over the completed basic reactor. The final coils are placed in the upper positions and the entire magnet restraint/support system locked into position (Step 7, Figure XII-5).

Addition of the neutral-beam duct extensions, RF heating systems and the fuel-injector port extensions complete the basic reactor (Step 8, Figure XII-5).

Completion of the coolant, electrical supply leads, cryogenic connections and instrumentation and controls and diagnostic sensors prepares the reactor for the final installation of cryosorption pumps, neutral-beam injectors, RF heating wave guides and refueling systems. Weights of major reactor components are listed in Table XII-1.

3. Schedule Sequence

An elapsed period of 32 months is estimated for assembling the reactor, performance system checkout and preparations for initial operation. This schedule assumes two shift operation during the entire assembly period, leaving the third shift possible for repairs and problem resolution.

Table XII-1. Weights of Major Reactor Components

Major Component	Quantity	Size	Weight/unit
TF Coil	16	14 m H x 8.5 m W x 1.3 m thick	210 tonnes
OH Coil	1	solenoid 12 m H x 1.6 m Dia.	160 tonnes
	10	(max ring) 23 m Dia.	14 tonnes
EF Coil	16	(max ring) 24 m Dia.	54 tonnes
First Wall Sector	16	22.5° of 13.5 m Dia. torus	22 tonnes
Blanket Block	416	average 1 m x 1 m x .28 m thick	2 tonnes
Shield Block	272	average 1 m x 1 m x .8 m thick	6 tonnes
Vacuum Pump & Accessories	64		15 tonnes
Neutral Beam Injectors	16	9 m L x 4 m W x 7 m H	200 tonnes

C. Remote Maintenance

Remote maintenance, repair and modification are serious considerations which must be factored into the initial design of the reactor. The approach to these activities in the EPR design is to consider that all maintenance operations are done with semi-remote to fully remote operations. A large number of remotely operated machines and apparatus have been identified along with the major support facilities. In addition two specific reactor repairs are explained in a stepwise manner.

1. General Approach

All maintenance and repair operations in and near the reactor are assumed to be accomplished with remotely operated apparatus. This apparatus may be anything from a 3 to 5 ft extension for an impact wrench up to an automated robot which traverses the interior of the reactor vacuum vessel. Various types of equipment have been identified. Broad categorization of these places them as follows: internal machines (Reactor); external machines and support facilities.

a. Support Facilities

The major support facility is the full size 1/5 to 1/4 segment mockup of the reactor. This facility is the focal point of all assembly, disassembly and repair work on the reactor, beginning with the start of reactor assembly. All procedures, assemblies, machines and techniques will be established on this mockup prior to any final reactor installation or assembly. The mockup is kept available at all times, and maintenance tools and procedures are practiced prior to reactor activities.

Two hot cells and a neutral beam test stand are provided for maintenance, examination and repair of radioactive reactor components. Basic coil winding facilities are provided beneath the reactor to rewind OH and EF coils that cannot readily be removed. The polar crane has a special, shielded, manned carriage fitted with apparatus for manipulating reactor components safely in high radiation background.

b. Internal Machines

Several pieces of apparatus are specially designed to handle maintenance and repair of the reactor vacuum vessel. Since the vessel is permanently mounted all major maintenance operations will be performed with remote operating machines working inside the vessel. Four experimental/maintenance ports of 1.5 m diameter, with quick opening features, are provided for entry of the maintenance repair equipment. Mechanical access to the reactor is attainable in less than five hours. The vacuum and neutral beam ports can also be used for maintenance access.

Basic conceptual design of internal apparatus is depicted in Figure XII-6. This consists of accordion structures positioned through the vacuum ports to form a central rail support from which polar type manipulators that reach all areas of the inner torus can be mounted. This apparatus is supplemented with mobile cart type vehicles which are placed into the toroidal chamber and travel on the vessel wall. The machines are capable of inspections, removing and replacing wall panels and manifolds, repairing the vessel wall by cutting and welding fittings, recoating the inner surface, and other operations. All equipment is tested on the mockup and programmed to the degree practical. All operations are observed on video equipment while being guided by duplicate master manipulators from the mockup

Figure XII-6. Basic conceptual design of internal remote repair machine

itself. Retrieval equipment is designed to retrieve any inserted equipment, as a backup.

c. External Machines

Reactor maintenance is performed with three types of equipment operating overhead, horizontal (floor mounted) and beneath the reactor. The polar crane, bridge and special remote maintenance cab comprise the overhead equipment. Up to 6 crane hoists and four manipulators are provided. Maintenance operations using this equipment include capability to manipulate the reactor torque shell or frame, access panels, upper vacuum port apparatus, neutral beam injectors, magnet power supply leads, reactor blanket and shield blocks, coolant piping, etc.

Floor mounted equipment includes vehicle mounted snorkle type units with both shielded and unshielded platforms for manned and remote operation in various sizes. These vehicles will be programmable and when trained to perform an operation on a mockup, will repeat that operation on the reactor. Special heads are provided to manipulate, inspect, repair, remove, and replace power lead connections, cryogenic lines, blanket and shield blocks, coolant lines, and other components of reactor. Portable shield walls are available to set up in areas where hands-on repairs are determined reasonable.

Sub-reactor equipment is designed to facilitate operations required beneath the reactor. Both snorkel and raised platform type equipment are utilized. Major sub-reactor operations anticipated are: vacuum system maintenance and repair and vacuum chamber inspection; coolant piping removal and repair; magnet coil lead connections; and magnet coil repairs including in situ rewinding. An access tunnel is provided to carry radioactive components to the hot cells for repair or to storage facilities.

The presence of tritium in the reactor room is assumed, and properly suited personnel are outfitted to operate with breathing apparatus. (All maintenance and repair activities are performed by properly protected personnel.)

2. Specific Repairs

Two specific repairs have been considered: (1) repair of a section of the first wall which has been damaged; and (2) replacement of a lower (difficult access) blanket block. The first wall, if it should sustain a rupture, would be repaired from within the vessel in what is believed to be a relatively simple and quick manner in contrast to disassembly of large sections using remote operated apparatus.

