UCID--19328

DE84 006272

DISCLAIMER

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

A TANDEM MIRROR TECHNOLOGY DEMONSTRATION FACILITY

Lawrence Livermore National Laboratory
Fusion Engineering Design Center
TRW Energy Development Group
General Dynamics/Convair Division
University of Wisconsin-Madison
Bechtel Group, Inc.
Science Applications, Inc.
Los Alamos National Laboratory
University of California at Los Angeles
Massachusetts Institute of Technology

Manuscript date: October, 1983

NOTICE
PORTIONS OF THIS REPORT ARE ILLEGIBLE.
It has been reproduced from the best
available copy to permit the best
possible availability.

MASTERDISTRIBUTION STATEMENT 1
UNCLASSIFIED

CONTRIBUTORS

LAWRENCE LIVERMORE NATIONAL LABORATORY

T. H. Batzer	J. A. Kerns	L. R. Pedrotti
R. H. Bulmer	B. G. Logan	R. R. Stone
J. N. Doggett	J. E. Osher	K. I. Thomassen
A. I. Goldner	H. H. Otsuki	

FUSION ENGINEERING DESIGN CENTER

b. A. Cramer	J. R. Haines	P. T. Spampinato
B. A. Engholm	B. L. Hunter	V. C. Srivastava
P. J. Fogarty	D. H. Metzler	F. W. Wiffen
G. M. Fuller	W. D. Nelson	M. G. Willey
S. K. Ghose	M. Seki	
G. E. Gorker	G. E. Smith	

TRW ENERGY DEVELOPMENT GROUP

H. E. Boehmer	C. Garner	J. A. Maniscalco
R. B. Campbell	D. Goebel	S. Mortenson
D. L. Cheshire	J. D. Gordon	S. Salem
A. J. Cole	D. J. Grady	J. Vetrovic
S. A. Freije	P. W. Kidd	

GENERAL DYNAMICS/CONVAIR DIVISION

K. L. Agarwal	C. M. Powers	D. J. Tait
R. W. Baldi	J. L. Saunders	W. E. Toffolo
R. Dharmarajan	C. A. Sink	J. I. Valerio
M. W. Liggett	J. D. Sutliff	P. G. Vander Kraats
J. F. Parmer	R. A. Sutton	J. C. Yu

UNIVERSITY OF WISCONSIN

G. L. Kulcinski

J. F. Santarius

L. J. Perkins

M. E. Sawan

BECHTEL GROUP, INC.

S. L. Thomson

SCIENCE APPLICATIONS, INC.

J. E. Glancy and D. T. Pence

LOS ALAMOS NATIONAL LABORATORY

J. L. Anderson

UNIVERSITY OF CALIFORNIA AT LOS ANGELES

R. W. Conn and N. M. Ghoniem

MASSACHUSETTS INSTITUTE OF TECHNOLOGY

J. Schultz

ABSTRACT

This report describes a facility for generating engineering data on the nuclear technologies needed to build an engineering test reactor (ETR). The facility, based on a tandem mirror operating in the Kelley mode, could be used to produce a high neutron flux (1.4 MW/m^2) on an 8-m^2 test area for testing fusion blankets. Runs of more than 100 h, with an average availability of 30%, would produce a fluence of 5 MW/yr/m^2 and give the necessary experience for successful operation of an ETR.

ACKNOWLEDGMENTS

As indicated by the list of contributors, this design study was undertaken by a large group of organizations, including national laboratories, industrial firms, and universities. We would like to thank these organizations and their individual participants for their patience and cooperation in bringing together the efforts of contributors working in geographically separated areas with varying corporate priorities and constraints.

We are particularly indebted to the industrial contributors whose independent R&D work in several technology areas is reported here in order to form a clear and complete picture of the TDF system.

Finally, we especially thank the LLNL Technical Information Department editorial and Magnetic Fusion Energy production staff, along with the clerical staff of each of the contributors, for getting all these words and drawings assembled into a readable document.

CONTENTS

1.0. Executive Summary	
1.1. Introduction	1-1
1.2. Mission	1-2
1.3. Physical Description	1-5
1.4. Physics Description	1-8
1.5. Engineering Description	1-12
1.6. Conclusions	1-19
References--Section 1	1-20
2.0. Physics	
2.1. Introduction	2-1
2.2. TDF Equilibrium Parameters	2-2
2.2.1. Overview	2-2
2.2.2. Code Base for Calculations	2-8
2.2.3. Startup Scenario	2-10
2.3. Physical Requirements for Stream	
Stabilized Operation	2-13
2.3.1. Heating and Fueling with	
Neutral-Beam Injection	2-15
2.3.2. Pellet Fueling for Stream Stabilization	2-17
2.3.3. Neutral Beam Pumping and ECRH	
for Barrier Formation	2-18
2.3.4. Vacuum and Halo Requirements	2-23
2.4. Design Selection for Stability	2-24
2.4.1. Magnetic-Field Arrangement and Barrier	
Profile Used to Provide Stability from	
Flute and Ballooning	2-24
2.4.2. Neutral-Beam Orientation Necessary to Avoid	
Alfven Ion Cyclotron Instabilities	2-26
2.4.3. Stream-Stabilization of Drift-Cyclotron-	
Loss-Cone Modes	2-26
2.4.4. Tandem Mirror Magnetic and Barrier/Plug	
Combination Used to Avoid Trapped Particle Modes	2-27

2.5. Effect of Full Thermal-Barrier Operation	2-28
2.6. Fusion Reactor Condition Wall Loading	2-31
2.6.1. D-T Neutron Yield and Wall Loading	2-31
2.6.2. First Wall Thermal Loading	2-32
Appendix A Code Formulation for TDF Case	2-34
Appendix B An Example of Plasma Startup in Programmed Steps	2-38
Appendix C Factors Related to Neutral Beams	2-42
Appendix D A Monte Carlo Study of Charge-Exchange First Wall Power Densities for the TDF Central Cell Beams	2-47
Appendix E Pellet Fueling Calculations	2-60
Appendix F Vacuum and Halo Requirements	2-67
Appendix G Impurity Buildup in TDF	2-88
References--Section 2	2-90