Without disassembling the reactor, the following activities are required for repair of a hypothetical 0.1 m x 0.5 m failed portion of the first wall coolant panel and the vacuum vessel immediately behind it:

- (1) Remove shield plug in one of the four 1.5 m diameter ports any time from 3 to 6 hours after reactor shutdown and tritium decontamination.
- (2) Damage (leak detection) location detection activities can begin, utilizing the external coolant circuitry. Employing both internal and external methods, any critically damaged first wall position should be located in a matter of a few hours.
- (3) Remove first wall coolant panels that are damaged or those required to provide working access to the vacuum vessel wall in need of repair. Special apparatus would be inserted through the access ports to perform this task, after first having performed all operations in the facility reactor mockup.
- (4) Having uncovered the vacuum vessel wall, the damage is assessed and a portion of the vessel wall prepared for replacement. A pattern taken from the damaged area is reconstructed in the reactor mockup and an entire operation of remote repair and inspection established therein.
- (5) With an established procedure from the mockup, repair of the reactor vessel is accomplished, followed by inspections and testing. Critical analyses is made during the interim period to establish the limitations of the repair on future operation of the reactor, if any.

- (6) Replacement of the first wall coolant panels and the attendant fittings is accomplished. Testing of the panel coolant systems would follow.
- (7) Repair apparatus would be withdrawn and initial vessel testing begins.

The overall operation described could take place within a 9 to 11 day period without disturbing any of the other reactor systems. This relatively short repair period is attributed to the experience of personnel gained during the erection of the reactor and to the availability of the mockup.

The EPR system design precludes the need for the following: movement of massive reactor segments weighing up to 500 tons, most of which are in operating condition and need not be disconnected; storage of the (same) large reactor segment; repair or replacement of the removed segments (which would probably take considerably more reactor down time than 5 days); removal or cutting and repair of EF shaping coils, OH coils and possible TF coils and; removal of reactor accessories e.g. neutral beam injectors, vacuum components, reactor torque frame, etc.

3. Blanket and Shield Block Removal and Replacement Procedure

Under the same general conditions prescribed for repair of the first wall, the following procedures are required for blanket repairs. The removal and replacement of a difficult to reach blanket block designated either R or S in Figure IX-25 is designed to span a period of 7 to 9 days using the available equipment, trained personnel and spare parts.

No major disassembly of the reactor is required and the first wall is undisturbed. Steps for removing blanket block R or S:

- (1) Remove upper and lower vacuum port "Y" extension ducts and move cryosorption pumps out of the area
- (2) Disassemble upper shield blocks N, K-1, K-2, J-1, J-2, J-3, etc. one at a time from their coolant lines and structural supports. Remove them through the spaces between the TF coils with equipment provided.
- (3) With the upper shield removed, disconnect and remove the inner shield block H and the blanket blocks V, W, X, Y in the same order as the shield was handled.

- (4) Lower the shield segment assembly support structure containing the lower blanket and shield. Disconnect and remove blanket pieces R and S.
- (5) Repair and replace blanket pieces R and S, raise lower blanket and shield into position, connect coolant system, and test.
- (6) After testing replace stored upper blanket and shield blocks in sequence testing the coolant circuits for each. Thirty-six hours is the required time estimate for Steps 1-6.
- (7) Replace vacuum port plug and pump assemblies and recheck vacuum.

-
- 1. C. A. Trachsel, "Development and Analysis of Support Structure Concepts," SAN-76-51, McDonnell Douglas Astronautics Company - East, June, 1976.
 - 2. Manual of Steel Construction, American Institute of Steel Construction, Seventh Edition.
 - 3. Rules for Construction of Nuclear Power Plant Components, ASME Boiler and Pressure Vessel Code, Section III, Division 1, Subsection NF, 1974 Edition.

CHAPTER XIII

REACTOR AND FACILITIES

The design and layout of the reactor components into an integrated functionable system required a substantial effort in adjusting the interfaces among various components and systems. Acute space limitations for required component volume and for accessibility limited the design options and in many cases assembly and disassembly requirements dictated the configuration. Reactor design and layout problems included allowance for placement and maneuvering (1) components between the fixed TF coils and vacuum vessel (e.g. the blanket and shield blocks), (2) the twelve, large neutral beam injectors, (3) the vacuum system components and extension ducts. Once the reactor and adjacent equipment were arranged, the reactor building layout was prepared. Support equipment sizing established the need for seven other buildings for the EPR facility (Figure XIII-1).

A. Reactor Building

The reactor building is cylindrical, 73.2 m in diameter by 51.5 m high made of reinforced concrete with a flat pad and elliptical head. The reactor and its array of neutral beam injectors are housed in the main hall. A 250 m² (floor space) neutral beam test facility is located within the reactor building, complete with power supply, beam dump, shielding and remote hookup capability. A 8 meter deep basement is used for equipment and services supplied from externally located equipment. The walls of the building are 1.5 m thick and provide the biological shielding as well as structural support (Figure XIII-2). The building features a 300 Tonne polar crane. A shielded cab mounted on the overhead circular crane bridge is provided for remote maintenance and inspection purposes. This unit is equipped with a small crane, a single heavy-capacity and two standard-duty extended reach manipulator arms. It will carry two persons in its shielded cab to a vantage viewing area anywhere above the reactor. Both cab and manipulators will be programmable for routine operations.

A 1 cm thick steel membrane liner provides a continuous vapor barrier primarily for secondary tritium containment. Three cargo-sized sealed doors and two personnel air lock doors are used for access. The reactor support consists of a central pedestal surrounded by an annular ring abounded by

Figure XIII-1. Overall Site View

Figure XIII-2. Reactor Facility Elevation

Figure XIII-3. Reactor Facility Plan View

the reactor service floor. Both pedestal and ring structure extend downward 9.1 m to a sub-building foundation pad. The sub-reactor basement area houses the rf system power supplies, the TF coil energy dump resistors, neutral beam high voltage power source, vacuum pumps and blowers, the first wall, blanket and shield primary coolant and coolant storage system and magnet power supply cables. The building is designed to withstand pressure ranging from minus one half atmosphere to plus one atmosphere.

B. Mockup Maintenance Building

Four ancillary facilities in direct support of the reactor are provided. These are located in the mockup maintenance building and are shown in Figure XIII-3. Two hot cells are used to repair and examine activated reactor components and experiments. The larger cell is used primarily for neutral beam injector reconditioning and has an atmospheric control system to accommodate tritium release from the components. A mockup area outfitted with a full scale one-quarter section of the reactor is maintained in an adjacent building. This facility is required to support the remote maintenance/repair and to aid in the future upgrading of the reactor. All apparatus and operations may be practiced and perfected at this facility prior to live operations on the highly radioactive reactor internals.

The third facility is a decontamination area formed in the large cargo door entrance foyer. Special apparatus may be set up to decontaminate reactor parts prior to hot cell disassembly or off-site shipment. A fourth facility for winding and assembling the TF coils is provided near the mockup area. The equipment needed initially for reactor assembly can be moved into the reactor building to rewind damaged coils.

The Mockup Maintenance Building dimensions are 102 m by 47 m by 38 m high. A 225 tonne and a 35 tonne crane ride a full coverage bridge over the entire main floor area of the building. The 225 tonne crane is used primarily for winding and initial handling of the TF coils. The cranes cover the mockup area, both hot cells (through cell roof ports) and the basement area through floor slabs.

The large hot cell is 29 m by 18.5 m by 10.7 m high. It will handle two neutral beam injectors simultaneously. Equipment consists of overhead bridge mounted hoists and manipulators, seven shielded windows with manipulators, atmospheric control, and access to both the reactor lower equipment

passage and basement storage. One air lock links the cell with the reactor containment, permitting passage of a neutral beam injector. The smaller hot cell is 19.8 m by 13.7 m by 9.1 m high and will be used mainly for examination of samples, experimental apparatus and repair of blanket and shield blocks extension ducts, etc.

Provisions for a sub-basement level in the mockup area are required to duplicate the remote handling activities under the reactor. Other basement areas will be used to store remote handling equipment and many other spare parts.

The lab-shop-maintenance area is 47 m by 20 m and is three stories high. It will house electronics shops, electrical repair shops, a machine shop, experimental laboratory and a set-up area.

C. Tritium Facility

A cylindrical containment building 26 m in diameter and 27.4 m high will house the fuel reprocessing apparatus and the reactor building tritium cleanup equipment (Figure XIII-3). The normal reactor building air circulating system will function in parallel with the tritium removal system. Appropriate controls will seal off the ventilating system and allow the cleanup system to be activated. A high pressure tritiated air storage system will be included as an interim backup safeguard system.

The containment building will be compartmentalized and personnel will occupy limited areas. Services to the building will include liquid nitrogen and liquid helium supplies in addition to the normal process and power supplies. Storage facilities for all the fuel will also be provided. There are 730 m² of laboratory and office space adjacent to the containment building used to monitor, maintain and experiment with the tritium/fuel handling system. Two air locks allow personnel entry into the containment area while maintaining seal integrity.

D. Power Supply Facilities

The power supply facilities for the OH, EF, TF, RF and neutral beam injection systems are housed in two areas (Figure XIII-1). A large building located adjacent to the reactor containment and in close proximity to the reactor proper (to reduce the power lead length) houses the power supplies.

The building is ~ 80 m by 40 m by 24 m high providing three levels for SCR banks, capacitors, resistors, lead lines, switchgear, homopolar generators, etc. The equipment will be strategically located to minimize problems and costs. Cranes and other handling equipment will be installed as needed.

A superconducting energy storage unit was selected to optimize the pulsed power requirements for the OH, EF, rf and neutral beam systems. This results in a much lower utility grid requirement. The facility consists of an underground circular building 18 m in diameter by 3 m high. Cryogenics are supplied from the main magnet supply. The unit is located in an isolated area 70 m from the power supply building.

E. Cryogenic Facilities

A large building located between the power supply building and the control center houses the liquid helium and nitrogen cryogenic apparatus (Figure XIII-1). The building has 1860 m^2 ($20,000 \text{ ft}^2$) of floor space. The compressors are located ~ 40 m from the reactor while the liquid helium cold box equipment is near to the reactor. The TF coil system requires the largest supply of cryogen, however the equipment for the cryosorption pumps, tritium facility, energy storage ring and remaining magnet systems has been centralized. Additional major components include a liquid helium storage vessel, a constant pressure gas expansion tank, high pressure gaseous helium storage and liquid nitrogen storage.

F. Heat Transport and Cooling Tower Facilities

Facilities to transport up to 550 MW of reactor and ~ 50 MW of component waste heat are provided (Figure XIII-1). Reactor coolant water, pressurized up to 2000 psig is circulated in a closed system to contain the tritium buildup produced in the reactor. The system has four secondary coolant loops with one steam generator per loop rated at 150 MW thermal. Coolant is pumped through the reactor to a heat exchange building 30 m by 25 m x 14 m high complete with containment against inadvertent release of tritiated water. The heat exchange building contains the steam generators, canned rotor pumps and heat exchangers capable of handling up to 550 MW of heat. The heated steam is fed to a PWR type turbine generator set (150 MWe gross) or dumped to atmosphere through a cooling tower system. The water for cooling plant and components is in a separate non-tritiate circuit.

Cooling towers and associated pumping and piping systems are provided as follows: 550 MW peak load of high temperature water, steam, or turbine condenser coolant from the reactor using four multipurpose towers; 50 MW of plant component low grade heat in a demineralized system from magnet electrical leads, compressors, etc. using 2 towers, a pump house and demineralizing equipment.

G. Control Center

A 50 m by 62 m building will be used as a control center, office and laboratory facility (Figure XIII-1). The building is four stories tall with a control room 9.2 m by 18.3 m by 5.2 m high. Other features include a briefing room/auditorium, computer center and administrative offices.

H. Turbine Generator Facility

A turbine generator building 75 m by 48 m houses the 150 MWe gross peak output power conversion unit (Figure XIII-1). The building is located adjacent to the heat exchanger facility. Since this is an experimental reactor the size of the system at 150 MWe represents the highest reactor electrical power conversion practical. The EPR could operate with a smaller power plant or without one. This question has not been resolved.

I. Power Substation

A 150 MW substation with 132 kv incoming power is provided (Figure XIII-1). This is an initial estimate believed to be conservative, and the power meets the needs of the facility at this stage of design. This power requirement assumes there is no power plant provided, and the reactor operates from grid power alone. The major electrical base loads will be from; the cryogenic facilities which will necessarily draw power for a relatively long period prior to reactor operation; the building services, cranes, air conditioning, lighting, etc.; and the cooling towers.