3.0. Engineering

3.1. Configuration Overview	3-1
3.1.1. Introduction and Summary	3-1
3.1.2. Design Approach and Requirements	3-4
Modular Design	3-6
Access	3-6
Systems Integration	3-7
3.1.3. Selected Physical Characteristics	3-7
Overall Device	3-9
Central Cell	3-10
Anchor Cell	3-10
End Cell	3-11
3.2. Magnets	
3.2.1. Overview	3-12
Magnet Field Requirements	3-12
Magnet Configuration	3-12
External Field	3-12
Conductor Fields	3-12
Stored Energy	3-18
Halo Clearance	3-18
Magnetic Forces	3-18

3.2.2.	Summary Description of the Central Cell and Choke Coil System	3-18
	Central Cell Solenoids	3-18
	Choke Coils	3-23
3.2.3.	Central Cell Solenoids	3-23
	Design Considerations and Requirements	3-23
	Engineering Trade-off Studies	3-24
	Choice of Coolant	3-26
	Effect of Neutron Shielding on Cryopant Cost	3-26
	Selected Design Approach	3-30
	Quench Analysis	3-47
3.2.4.	Choke Coils	3-48
	Design Considerations and Requirements	3-48
	Engineering Trade-Off Studies	3-51
	Selected Design Approach	3-56
	Alternative Design Approaches	3-67
3.2.5.	Anchor Cell	3-70
	Design Requirements	3-70
	Engineering Trade-Off Studies	3-75
	Selected Design Approach	3-80
3.2.6.	Internally Water-Cooled Resistive Coil	3-118
	Theory	3-118
	Results and Discussion	3-124
3.3.	Neutral Beams	
3.3.1.	Summary	3-128
3.3.2.	Central Cell Neutral-Beam Requirements	3-130
3.3.3.	Central Cell Beams	3-132
	Approach	3-132
	Component Design	3-139
	Ion Source	3-139
	Neutralizer	3-149
	Reflection Magnet	3-154
	Drift Duct	3-157
	Power Absorbing Surfaces	3-178
	Vacuum System	3-204

Magnetic Shield	.3-213
Neutron Shield	.3-221
Systems Design	.3-223
Instrumentation, Control, and Data Acquisition	.3-223
Service Requirements	.3-229
Installation and Servicing	.3-233
Key Technical Issues	.3-239
3.3.4. End Cell Beam Requirements	.3-240
3.3.5. End Cell Beam Discussion	.3-245
Approach	.3-245
Component Design	.3-245
Transition Pumping Neutral-Beam Injector	.3-245
Anchor Cell Pump Beam	.3-256
Anchor Cell Slushing Beam	.3-268
Key Technical Issues	.3-278
Anchor Cell Beam Access Requires Low Divergence and Collimation	.3-278
Direct Recovery Development Risk is Worthwhile	.3-286
Magnetic Shielding Requirements for Low Divergence	.3-287
Drift-Duct Design Optimization	.3-287
Neutral-Beam Injector Sizing for Acceptable Availability	.3-291
3.4. Microwave Systems	
3.4.1. Overview	.3-293
3.4.2. Requirements	.3-293
3.4.3. Design Description	.3-293
Component Availability	.3-304
Window Survivability	.3-304
Power Control	.3-305
3.5. Low-Energy Deuterium Injection	
3.5.1 Gas Injectors	.3-307
Summary	.3-307

	Requirements	.3-309
	Design Description	.3-309
	Key Issues	.3-312
3.5.2.	Plasma-Gun Injectors	.3-312
	Summary	.3-312
	Requirements	.3-312
	Design Description	.3-314
	Key Issues	.3-314
3.5.3.	Pellet Injectors	.3-319
	Summary	.3-319
	Requirements	.3-319
	Design Description	.3-322
	Key Issues	.3-326
2.6.	Vacuum Vessel	
3.6.1.	Main Vacuum Vessel	.3-329
3.6.2.	Manufacturing	.3-330
	Fabrication	.3-330
	Magnets	.3-330
	End Cells	.3-330
	Central Cell and Anchor Cell	.3-332
	Installation	.3-332
	Magnets	.3-332
	End Cells	.3-333
	Central Cell and Anchor Cell	.3-333
	Structural Analysis	.3-334
	Central Cell Magnet (S-0 and S-1)	
	Vacuum Vessel	.3-334
	External Vacuum Vessel	.3-335
	Materials.	.3-335
3.6.3.	First Wall--Mechanical	.3-335
	Design	.3-335
	Manufacturing	.3-339
	Fabrication	.3-339
	Installation	.3-339
	First-Wall Material Properties	.3-341
3.6.4.	Structural Support	.3-341

Manufacturing	.3-342
Fabrication	.3-342
Installation	.3-343
Structural Analysis for TDF End Cell and Central Cell Support: Weight Loading	.3-343
Preliminary Stress Analysis of the Anchor Cell Vacuum-Vessel Support Structure	.3-344
3.7 Shielding	
3.7.1. Shield--Mechanical	.3-345
Manufacturing	.3-345
Fabrication	.3-345
Installation	.3-346
Thermodynamics	.3-346
3.7.2. First Wall	.3-348
Summary	.3-348
Requirements	.3-348
Discussion	.3-350
3.7.3. Bulk Shield	.3-355
Summary	.3-355
Requirements.	.3-355
Discussion	.3-356
3.7.4. Streaming and Shielding Analysis for the Neutral Beam Injection Duct	.3-361
Summary	.3-361
Requirements.	.3-364
Discussion	.3-364
3.7.5. End Cell Magnet Shielding	.3-373
Summary	.3-373
Requirements	.3-373
Discussion	.3-373
3.7.6. Biological Shielding	.3-376
Requirements	.3-376
Shield Design	.3-378
Reactor	.3-378
Building	.3-379

3.8. Test Facilities	
3.8.1. Summary	.3-380
3.8.2. Requirements	.3-381
3.8.3. Discussion	.3-386
3.8.4. Key Technical Issues	.3-399
3.9. Vacuum Pumping Subsystem Summary	
3.9.1. General Vacuum Requirements	.3-402
3.9.2. Discussion	.3-403
3.9.3. Design	.3-403
3.10. Particle Dumps	.3-419
3.10.1. Overview	.3-419
3.10.2. Requirements	.3-424
3.10.3. Neutral Beam Dump Design	.3-424
3.10.4. Neutral-Beam Dump--Startup Analysis	.3-441
3.10.5. Central Cell Beam Dumps--Trade Study and Off-Normal Conditions	.3-456
3.10.6. Beam Dumps for the Transition Region	.3-459
3.10.7. First Wall of the Central Cell	.3-473
3.10.8. Blistering of the Beam Dumps	.3-480
3.10.9. Plasma End Dump	.3-481
3.11. Tritium Studies	
3.11.1. Summary	.3-502
3.11.2. System Requirements	.3-502
3.11.3. Tritium Material Balance	.3-503
3.11.4. Fuel Cleanup System	.3-506
3.11.5. Isotope Separation System	.3-510
3.11.6. Fuel Preparation System	.3-513
3.11.7. Tritiated-Waste Treatment System	.3-513
3.11.8. Tritiated-Water Recovery System	.3-514
3.11.9. Facility Air Detritiation System	.3-518
3.11.10. Glovebox Gaseous Treatment System	.3-518
3.11.11. Tritium Systems Data Acquisition.	.3-519