Initial estimates indicate ~ 2 MW of emergency power will be needed to maintain safe standby conditions in the event of a site power outage. It is too early in the design phase to adequately assess the emergency power needs beyond this point.

CHAPTER XIV

ENVIRONMENTAL AND ENGINEERING SAFEGUARDS

Environmental and safety considerations have been introduced into the EPR design at an early stage in order that subsequent engineering safeguards could be designed. Potential hazard sources and accident initiation mechanisms have been identified and are discussed qualitatively in this chapter. Quantitative estimates of off-site tritium exposure and of the radioactivity, after-heat and biological dose due to activated materials have been made.

A. Preliminary Hazard Analysis

A preliminary hazard analysis has been performed for the EPR to identify potential hazard sources inherent in the system and to identify potential accident initiation mechanisms. At this preliminary stage, no attempt has been made to quantify the probability of malfunctions occurring or to classify the potential hazards according to their consequences for equipment damage or personnel hazard.

Several potential hazard sources are identified in Table XIV-1. The EPR plasma is a source of high energy neutrons, which implies the activation of reactor and ex-reactor materials and of the primary coolant. Materials activation is discussed in Section XIV-C. The tritium in the fuel cycle system and the tritium in the first-wall coolant constitute additional radioactivity sources (see Chapter X).

Large quantities of energy are stored and transferred within the EPR system. An inductive energy of 30 GJ, or 1.9 GJ/coil, is associated with the steady-state toroidal field. Gigajoule quantities of energy are drawn from the energy storage unit and transferred to inductive energy associated with the OH coil, EF coil and plasma current at the beginning of each burn pulse, then returned at the end of the burn pulse. Similar quantities are withdrawn from the energy storage unit for plasma heating on each burn pulse. The primary coolant has a large thermal energy content. In addition, chemical energy could be released if hydrogen mixtures reached an explosive limit, and thermal energy could be released if molten metal were formed and subsequently interacted with stagnant coolant.

Table XIV-1 Potential Hazard Sources

Radiation Sources

1. Inadequate or malfunctioning radiation shielding.
2. Activated first-wall, blanket, shield and structural materials -- 1×10^9 Ci after two years operation.
3. Activated ex-reactor components.
4. Activated primary coolant (H_2O).
5. Tritium in fuel cycle -- 1.5×10^7 Ci for 1.5 kg inventory.
6. Tritium in first-wall coolant (H_2O) -- 10^5 Ci after 1 year operation.

Energy Sources

1. Inductive energy in magnet systems
 - TF coil -- 30 GJ
 - OH coil -- 0.9 GJ
 - EF coil -- 1.7 GJ
2. Inductive energy in plasma current -- 331 MJ.
3. Inductive energy in energy storage unit -- 3.2 GJ.
4. Energy transfer in the neutral beam and/or rF heating system -- 1 GJ.
5. Thermal energy content of plasma -- 270 MJ.
6. Thermal energy content of pressurized water primary coolant -- 15.8 GJ
7. Chemical energy potential in hydrogen (D,T) mixtures.
8. Chemical energy potential in molten metal-water reaction.
9. Inertial energy homopolar generators -- 0.8 GJ

A number of hypothetical accident initiation mechanisms can be identified. Tritium and activated material could be released to the environment as a result of an earthquake, an airplane crash, missiles ejected from the reactor system, etc.

Loss of coolant flow resulting from loss of pressurization, pipe break or blockage of a coolant passage could occur. A sudden loss of pressure would cause the coolant to flash into vapor which, if it escapes into the primary containment, will raise the pressure on the primary containment system. If loss of coolant occurs without plasma shutdown, coolant boiling and burnout could result in melting of some segments in the first-wall and blanket, which could lead to a vapor explosion if the molten structural material came in contact with stagnant coolant.

An uncontrolled plasma dump on the first wall could deposit the plasma thermal energy in a localized region at a very rapid rate. This could cause ablation and vaporization of the coolant panel. In addition, a sudden loss of the plasma current could produce rapid transients in the inductive energy in the OH coil and EF coil systems.

Small pockets could be formed in which an explosive mixture ($\sim 4\%$) of D_2 or T_2 was confined. A D_2 or T_2 leak could lead to this situation.

The liquid helium in the cryogenics system could be released, as the result of a rupture of the cryogenics system. Upon release of liquid helium to the containment, it will flash into a two-phase vapor-liquid mixture. The helium would extract the heat from the reactor structure, producing thermal stresses in the structure and pressurized water coolant system.

The most critical condition for both superconducting toroidal and poloidal coils is an open circuit. The dump resistor coil protection system with a $8\frac{1}{2}$ minute time constant (described in Chapter IV) could protect the TF coil system if local normalization occurs, but arcing from an open circuit could cause extensive coil damage. Melting might occur in the conductor, cryostat and coolant lines. The most difficult problem for coil protection is with the poloidal coil system because of the coupling effect related to plasma stability, poloidal coil stability and power supply. If any of these systems become unstable and quench, the energy stored in the system could dump to one of the other systems.

Malfunction of the power supply and superconducting energy storage ring may cause problems similar to those caused by magnet failures particularly for the components associated with the poloidal field generation. In addition to the electrical/magnetic problems, a significant problem in the power supply is associated with the mechanical failure of the homopolar generator drums. Additional analysis is required to determine if missiles thereby produced will have enough energy to penetrate the housing and initiate a propagating type failure in adjacent generators or in the superconducting coil. Another potential problem is associated with the inductor-converter circuits which contain capacitors storing megajoules of energy. An overload or internal fault could cause an explosion in spite of the blowout plugs designed into these capacitors. The size of the energy overload and the results of the chemical/mechanical reaction require further analysis.

This preliminary survey of hazard sources and accident initiation mechanisms is certainly not complete. These factors are considered qualitatively in the development of the conceptual design.