3.11.12. Response of Tritium Systems to Accidents	.3-519
Reactor Building	.3-519
Tritium Processing Building	.3-523
Hot Cell	.3-524
Glovebox Gaseous Treatment System	.3-524
3.11.13. Conclusions	.3-527
3.11.14. Uncertain Technical Issues	.3-527
3.12. Plant Facilities	
3.12.1. Introduction and Summary	.3-529
3.12.2. Facility Requirements	.3-532
3.12.3. Design Description	.3-543
Site Facilities	.3-543
Mechanical Systems	.3-545
Buildings and Structures	.3-558
AC Power System	.3-580
3.12.4. Key Issues	.3-587
3.13. Instrumentation And Control System	
3.13.1. Summary	.3-591
3.13.2. Requirements	.3-591
3.13.3. Discussion	.3-592
The Supervisory control and	
Diagnostic System	.3-592
LCIS Interface	.3-595
Diagnostics	.3-595
Operator-Machine Interface	.3-598
Exception Handling	.3-598
Local Control and Instrumentation System	.3-599
Computer-Automated Measurement	
and Control	.3-600
Safety Interlock System	.3-603
3.14. Cryoplant	.3-606
3.14.1. Summary	.3-606
3.14.2. Requirement Survey and Analysis	.3-606
Magnet Cryogenic Requirements	.3-606

	Vacuum Pumping System	.3-611
	Main-Vessel Beamlines	.3-611
	Tritium Systems/Processing.	.3-611
	Pellet Injection	.3-611
3.14.3.	Cryoplant System Concepts	.3-612
	Helium System	.3-612
	Cold Box (Refrigerator)	.3-612
	Compressors	.3-613
	Helium Storage and Supply Dewars	.3-613
	Nitrogen System	.3-613
	building/Utility Requirements	.3-613
3.15.	Magnet Power Conversion and Protection	
3.15.1.	Summary	.3-615
3.15.2.	Requirements	.3-615
3.15.3.	Design Description	.3-617
3.15.4.	Key Issues	.3-626
3.16.	Maintenance	
3.16.1.	Overview	.3-628
3.16.2.	Requirements and Performance	.3-629
	Contact and Remote Maintenance	.3-629
	Component Disassembly	.3-629
	Remote Maintenance Equipment Technology	.3-629
3.16.3.	Maintenance Equipment	.3-632
3.16.4.	Scheduled and Unscheduled Operations	.3-632
3.16.5.	Component Replacements	.3-638
	Test Module.	.3-638
	Choke Coil	.3-638
	Central Cell Beam Dump	.3-642
	Anchor-Sloshing beamline	.3-642
	Central Cell Beamline	.3-643
	Cryopanel	.3-645
	Particle Dump	.3-645
	Helium Pump	.3-645

Magnet Vacuum Vessel	3-645
References--Section 3	3-651

4.0 Safety Assessment

4.1. Introduction	4-1
4.2. Safety Criteria and Requirements.	4-3
4.3. Potential Radiological Consequences.	4-10
4.4. Safety Features	4-24
4.5. Environmental Considerations	4-27
4.6. Key Safety Issues.	4-28
References--Section 4	4-29

5.0. Cost Estimates

5.1. Introduction	5-1
5.2. Total Capital Costs	5-1
5.2.1. Direct Costs	5-1
5.2.2. Indirect Costs	5-6
5.2.3. Discussion of Estimate	5-7
References--Section 5	5-13

LIST OF ILLUSTRATIONS

1-1. Cross section of TDF machine	1-6
1-2. Central portion of TDF, showing the two blanket module test locations, scheme for removing choke coils, beamline penetration through shielding, and beam dump access for repair	1-7
1-3. Profiles of magnetic field, potential, and density along the machine axis	1-10
1-4. Details of the blanket test area showing how modules could be readily removed	1-15
1-5. Azimuthal view of the four beamlines . at each end of the central cell	1-16
1-6. An individual beamline with four sources, one of them a spare to improve reliability	1-17
2-1. Base case axial magnetic field, plasma potential, and density profiles	2-9
2-2. Phase space plot for the $Z \sim 6.5$ m position of the transition section	2-20
2-3. The β -corrected phase space plot for the anchor midplane	2-22
2-4. Marginal flute and ballooning stability boundaries calculated by R. Wong with the TEBASCO code under base case conditions	2-25
2-5. The line source neutron yield calculated for D-T, D-D, and T-T reactions, as a function of Z	2-33
C-1. Average D^0 energy for an 80-kV NBI system with a D_2 gas fill in the charge-exchange cell	2-44
D-1. Polar plot of charge-exchange wall flux in the x-y plane for one of four D-T beams on D-T plasma	2-52
D-2. Composite polar plot of charge-exchange wall flux in the x-y plane for all four D-T beams on D-T plasma	2-53
D-3. Plot of an 80-keV beam on D-T plasma	2-55
D-4. D-T beams on D-T plasma	2-58

E-1.	Pellet deposition in the TDF plasma where the plasma radius is 8 cm and the pellet diameter is 0.57 mm	2-63
E-2.	Pellet deposition in the TDF plasma where the plasma radius is 8 cm and the pellet diameter is 0.72 mm	2-64
E-3.	The TDF normalized plasma volume and mass parameters as a function of $q(1 - r/a)$	2-65
E-4.	The pellet velocity and plasma penetration, superimposed on normalized plasma parameters	2-66
F-1.	Sputtering yield vs ion energy for molybdenum (Mo)	2-69
F-2.	Halo model geometry and particle balance terms	2-71
F-3.	Attenuation of neutral gas vs the halo line density	2-74
F-4.	<i>Our rough model for the reionization of charge-exchanged neutrals, showing (a) the model and (b) three possible special cases</i>	2-79
F-5.	Halo density and temperature vs neutral density at the first wall	2-85
F-6.	Halo density and beam attenuation vs relative transport rate from the central cell	2-87
3-1.	The TDF configuration: an evolved design that meets the requirements of all system components	3-2
3-2.	Side elevation and end views of the nuclear engineering device	3-2
3-3.	The three areas of the machine: the central, anchor, and end cells	3-5
3-4.	<i>Five components integrated into a limited portion of the central cell</i>	3-8
3-5.	Center-line field profile	3-14
3-6.	Reference magnet configuration	3-15
3-7.	TDF external fields	3-16
3-8.	Flux bundle through the choke	3-20
3-9.	Baseline magnet system forces	3-21
3-10.	TDF magnet arrangement	3-22
3-11.	Coil configurations for HeI and HeII cooling schemes	3-27