B. Potential Off-site Tritium Exposures*

Preliminary studies aimed at establishing siting requirements and environmental impact of the EPR have been carried out. Two events leading to off-site exposure due to the release of tritium from a fusion reactor facility have been considered. The first event considers 2% of a total inventory of 2 kg of tritium released as water vapor in an accident. The dose commitment for an individual at the site boundary is then calculated for a certain set of assumptions. The result is compared to the guideline level as contained in 10CRF100. The other situation considered treats a daily release of 100 Ci of tritium under normal conditions and compares the results to the concentration guide listed in ERDAM 0524.

A number of assumptions are necessary for any computation as preliminary as this, and the following have been used in this exercise: (1) the Gaussian plume model¹ for atmospheric diffusion applies for the case of tritium released as HTO or T₂O; (2) at the time of release, the F stability condition (Pasquill Categories) in regard to dispersion prevails; (3) the average wind

* This work was performed by H. Moe, ANL.

speed of interest $\bar{u} = 1$ m/s; (4) the distance to the site boundary is 500 m in one case and 1000 m in a second case; (5) the total inventory is 2 kg of tritium, with 2% of that lost in an accidental ground level release; and (6) no modification occurs as a result of decay or scavenging due to precipitation following release.

For the case of a ground level accident release, the concentration, x , is given by

$$x = Q / \pi \sigma_y \sigma_z \bar{u} \quad (\text{XIV-1})$$

where x is in Ci/m³, Q is in Ci/sec, $\bar{u} = 1$ m/sec, and σ_y and σ_z are in meters. (Both σ_y and σ_z are obtained from reference 1.) Letting the exposure, ψ , represent the integrated concentration gives

$$\psi = Q^0 / \pi \sigma_y \sigma_z \bar{u} \quad (\text{XIV-2})$$

where ψ is in Ci·sec/m³ and Q^0 is the total number of Curies released over the duration of the incident. Using the assumed distances of 500 m ($\sigma_y = 17.7$ m and $\sigma_z = 8.5$ m) and 1000 m ($\sigma_y = 34$ m and $\sigma_z = 14$ m) results in

$$\psi / Q^0 = 2.1 \times 10^{-3} \text{ sec/m}^3 \text{ for 500 m}$$

and

$$\psi / Q^0 = 6.7 \times 10^{-4} \text{ sec/m}^3 \text{ for 1000 m.}$$

The total intake, I (in Curies), for an individual at the site boundary is given by

$$I = \psi \cdot f_a \cdot B \quad (\text{XIV-3})$$

where f_a is the fraction entering the individual's body water by inhalation² (taken to be 1 in this case), and B is the individual's breathing rate² (taken to be 2.3×10^{-4} m³/sec). For the above cases, $I = 2.3 \times 10^{-4} \psi$, or, using the appropriate ψ / Q^0 values, $I = 4.9 \times 10^{-7} Q^0$ for 500 m and $1.6 \times 10^{-7} Q^0$ for 1000 m.

Making use of expressions and data from ICRP #2 (reference 2), the body burden, q (in mCi), can be calculated as follows:

$$q = 2.8 \times 10^{-3} \text{ mR}/f_z \cdot \Sigma EA \quad (\text{XIV-4})$$

where (from reference 2) $m = 4.3 \times 10^4$ g, $R = 0.1$ rem/wk, $f_2 = 1$, and $EEA = 0.006$ MeV/dis. (A as used here is the relative biological effectiveness.) The resulting value for q is 2 mCi. This value is roughly twice the body burden listed in reference 2 because the value of A currently used³ is 1, not 1.7 as is used in reference 2.

For a burden of 2 mCi then, the whole body dose is 0.1 rem/wk, or the initial dose equivalent rate from a 1 Ci burden is 50 rem/wk. In accord with reference 2, the calculated dose commitment, D, per Curie is 124 rem/Ci intake. For the case of the ground level loss of 2% of the 2 kg inventory, the corresponding release is $\sim 4 \times 10^5$ Ci and the resulting dose commitments are 23.5 rem (whole body) at 500 m and 7.4 rem (whole body) at 1000 m. From 10CFR100, the guideline for total body dose equivalent in an accident situation is 25 rem. In each case, the accident would result in exposures within the limits although clearly it is desirable to utilize the greatest source to boundary distance.

Considering a continuous release during operation of 100 Ci/d, it becomes necessary to use a long-term dilution factor based upon the meteorological aspects of the given site. Such a long term average is given⁴ by the expression

$$\frac{x}{Q} = \sum_S \sum_N \frac{2 f(\theta, S, N)}{\sqrt{2\pi} \sigma_{zS} u_n \frac{2\pi x}{n}} e^{-1/2 \left(\frac{H}{\sigma_{zS}} \right)^2}.$$

This average takes into account the frequency [$f(\theta, S, N)$] with which the wind blows with respect to the stability class (S), wind speed range (N), and direction of interest (θ). Values of $\frac{x}{Q}$ were derived from data⁵ used to arrive at current ANL effluent criteria for stack monitoring. Using data for two typical locations at ANL, the values become:

$$\frac{x}{Q_{500m}} = 4.2 \times 10^{-6} \frac{\text{sec}}{m}$$

$$\frac{x}{Q_{1000m}} = 1.5 \times 10^{-6} \frac{\text{sec}}{m}$$

It should be noted that the $\frac{x}{Q}$ values are specific for a given direction since they are based upon wind direction frequencies. The relevant values of $\frac{x}{Q}$ given above are the maximum for the locations considered in the important direction. Based on these assumed $\frac{x}{Q}$ values, the values of x calculated for a 100 Ci/d release rate are $\sim 5 \times 10^{-9}$ Ci/m³ at 500 m and $\sim 2 \times 10^{-9}$ Ci/m³ at 1000 m. ERDAM 0524 (reference 6) gives the uncontrolled concentrations guide as 2×10^{-7} Ci/m³. So, the concentration in each case would be below that allowed off site by ERDA. Again the preference for locating farther away from the boundary would result in almost a three-fold reduction in concentration. Although the above values would satisfy the ERDA requirements, the ANL criteria for effluent monitoring (which is being reviewed by ERDA) would require effluent monitoring if the concentration off site exceeded 2×10^{-9} Ci/m³. To meet this criteria at a location similar to ANL would require siting at 1000 m from the site boundary or supplying stack monitoring.

C. Radioactivity, Afterheat, and Biological Dose

Calculations of radioactive inventories, afterheat, and biological hazard potential provide important information for the safety analysis and environmental impact questions. The biological dose at many key locations during operation and after shutdown is important for planning of maintenance and repair services. This section presents such information for the reference design.