3-12.	Cross section and characteristics of the S-0 solenoid	3-32
3-13.	Cross section and characteristics of the S-1 solenoid	3-33
3-14.	Solenoid magnets, TDF-010	3-37
3-14.	Solenoid magnets, TDF-010 (Continued)	3-38
3-14.	Solenoid magnets, TDF-010 (Continued)	3-39
3-15.	Service stack, TDF-011	3-41
3-16.	Maximum cryostable current density for S-0 solenoid	3-45
3-17.	Maximum cryostable current density for S-1 solenoid	3-46
3-18.	Coil configuration and final temperatures following quench for four sets of current leads	3-49
3-19.	Conductor yield strength vs electrical conductivity	3-52
3-20.	Comparison of coolants and cooling configurations	3-55
3-21.	Baseline concept for the TDF choke coil	3-57
3-22.	Operating efficiency as a function of magnet geometry	3-60
3-23.	Maximum efficiency for any given magnet volume	3-62
3-24.	Comparison of internally and externally cooled resistive coils	3-68
3-25.	TDF internally cooled conductor concept, TDF-014	3-71
3-26.	Power required for choke coil to produce 15 T for varying background fields	3-72
3-27.	Alternate solenoid options	3-73
3-28.	Coil winding support alternatives	3-78
3-29.	TDF arrangement	3-81
3-30.	Anchor cell magnet conductor	3-82
3-31.	Anchor cell winding pack--typical	3-84
3-32.	Typical coil-case cross section	3-87
3-33.	Anchor cell coil set	3-89
3-34.	Cold mass support concept	3-91
3-35.	Anchor cell vacuum vessel	3-92
3-36.	Section of anchor cell through the mouths of coils T-1 and M-2	3-93
3-37.	Section of anchor cell through the mouth of coil M-3	3-94

3-38.	Inner shell of anchor cell vacuum vessel	3-96
3-39.	Anchor pumping beam dump	3-97
3-40.	Anchor cell shielding detail	3-98
3-41.	Anchor cell sections at (a) $z = 9.5$ m and (b) $z = 10.5$ m	3-101
3-42.	Anchor cell magnetic field on-axis	3-103
3-43.	Operating current vs peak field	3-105
3-44.	Notation for coil electromagnetic forces	3-109
3-45.	Section through anchor coil at $z = 9.75$ m	3-117
3-46.	Resistance change as a function of radial location of conductor	3-122
3-47.	Heating rate vs radial location of conductor	3-123
3-48.	The internally-cooled choke coil concept at 2800 A/cm^2 operated within design constraints	3-125
3-49.	Neutron-streaming considerations	3-134
3-50.	Central cell beam-duct geometry	3-137
3-51.	Central cell neutral-beam and beam-dump configuration	3-138
3-52.	Arrangement of beamline components	3-140
3-53.	Beamline layout	3-143
3-54.	Extraction grid with slits	3-144
3-55.	Extraction grid with circular apertures	3-146
3-56.	Beam focusing	3-147
3-57.	Baseline neutralizer tube	3-150
3-58.	Neutralizer geometry	3-153
3-59.	Ion dump arrangement	3-156
3-60.	Magnet dimensions	3-159
3-61.	Drift-duct geometry	3-160
3-62.	Drift-duct location	3-166
3-63.	Duct geometry	3-168
3-64.	Physical model	3-169
3-65.	Dependence of T_D on f	3-172
3-66.	Duct-dump relationship	3-173
3-67.	Model illustration	3-174
3-68.	Dependence of transmission on conductance	3-176

3-69.	Detail of duct wall	.3-179
3-70.	Drift-duct final design	.3-180
71.	Variation of sputtering yield with angle of incidence for H^+ , D^+ on Mo	.3-183
3-72.	Beams intersecting plasma	.3-185
3-73.	Beam absorption in plasma vs plasma radius	.3-186
3-74.	Beam vs dump geometry in X-Z plane	.3-187
3-75.	Beam vs dump geometry in Y-Z plane	.3-188
3-76.	Power-density distribution on dump face (one quadrant)	.3-190
3-77.	Plotted quadrant location	.3-191
3-78.	Defocusing of ion beam	.3-193
3-79.	Defocusing model	.3-194
3-80.	Power-density distribution on ion dump surface after defocusing	.3-196
3-81.	Collimator locations	.3-197
3-82.	Rear collimator	.3-198
3-83.	Front collimator	.3-200
3-84.	Line-of-sight optics in X-plane.	.3-202
3-85.	Diffusion model	.3-203
3-86.	Vacuum-vessel sectioning	.3-205
3-87.	Cryopanel structure, showing TDF/NBIS and cryopanel detail	.3-207
3-88.	Cryopanel structure	.3-208
3-89.	Liquid-nitrogen flow system (each panel)	.3-215
3-90.	Liquid-helium flow system (each panel)	.3-216
3-91.	Magnetic field identification	.3-218
3-92.	Superconducting shield design	.3-222
3-93.	Electrical and fluid connectors to ion source	.3-231
3-94.	Vacuum-vessel disassembly	.3-234
3-95.	Servicing a source	.3-237
3-96.	Removing cylindrical section for servicing	.3-238
3-97.	Anchor cell NBI locations	.3-241
3-98.	Neutral-beam plasma targets	.3-244
3-99.	Anchor cell vacuum vessel	.3-246