These calculations are based on the neutron radiation field as characterized in Chapter IX. The concentrations of the radionuclides from the transmutation chains were calculated with the TK3 program.⁷ The transport of decay gamma-rays was determined with a series of one-dimensional geometric models serving as idealization of the nuclear systems described in Chapter IX. The average energies of the β -particles were calculated according to the method described in Reference 8.

Figure XIV-1 shows the radioactivity in units of curies per MWt as a function of time after shutdown following two years of operation. The level of neutron-induced activation at shutdown is 3.5 MCi/MWt and decreases by only a factor of 4 during a cool-down period of 1 yr, then

Figure XIV-1. Radioactivity and Biological Hazard Potential as a Function of Time after Shutdown Following Two Years of Operation

drops much more rapidly at later times. More than 99% of this activity comes from the first wall and blanket which constitute a 32-cm thick region of stainless steel surrounding the plasma. The level of radioactivity builds-up fast during operation and reaches a significant level in a few hours and a near steady-state level in a few days. It is worth noting that the level of radioactivity here expressed in Ci/MWt is about a factor of 3 higher than in reactors employing lithium blankets with only 5 to 10% stainless steel structure such as UMMAK-II.⁹ It was also found that the curies per MWt are fairly independent of the neutron wall loading when the latter is in the range of 0.1 to 5 MW/m².

The maximum radioactivity occurs in the stainless steel first wall. Table XIV-2 shows the specific radioactivity in the innermost region of the first wall in units of disintegration per sec per cm³ after 2-yr operation with a neutron wall loading of 0.5 MW/m². In the first few minutes after shutdown, the radioactivity is dominated by ⁵⁵Fe ($T_{1/2}$ = 2.6 yr), ⁵⁶Mn ($T_{1/2}$ = 2.58 hr), ⁵¹Cr ($T_{1/2}$ = 27.8 d), and ⁵⁸Co ($T_{1/2}$ = 71.3 d). Except for ⁵⁶Mn, the radioactivity from these isotopes remains dominant during the first few weeks after shutdown. At long times, the main isotopes are ⁶³Ni ($T_{1/2}$ = 92 yr), ⁵⁹Ni ($T_{1/2}$ = 8×10^4 yr), and ⁵³Mn ($T_{1/2}$ = 2×10^6 yr).

A crude, but useful, indication of the biological effects of the various radionuclides is provided by the Biological Hazard Potential (BHP). The BHP is defined as the amount of air required per kilowatt of thermal power to dilute the radioactive isotope to its maximum permissible concentration. The BHP is obtained simply by dividing the activity in Ci/KW(th) by the maximum permissible concentration (MPC) in Ci/Km³ of air for each isotope and summing over all isotopes. The BHP for the reference design is shown in Figure XIV-1 as a function of time after shutdown following two years of operation. The MPC values in the TK3 program⁷ library used in these calculations are taken from the US Atomic Energy Commission rules, Title 10, Part 20. At shutdown, the BHP is 800 Km³/KWt with about one-third of this value coming from the 4-cm-thick first wall. The greatest contribution to BHP comes from ⁵⁴Mn (25%), ⁵⁷Ni (25%), ⁵⁸Co (17%), ⁶⁰Co (10%), ⁵⁶Mn (5%), and ⁵⁶Fe (4%).

Table XIV-2. Specific Radioactivity in the First Wall (in disintegration per s per cm² after 2-yr operation with a neutron wall loading of 0.5 MW/m²)

Nuclide	Half-Life	TIME AFTER SHUTDOWN								
		0	1 min	10 min	1 hr	1 day	1 week	1 yr	10 yr	100 yr
⁵⁵ Fe	2.6 yr	4.5 (11) ^a	4.5 (11)	4.5 (11)	4.5 (11)	4.5 (11)	4.5 (11)	3.5 (11)	3.2 (10)	1.2 (0)
⁵¹ Cr	27.8 day	2.1 (11)	2.1 (11)	2.1 (11)	2.1 (11)	2.0 (11)	1.7 (11)	2.3 (7)		
⁵⁴ Mn	303 day	9.1 (10)	9.1 (10)	9.1 (10)	9.1 (10)	9.1 (10)	8.9 (10)	3.9 (10)	2.0 (7)	
⁵⁷ Co	270 day	4.2 (10)	4.2 (10)	4.2 (10)	4.2 (10)	4.2 (10)	4.2 (10)	1.7 (10)	1.6 (6)	
⁶⁰ Co	5.26 yr	1.0 (10)	1.0 (10)	1.0 (10)	1.0 (10)	1.0 (10)	1.0 (10)	9.0 (9)	2.7 (9)	1.9 (4)
⁶⁰ Co	10.5 min	2.8 (10)	2.6 (10)	1.4 (10)	5.2 (8)	1.3 (0)				
⁵⁸ Co	71.3 day	1.3 (11)	1.3 (11)	1.3 (11)	1.3 (11)	1.3 (11)	1.2 (11)	3.8 (9)	2.1 (-5)	
⁵⁶ Mn	2.58 hr	3.6 (11)	3.5 (11)	3.4 (11)	2.7 (11)	5.5 (8)	7.3 (-9)			
⁵² V	3.3 min	5.9 (10)	4.9 (10)	9.2 (9)	9.0 (5)					
²⁸ Al	2.3 min	1.8 (10)	1.3 (10)	8.7 (8)	2.5 (2)					
⁶³ Ni	92 yr	7.3 (7)	7.3 (7)	7.3 (7)	7.3 (7)	7.3 (7)	7.3 (7)	7.2 (7)	6.8 (7)	3.4 (7)
⁵⁹ Ni	8 x 10 ⁴ yr	8.1 (4)	8.1 (4)	8.1 (4)	8.1 (4)	8.1 (4)	8.1 (4)	8.1 (4)	8.1 (4)	8.1 (4)
⁵³ Mn	2 x 10 ⁶ yr	1.2 (4)	1.2 (4)	1.2 (4)	1.2 (4)	1.2 (4)	1.2 (4)	1.2 (4)	1.2 (4)	1.2 (4)
TOTAL		1.4 (12)	1.4 (12)	1.3 (12)	1.2 (12)	9.3 (11)	8.9 (11)	4.2 (11)	3.4 (10)	1.4 (7)

^a 4.5 (11) should be read 4.5 x 10¹¹, etc.