3-100.	Transition pumping-beam geometry	.3-248
3-101.	Transition beam plasma power density for two configurations	.3-249
3-102.	Transition beam-injector--plan view	.3-251
3-103.	Transition beam-injector--elevation view	.3-252
3-104.	Transition beam-injector--end view showing connections	.3-253
3-105.	Transition beam-power fractions	.3-255
3-106.	Transition beam-dump patterns	.3-257
3-107.	Magnetic fields around transition pumping beam	.3-258
3-108.	Anchor cell beam-injector--plan view	.3-259
3-109.	Anchor cell beam-injector--elevation view	.3-260
3-110.	Anchor cell beam-injector--end view showing connections	.3-261
3-111.	Anchor cell pumping-beam geometry	.3-263
3-112.	Anchor cell pumping-beam access	.3-264
3-113.	Anchor cell pumping-beam power fractions	.3-266
3-114.	Magnetic fields around anchor pumping injector	.3-269
3-115.	Anchor cell pumping beam-dump patterns	.3-270
3-116.	Anchor cell sloshing beam plasma patterns	.3-271
3-117.	Anchor cell sloshing beam access	.3-273
3-118.	Anchor cell sloshing beam power fractions	.3-276
3-119.	Anchor cell sloshing beam-dump patterns	.3-277
3-120.	Magnetic fields around anchor cell pumping beam	.3-279
3-121.	Beam access constrictions	.3-280
3-122.	Beam acceptance angles	.3-281
3-123.	(a) Beam power loss to beam scraping (b) Beam cost increases slowly with number of sources	.3-284
3-124.	Ion-dump characteristics	.3-288
3-125.	Direct recovery configuration	.3-289
3-126.	Divergence degradation from magnetic fields	.3-290
3-127.	ECRH/anchor cell interface	.3-294
3-128.	Cross section of anchor cell vacuum vessel at ECRH launcher	.3-296

3-129.	ECRH launcher detail	.3-298
3-130.	ECRH transmission system	.3-299
3-131.	ECRH system block diagram	.3-301
3-132.	ECRH loss summary	.3-303
3-133.	Flow diagram of deuterium gas system	.3-311
3-134.	Schematic sectional view of one of four startup plasma guns	.3-315
3-135.	Plasma-gun supply system diagram	.3-316
3-136.	Sectional view, showing the location of the TDF startup plasma gun assemblies (two guns on each end cover)	.3-317
3-137.	Two-channel pellet injector flow diagram (one of two) . . .	.3-323
3-138.	Outline of centrifugal pellet injector, showing principal features and interface connections . . .	.3-325
3-139.	Sectional view of TDF reactor, showing the location of one pellet injector and its mechanical interface . . .	.3-327
3-140.	Dual isolation valve assembly and the pellet drift-tube extension	.3-328
3-141.	Test module seal	.3-331
3-142.	First-wall configuration	.3-338
3-143.	TDF magnet models for neutronics analysis	.3-347
3-144.	Nuclear heating in the TDF central cell bulk shield	.3-349
3-145.	First wall neutronics modeling geometry	.3-352
3-146.	Bulk shield neutronics modeling geometry	.3-359
3-147.	Energy spectrum of neutrons and gamma photons streaming into the neutral beam duct	.3-366
3-148.	Angular distributions of neutrons and gamma photons streaming into the neutral beam duct	.3-367
3-149.	Geometrical model for the neutral beam duct in TDF	.3-368
3-150.	Magnet shielding requirements for different neutron wall loadings	.3-375
3-151.	Shield configuration for the transition and anchor coils	.3-377
3-152.	TDF test configuration	.3-394
3-153.	Detailed TDF test configuration	.3-395
3-154.	Neutron fluence requirements and capabilities per run . . .	.3-398

3-155.	Total integrated neutron fluence requirements and capabilities	.3-400
3-156.	Prototype of the TDF continuous cryopumping system tested at LLNL	.3-406
3-157.	Regenerable cryopumping panel concept for TDF	.3-407
3-158.	One of 12 six-panel degassing assemblies	.3-408
3-159.	Helium-appendage pumping installation in the end cells	.3-409
3-160.	Details of the helium-argon cryotrapping panel	.3-410
3-161.	Plot of tritium inventory vs excess capacity for the TDF end-cell pumping system	.3-413
3-162.	Baseline panel design	.3-420
3-163.	Dump panel assembly	.3-421
3-164.	Maximum heat flux on plasma end dump surface at various dump locations	.3-422
3-165.	Basic end dump panel	.3-423
3-166.	Sputtering rate enhancement vs angle of incidence from normal	.3-425
3-167.	Calculated power density normal to the beam after passing through the plasma	.3-427
3-168.	Calculated power density normal to the beam for four sources on the dump	.3-428
3-169.	TDF source startup sequence for central cell neutral-beam injectors	.3-430
3-170.	Sputtering thickness for 1 full-power year for various materials at increasing heat flux	.3-432
3-171.	Maximum temperatures of the beam dump front surface	.3-433
3-172.	Water pressure requirements as a function of impinging heat flux	.3-434
3-173.	Panel coil geometry	.3-436
3-174.	TDF end cell configuration	.3-437
3-175.	Tritium diffusion model	.3-438
3-176.	Neutral-beam deposition in metal surface	.3-439
3-177.	Internally finned, water-cooled dump	.3-442
3-178.	The central cell neutral-beam energy overlap on a normal dump surface with no plasma trapping	.3-444

3-179.	Source startup sequence for the central cell neutral-beam injectors	.3-445
3-180.	TDF plasma trapping fraction increase at startup	.3-446
3-181.	The heat flux scenario for the reactor startup phase study of the neutral dump at various maximum flux locations	.3-448
3-182.	The thermal analyzer front plate nodalization used to predict thermal gradients in the front plate of the neutral-beam dump during system startup	.3-449
3-183.	The begin-life temperature profile of the beam dump's front surface during reactor startup at the peak heat flux location of source 1	.3-450
3-184.	The begin-life temperature profile of the beam dump's front surface during reactor startup at the peak heat flux at the intersection of sources 3 and 4	.3-451
3-185.	The end-life temperature profile of the beam dump's front surface during reactor startup at the peak heat flux location of source 1	.3-452
3-186.	The end-life temperature profile of the beam dump's front surface during reactor startup at the peak heat flux at the intersection of sources 3 and 4	.3-453
3-187.	The begin-life maximum temperature of the beam dump's front surface during reactor startup	.3-454
3-188.	The end-life maximum temperature of the beam dump's during reactor startup	.3-455
3-189.	Peak heat fluxes on central cell beam dumps	.3-457
3-190.	Pumping power for TDF central cell beam dumps	.3-458
3-191.	Effect of misalignment between beam and plasma	.3-460
3-192.	Effect of plasma loss on the beam dump surface	.3-461
3-193.	Transition region beam dump locations	.3-462
3-194.	Surface heat load on the anchor pumping beam dump	.3-465
3-195.	Surface heat load on the anchor sloshing beam dump	.3-466
3-196.	Surface heat load on the transition pumping beam dump	.3-467
3-197.	End cell plan layout used in cost trade study	.3-469
3-198.	Summary of end cell cost study	.3-470
3-199.	Vacuum vessel surface area	.3-471

3-200.	End cell vacuum vessel thicknesses used for trade study relative volumes	.3-472
3-201.	End dump surface heat loads based on magnetic field strength	.3-474
3-202.	First wall of the central cell	.3-476
3-203.	Tritium processing and treatment systems for TDF	.3-507
3-204.	Fuel cleanup system for TDF	.3-509
3-205.	Isotope separation system for TDF	.3-511
3-206.	Contaminated air distribution system for TDF	.3-515
3-207.	TDF tritiated-water recovery system	.3-517
3-208.	Tritium concentrations remaining in the reactor building as a function of time and of cleanup rates for an accidental release of 100 g of tritium	.3-522
3-209.	Tritium concentration in tritium processing building as a function of time and of cleanup rate for an accidental release of 300 g of tritium	.3-525
3-210.	Tritium concentration in hot cell as a function of time and of cleanup rate for a 100 Ci/d source term	.3-526
3-211.	Plot plan for TDF facilities	.3-544
3-212.	Heat transport system	.3-548
3-213.	Emergency-cooling water system	.3-549
3-214.	Activity as a function of time after reactor shutdown	.3-552
3-215.	Decay heat vs time after reactor shutdown	.3-553
3-216.	Processing of activated structure wastes	.3-554
3-217.	Radioactive waste handling system	.3-557
3-218.	Interface of facility systems	.3-559
3-219.	Reactor and support buildings (plan below el. 130 m)	.3-560
3-220.	Reactor and support buildings (plan below el. 113 m)	.3-561
3-221.	Reactor and support buildings (plan below el. 106 m)	.3-562
3-222.	Reactor and support buildings (plan below el. 98 m)	.3-563
3-223.	Reactor building (el. 100 m) in more detail (Section A-A from Figure 3-219)	.3-564
3-224.	Reactor complex (Section B-B from Figure 3-219)	.3-565

3-225.	Cryogenics building of reactor complex (Section C-C from Figure 3-219)	.3-566
3-226.	Tritium and hot-cell buildings (Section D-D from Figure 3-219)	.3-567
3-227.	High-voltage side of heavy-duty substation with single phase transformers	.3-581
3-228.	Heavy-duty substation 13.8-kV power-distribution system . . .	.3-582
3-229.	Motor control and load control centers for the superconductor coil power supplies	.3-585
3-230.	Light-duty substation and essential power-distribution system	.3-586
3-231.	No-break control power for the superconductor coil protection and fuel reprocessing systems	.3-588
3-232.	The supervisory control and diagnostics system (SCDS) . . .	.3-593
3-233.	The plasma diagnostics data processing and display system	.3-596
3-234.	The local control and instrumentation system (LCIS) subsystem	.3-601
3-235.	The safety interlock system (SIS)	.3-604
3-236.	A typical primary power supply for the superconducting coils consisting of a silicon-controlled rectifier with low voltage and high current	.3-619
3-237.	The magnet power conversion system for the series-connected choke coils, M-1	.3-621
3-238.	The magnet power supply and coil protection system for the M-2 and M-3 pair of superconducting coils	.3-622
3-239.	A conceptual plan view of the power conversion and magnet protection equipment	.3-624
3-240.	Section A-A elevation view of the power conversion and coil protection equipment, from Figure 3-239	.3-625
3-241.	The motor and load control centers for the superconducting coil power supplies	.3-627
3-242.	The contact maintenance boundary and shield thicknesses . .	.3-631
3-243.	An estimate of first-time replacements for selected components	.3-637

3-244.	The choke coil replacements scheduled for the device's lifetime	.3-639
3-245.	Test module removal	.3-640
3-246.	Choke coil and beam dump removal	.3-641
3-247.	Beamline removal: typical for anchor sloshing, transition pumping, or anchor pumping	.3-643
3-248.	Central cell beamline removal	.3-644
3-249.	Removal of cryopanel modular assemblies	.3-646
3-250.	Particle dump panel removal	.3-647
3-251.	Helium pump module removal	.3-648
3-252.	Magnet vacuum vessel assembly removal	.3-650
4-1.	Major gaseous tritium leakage pathways	4-12
4-2.	Tritium-release pathways to environs	4-13
4-3.	Maximum-allowable tritium-release rate from plant buildings under normal conditions	4-15
4-4.	Maximum-allowable tritium-release in plant buildings from an accidental release	4-18
4-5.	Single confinement compared to double confinement	4-22

LIST OF TABLES

1-1.	Characteristic times for fuel power cycle tests on blankets and auxiliary equipment	1-4
1-2.	TDF physics parameters	1-9
1-3.	TDF engineering parameters	1-13
2-1.	Tandem mirror parameters for the central cell based on the stream-stabilized base case	2-4
2-2.	Tandem mirror parameters for the TDF end cells based on the stream-stabilized base case	2-5
2-3.	TDF startup scenario	2-11
2-4.	Heating, fueling, and pump system listing	2-14
2-5.	Design levels for the tandem mirrors of the TDF central cell, without a stream plasma	2-29
2-6.	Design levels for the tandem mirrors of the TDF end cells, without a stream plasma	2-30
B-1.	Definition of the parameters used in Table B-2	2-39
B-2.	Values of the parameters used in a TDF startup scenario	2-40
D-1.	Beam-plasma first-wall parameters for the TDF charge-exchange wall flux computation	2-49
D-2.	Particle and power balances for the TDF central cell beams, determined from four beams at one end of the device	2-51
E-1.	Pellet injector requirements for the central cell	2-62
F-1.	Neutral beam parameters for the central cell	2-68
F-2.	General halo parameters for zone 1	2-83
F-3.	Particle and power balance parameters for zone 1	2-84
3-1.	TDF magnet field requirements	3-13
3-2.	Design criteria that affect the magnet configuration	3-13
3-3.	Peak fields in each conductor bundle	3-17
3-4.	Energy stored in each coil	3-19
3-5.	Solenoid magnet requirements	3-25
3-6.	Characteristics of LHeI- and LHeII-cooled conductors	3-28