Figure XIV-2 shows the afterheat as a function of time after shutdown following two years of operation. The afterheat expressed as a percentage of the reactor thermal operating power is also fairly independent of the neutron wall loading in the range 0.1 to 5 MW/m². At shutdown, the decay heat is ~ 2.5% of the operating thermal power. This value drops only by ~ 20% during the first few minutes that are crucial to the emergency cooling. However, the percentage afterheat here is about one-third of that in a fission reactor. Moreover, the maximum power density in the EPR reference design was shown in section A of Chapter IX to be ~ 6 W/cm³ which is about a factor of 10 lower than the power density in a light water reactor and a factor of 100 lower than that in a fast breeder reactor.

The biological dose that would be received by a human being at many key locations in the reactor is extremely important for the design and planning of maintenance and repair operations as well as classifying the various areas for control of human access. During operation, the biological dose in regions external to the TF coils is ~ 10⁶ mrem/hr, which is too high to permit access to the inside of the reactor building for any reasonable length of time. The primary containment, serving also as the wall of the reactor building and as the biological shield, is 1.5 m thick ordinary concrete, which reduces the biological dose outside the reactor building to ~ 1 mrem/hr.

Table XIV-3 shows the biological dose at two locations as a function of time after shutdown following two years of operation at a neutron wall loading of 0.5 MW/m². The first location is in the vacuum chamber inside the first wall. The biological dose at shutdown is 6 x 10⁹ mrem/hr and decreases to 1 x 10⁹ mrem/hr in one-year of cool-down. Thus, it is obvious that remote maintenance will always be required for operations in regions near the first wall. The second location is at the inner side of the torus at a point on the poloidal axis that is in the same horizontal plane as the topmost boundary of the TF coils. The biological dose at this location after one day of shutdown is 600 mrem/hr. The exposure limits¹⁰ of Title 10, Part 20 for a "restricted area" are 2 mrem in any one hour and 100 mrem in any seven consecutive days. Thus, personnel access without additional radiation shield to the upper regions of the reactor near the central core may be possible only for short periods. It should be noted, however, that at all sites in the reactor, most of

Figure XIV-2. Afterheat as a Function of Time after Shutdown Following Two Years of Operation.

Table XIV-3. Biological Dose, in mrem/hr, After Shutdown Following Two Years of Operation

Time After Shutdown	Inside the Vacuum Chamber	In the Central Core Region
0	6×10^9	3×10^3
1 hour	5×10^9	2×10^3
1 day	2×10^9	6×10^2
1 week	2×10^9	5×10^2
1 year	1×10^9	2×10^2

the decay gamma-rays have energies in the range 0.1 to 2 MeV, with none above 3 MeV. The mean free path for absorption of a 2-MeV gamma-ray in lead is ~ 2 cm. Thus, about 10 cm-thick lead shield is sufficient to reduce the biological dose by two orders of magnitude.

The biological dose in the reactor building at the exterior of the outer shield is of particular concern. As indicated earlier, remote maintenance will be required for all service operations on components inside the first wall and the blanket. However, substantial savings on time and cost of these operations can be made if they are directed from a central platform inside the reactor building rather than from the outside. The biological dose exterior to the TF coils is the most difficult to calculate because of the presence of many major penetrations and the large-size neutral beam injectors which represent potentially large sources of induced activation. In the complete absence of the penetrations and beam injectors, the biological dose is ~ 2 mrem/hr at 24 hours after shutdown. To keep the biological dose to this value in the presence of the penetrations and injectors, full-shielding of the penetrations as specified in Chapter IX is required. Moreover, a shield has to surround all the walls of the neutral beam injectors.

1. D. H. Slade, Editor, Meteorology and Atomic Energy 1968, USAEC Report TID-24190 (1968).
2. ICRP, Report of Committee II on Permissible Dose for Internal Radiation Pergamon Press, London (1969).
3. ICRP, Progress Report from ICRP, Health Physics 17, 389 (1969).
4. D. B. Turner, Workbook of Atmospheric Dispersion Estimates, U.S. DHEW, Cincinnati, Ohio (1969).
5. H. Moses and M. A. Bogner, Fifteen-Year Climatological Summary, Argonne National Laboratory, ANL-7084 (1967).
6. ERDA Manual Chapter, Standards for Radiation Protection, ERDAM 0524 (1975)
7. T. Y. Sung and W. F. Vogelsang, "TK3: A Program to Calculate Radioactivity and Afterheat in Fusion Reactors," to be published as a University of Wisconsin FDM report.
8. M. A. Abdou, C. W. Maynard, and R. Q. Wright, "MACK: A Program to Calculate Neutron Energy Release Parameters (Fluence-to-Kerma Factors) and Multigroup Neutron Reaction Cross Sections from Nuclear Data in ENDF Format," UWEDM-37 and ORNL-TM-3994.
9. B. Badger, et al., "UWMAK-II, A Conceptual Tokamak Power Reactor Design," University of Wisconsin Report, UWEDM-112 (October 1975).
10. Code of Federal Regulations, Title 10, Part 20.

CHAPTER XV

RESOURCE REQUIREMENTS

A. Materials Inventory

The materials requirements, availability of metals and economics of a tokamak power reactor have been evaluated.¹ For the EPR, material requirements were developed to aid in facility design, e.g. crane capacity, helium storage capacity, building arrangement and to help in developing a cost estimate. The EPR material requirements are listed in Table XV-1. Excluding concrete and lead mortar, there are over 45,000 metric tonnes (Mg) of metal and non-metal materials required. Stainless steel, copper and B₄C are expensive materials and contribute a major cost increment. Materials such as niobium, titanium and helium are not only very expensive but are considered strategic materials, and supplies are limited.

B. Cost Estimate

A preliminary cost estimate has been developed for the EPR. The Plant Capital Investment Cost Estimate (Table XV-2) is based on 1976 costs and is organized essentially as outlined in reference 2. These costs include not only base material costs but also fabrication and installation costs. The cost of the 150 MWe turbine generator facility was identified but was not included in the base cost since this is an option which may or may not be required. A 25% factor on system direct costs was added for engineering, and another 25% factor was added for contingency. Development and management operating funds are not included in Table XV-2.