3-7.	Cost trade study of LHeI vs LHeII for central cell solenoids	3-29
3-8.	Cryoplant cost as a function of neutron heat deposition in the LHeI-cooled central cell solenoids	3-31
3-9.	Major characteristics of the solenoid magnets	3-34
3-10.	Input data for single lead-set quench analysis	3-50
3-11.	Anchor cell coil winding parameters	3-85
3-12.	Dimensions of coil-case cross section	3-88
3-13.	Peak fields in anchor cell coils	3-104
3-14.	Out-of-plane electromagnetic running loads on anchor set	3-107
3-15.	In-plane electromagnetic running loads on anchor set	3-108
3-16.	Force summary--anchor set	3-111
3-17.	Helium case sizing for magnetic running load	3-112
3-18.	Summary of coil stresses	3-114
3-19.	Center of gravity and support locations	3-115
3-20.	Operating parameters at the inner and outer coil layers	3-127
3-21.	Central cell NBIS requirements and design capabilities	3-131
3-22.	TDF standard ion source	3-148
3-23.	Neutralizer parameters	3-155
3-24.	Ion species, their relative abundance and Larmor radius in $B = 0.128$ T for D and $B = 0.157$ T for T	3-158
3-25.	Reflection magnet parameter for one beamline (four beams)	3-158
3-26.	Drift-duct parameters	3-177
3-27.	Gas sources and magnitudes	3-209
3-28.	Getter and cryopump beamline comparison	3-210
3-29.	Cryopanel area	3-212
3-30.	Cryopanel parameters for each NBIS unit	3-214
3-31.	TART results	3-224
3-32.	Results of neutron streaming and shielding calculations for the central cell neutral beamlines	3-224
3-33.	Service requirements for eight beamlines	3-230
3-34.	Power supply requirements	3-230
3-35.	Coolant requirements	3-232
3-36.	Summary of anchor cell neutral-beam requirements	3-243

3-37.	Summary of transition beam parameters	3-247
3-38.	Characteristics of a transition neutral-beam ion source	3-250
3-39.	Summary of anchor cell pumping beam parameters	3-265
3-40.	Characteristics of the anchor cell pumping beam ion source	3-267
3-41.	Summary of anchor cell sloshing beam parameters	3-274
3-42.	Characteristics of anchor cell sloshing beam ion source	3-275
3-43.	Summary of anchor cell microwave requirements	3-295
3-44.	Summary of system parameters (based on end values)	3-302
3-45.	Supply parameters for gyrotron oscillator (based on a 60-GHz tube developed by Varian)	3-302
3-46.	TDF startup scenario	3-308
3-47.	Gas-injection requirements/design data	3-310
3-48.	Startup plasma-gun requirements	3-313
3-49.	Plasma-gun design and interface data	3-318
3-50.	Pellet-injector design criteria	3-320
3-51.	Pellet-injector requirements	3-321
3-52.	Centrifugal injector data	3-324
3-53.	Vacuum-vessel thicknesses for the TDF external vacuum vessel	3-336
3-54.	Mechanical properties of 304L stainless steel at elevated temperatures	3-337
3-55.	Flux-degradation results	3-353
3-56.	Radiation damage and heating limits (bulk only)	3-357
3-57.	TDF bulk shield performance (neutronics)	3-362
3-58.	Total magnet nuclear input by zone (bulk only)	3-363
3-59.	Results for the S-1 superconducting magnets	3-371
3-60.	Results for the superconductive shield	3-372
3-61.	Categories of fusion test requirements	3-384
3-62.	Generic test categorization	3-387
3-63.	MARS blanket test categorization	3-388
3-64.	Fusion Breeder Program blanket test categorization	3-389
3-65.	Fusion neutron facilities	3-392
3-66.	End cell vacuum parameters	3-405
3-67.	Summary of beam dump heat flux, size, and life	3-463
3-68.	End dump parameters and costs	3-475

3-69.	First wall parameters for circumferential coolant flow	3-479
3-70.	Projected range of deuterium ions in beam-dump materials	3-482
3-71.	Plasma end-dump system requirements	3-485
3-72.	Particles incident on end dump	3-488
3-73.	Atomic number densities and sputter yields	3-489
3-74.	Mechanical properties of candidate materials at 200 °F	3-491
3-75.	Mechanical properties of candidate materials at 300 °F	3-492
3-76.	Mechanical properties of candidate materials at 400 °F	3-493
3-77.	Mechanical properties of candidate materials at 500 °F	3-494
3-78.	Mechanical properties of candidate materials at 600 °F	3-495
3-79.	Panel-coil structural design criteria	3-497
3-80.	Stress categories and maximum allowable stress intensities	3-498
3-81.	Plasma end dump heat flux vs panel thickness	3-499
3-82.	Fuel and plasma-stability requirements for a 2-MW full-power operation of the fusion device	3-504
3-83.	Reactor fueling rates and vacuum-pump exhaust rates for full-power (20 MW) operation	3-505
3-84.	Estimated contaminant-gas concentrations in the vacuum-pump exhaust for the plasma vessel of TDF	3-508
3-85.	The approximate dimensions of the ISS distillation columns of TDF	3-512
3-86.	Estimated tritium inventories at various locations in TDF	3-520
3-87.	Facility characteristics and assumed conditions for serious accident cases involving release of tritium inventories to their respective spaces	3-521
3-88.	Responses of facility cleanup systems to accidents and estimates of tritium losses to the atmosphere	3-528
3-89.	Major electrical power loads	3-531
3-90.	Characteristics of hypothetical site for TDF	3-536
3-91.	Safety factors for assumed various loading conditions	3-539
3-92.	Estimate of facility power loads	3-541
3-93.	Capacity requirements for ac power system	3-542
3-94.	Major plant cooling loads	3-546