The total capital cost for the EPR facility is \$868,300,000 of which 11.6% is for buildings and site facilities, 43.0% is for the reactor, 42.3% for reactor plant facilities and 3.1% is for turbine and other auxiliaries. As can be seen from Table XV-2, magnets and power supplies are the two most costly systems, and together they amount to 36.3% of the direct costs.

C. Manpower and Funding Files

A possible organizational structure for the EPR Project has been developed for the purpose of making manpower estimates. The postulated structure is a modified project organization that will have overall technical and

Table XV-1. Materials Requirements

	Concrete (m ³)	Structural Steel (Mg)	Stainless Steel (Mg)	B ₄ C (Mg)	Graphite (Mg)	Lead Mortar (Mg)	Copper (Mg)	Niobium (Mg)	Titanium (Mg)	Be (Mg)	Helium (Mg)	Aluminum (Mg)
Building												
Reactor	161,000	11,300										
Other	72,500	3,700										
Magnets												
TF			1,590				1,410	18	9		11	
OH			132				117	2	1		1	
EF			140				125	2	1		1	
Support			12						2			325
First Wall			350							1		
Blanket			1,260									
Shield												
Inner			205	48								
Outer			923		152	1,113						
Vacuum System			350								3	
Neutral Beams			1,014								6	
Power Supply												
OH		462					450					
EF		711					527					
NB		7,055					7,087					
RF		36					34					
ES		14					14					
Other		10					10					
Cryogenic System		885	1,285						2		8	
Tritium Handlin.			4									
Remote Handling		900										
Power/Heat Dump			105*									
Total	235,500	25,073	9,340	48	152	1,128	9,774	22	34	1	30	325

* Cr-Mo Steel

Table XV-2. Plant Capital Investment Direct Cost Estimate

	<u>Item Cost</u>	<u>Category Subtotal</u>
	(in millions of 1976 dollars)	
1.0 Structures and Site Facilities		67.3
1.1 Site Improvements	2.5	
1.2 Reactor Building	50.5	
1.3 Electric Power Conversion Building	5.5	
1.4 Cryogenic/Refrigeration Building	3.5	
1.5 Tritium Separation Building	3.3	
1.6 Heat Exchanger Building	2.0	
1.7 Turbine Generator Building	4.6*	
Subtotal	67.3	
2.0 Reactor		248.6
2.1 Magnet Sys'tems	100.0	
2.2 First Wall System	10.1	
2.3 Blanket System	16.6	
2.4 Shield System	30.7	
2.5 Support Frame	6.0	
2.6 Vacuum Systems	38.0	
2.7 Neutral Beam Injection System	25.0	
2.8 RF Heating	10.2	
2.9 Instrumentation	12.0	
Subtotal	248.6	
3.0 Reactor Plant Facilities		245.0
3.1 Tritium Facilities	50.0	
3.2 Electric Power Supplies	110.0	
3.3 Heat Transport and Exchange Systems (600 MW _e)	30.0	
3.4 Instrumentation and Control	15.0	
3.5 Remote Maintenance Equipment	20.0	
3.6 Cryogenic Refrigeration Supply	20.0	
Subtotal	245.0	
4.0 Turbine Plant and Auxiliaries		18.0
4.1 Turbine Generator (150 MW)	24.8*	
4.2 Heat Rejection System (600 MW)	8.0	
4.3 Auxiliary Power Supply (10 MW)	5.0	
4.4 Site Power Distribution	4.0	
4.5 Site Power Feed Lines	1.0	
Subtotal	18.0	
Total		578.9
Engineering 25%		144.7
Contingency 25%		144.7
Grand Total		868.3

* Not Included in Base Cost.

management responsibility and will carry out the design analysis in sufficient detail to provide technical guidance. The majority of the detailed design and all of the fabrication and installation of equipment and construction will be done by industrial subcontractors.

Table XV-3 lists project manpower needs in man-years to manage, to develop the design concept, to provide technical direction, to administer and support major procurement and to provide operations support for installation and test activities. A project organization of about 260 qualified staff plus major subcontractors will be required. Less than 15 million a year will be required to support this organization. This technical/management project group will supervise contracts spending up to 200 million per year. Initial funding for this organization could come from operating funds, but final engineering, procurement, fabrication, construction, installation and checkout will require construction funds. The combined costs per year are shown in Table XV-3.

D. Schedule

Table XV-3 showed a six year period from the start of Title II engineering to initial operation of the EPR. Figure XV-1 provides a detailed schedule showing major activities and their time frames. This schedule was made on the assumption that work is accomplished in two shifts, 5 days a week and the third shift and weekends would be available to work out unanticipated problems and still maintain key dates on the overall schedule. Controlling items are included on the schedule.

-
1. "UWMAK-II, A Conceptual Tokamak Power Reactor Design, UWFDM-112, The University of Wisconsin Fusion Feasibility Study Group, Madison, Wisconsin (October 1975).
 2. Guide for Economic Evaluation of Nuclear Reactor Plant Designs, NUS Corporation, Rockville, Maryland (January 1969).

Table XV-3. Project Manpower and Cost Profiles

	1*	2	3	4	5	6	Total
Project Manpower							
Management and Administration (MY)	40	60	60	60	60	60	
Design Engineering/Analysis (MY)	200	200	200	160	140	120	
Operations Support (MY)	--	--	--	40	60	80	
Funding Schedule (Millions of Dollars)							
Project Costs-Operating Fund							
Management and Administration	2	3	3	3	3	3	17
Design Engineering/Analysis	10	10	10	8	7	6	51
Operations Support	--	--	--	2	3	4	9
Total	12	13	13	13	13	13	77
Capital Investment-Construction Costs							
Title II and III Engineering	50	50	30	10	4	4	148
Procurement	10	50	50	50	30	10	200
Fabrication/Construction	--	80	80	80	80	80	400
Assembly/Checkout	--	--	20	30	30	40	120
Total	60	180	180	170	144	134	868
Grand Total	72	193	193	183	158	148	945

*Year 1 starts with initiation of Title II engineering and release of construction funds.

Figure XV-1. EPR Design and