3-95.	Amount of characteristics of radioactive waste from three reactor types	3-551
3-96.	Power system nomenclature	3-583
3-97.	Estimated cryogen requirements	3-607
3-98.	Anchor cell region cryogen requirements as provided by the FEDC cryoplant study	3-609
3-99.	Cryogenic requirements as determined from heat load estimates for solenoid coils S-0 and S-1	3-610
3-100.	The TDF's coil power conversion and protection requirements data	3-616
3-101.	Magnet power supply design data	3-618
3-102.	Design data for the magnet protection system	3-623
3-103.	Preliminary list of special purpose remote handling equipment	3-633
3-104.	Expected lifetimes of components in the central cell, anchor cells, and end cells	3-636
4-1.	Sources of safety hazards in TDF design	4-3
4-2.	Safety guidelines for TDF operating conditions	4-4
4-3.	Dose-limit guidelines for TDF	4-5
4-4.	Characteristics of TDF barriers for confinement of radioactive materials	4-8
4-5.	Design steps for reducing safety-related hazards	4-25
5-1.	TDF capital cost summary, as of mid-1982	5-2
5-2.	TDF direct cost summary, as of mid-1982	5-3
5-3.	TDF capital cost accounts	5-5
5-4.	Total direct cost of TDF, compatible with FEDC	5-9
5-5.	Total cost developed by the FEDC	5-9
5-6.	Estimated capital cost	5-10
5-7.	Direct cost elements, as of mid-1982	5-11

1.1. INTRODUCTION

The scientific feasibility demonstrations expected in several current-generation fusion experiments reflect a maturity in the understanding of plasma physics and signal the urgency to pursue further the technology of fusion. Fusion technology has progressed remarkably to be able to produce, heat, and contain plasmas in the breakeven experiments now in hand, but the nuclear technologies needed for engineering breakeven devices are in their infancy. If an Engineering Test Reactor (ETR) designed for engineering breakeven (electrical power output equal to the required recirculating power) is to be undertaken with acceptable risk, and if it is to be operated efficiently (to maximize the benefit/cost ratio through minimal unplanned outages for maintenance) these technologies must be developed as early as possible.

What sets off the technologies for the coming decade from those of the past is the introduction of tritium and the production of neutrons, and all that this implies for fusion systems. The impact should not be viewed solely in terms of blankets and tritium systems required for deuterium-tritium (D-T) operation, because the impact on component technologies for plasma production and on the entire configuration of the device is very significant. For these reasons one intuitively believes the experience gained in operating D-T fusion devices is much preferred to attempts to systematically solve nuclear technology problems with complementary facilities, including fission reactor tests of blankets and materials. While such auxiliary facilities have a role in the program, they cannot entirely substitute for an operating D-T device whose purpose is to guide the development of nuclear engineering for fusion and to be a test and demonstration bed for these technologies.

In charting a course leading to the construction of an ETR, one must determine the risks involved and have a clear set of criteria for success not related to physics performance alone. Failure might be defined by an availability too low to recover engineering data needed for the commercial demonstration to follow, or by a premature closing of the facility for reasons of safety or inability to repair the device (at reasonable cost) after a failure. In a broad sense then, success could be defined by the efficient operation of an ETR to generate this data base.

The Technology Demonstration Facility (TDF), first proposed by Fowler and Logan¹, is based on a tandem mirror operating in the Kelley mode (as explained shortly). In TDF one can produce a high flux of neutrons ($>1 \text{ MW/m}^2$), operate with steady state at modest availability, and therefore reach interesting levels of fluence for blanket and material testing. We would expect that the availability, initially low (5-20%), would reach 40-50% after a number of years of operation. Our objective is to gain the experience necessary to reduce the risks inherent in constructing an ETR from the data base that will be available in this decade. This early experience should shorten the time needed to construct and operate ETR, and hopefully allow an early start on the device.

1.2. MISSION

To accomplish the general objectives outlined above we have adopted the following mission for TDF:

- Demonstrate the steady-state operation of plasma heating systems, vacuum pumping and tritium handling systems, and superconducting magnets in a high flux neutron environment.
- Provide a steady-state neutron environment sufficient to test blankets that are designed to address power/fuel cycle issues for fusion.
- Develop and demonstrate instrumentation, control, and maintenance methods to show the safety and viability of operating fusion power devices.

The components of TDF for plasma production and containment are at the MFTF-B level of difficulty. Magnets, neutral beams, and electron-cyclotron resonant heating (ECRH) systems differ little in their parameters from those in MFTF-B. Even the steady-state operation requirement is only a slight extension of the 30-s capability of MFTF-B, except for cryopanel that must be continuously purged. It is the D-T operation, with attendant shielding, tritium handling, design constraints to accomodate maintenance, and required reliability that will extend the engineering data base on the confinement and production technologies.

Our major feature in this device however is the high flux, modest fluence capability of the machine, which will enable significant testing of power and fuel cycle components and systems. Blankets can be operated at a first-wall flux $>1 \text{ MW/m}^2$ for periods of time sufficient to discover failure modes that might occur well beyond any initial (first-operation type) failure but short of its expected operating life.

To be more specific on the required run time and fluence, we can classify the power/fuel system tests according to characteristic times. For example, neutronics tests require only seconds of exposure at a flux well below 1 MW/m^2 to gather important data. For our purposes we define three characteristic times for blanket and other power/fuel cycle components: (1) the time to reach thermal equilibrium in the blanket and heat exchanger system, (2) the time to reach an equilibrium tritium distribution in the blanket and recovery system (and other parts of the fusion device), and (3) the time required to discover early failure modes in the system. These failure effects, such as corrosion in lead-lithium blankets and the subsequent transport of activated corrosion products, radiation induced changes to material properties (density, porosity, thermal conductivity, resistivity), and compatibility of materials, are those that could lead to premature failure of components and/or systems. Radiation-induced sintering of ceramic tritium breeding materials, for example, is of particular concern, since it could drastically change the tritium holdup characteristics and lead to an excessive inventory of tritium.

One can roughly estimate these characteristic times from available basic materials data or designs of blankets, as is done in Table 1-1. Liquid metal blankets reach thermal equilibrium in ~ 0.1 hr, while solid-breeder blankets require some what longer (<1 hr). Tritium extraction at equilibrium requires only 10-20 minutes for lead-lithium blankets because tritium solubility is extremely low there. For blankets using a pure lithium breeder/coolant this time is tens of hours. Blankets with ceramic breeding materials could require up to a few hundred hours for recovery at equilibrium.

Finally, the premature failure effects might require from 10^3 to 10^4 hours of testing to discover problems of this type. In TDF, we would expect to be able to do this, and it is this capability which makes short tandem mirrors like TDF or TASKA², which is a design study carried out at

Table 1-1. Characteristic times for fuel/power cycle tests^a on blankets and auxiliary equipment.

Purpose of test	Uninterrupted test time and subject	Total test time needed	Issues being addressed
Thermal/hydraulic behavior	~1/10 hr (liquid metal blankets) ~1/2 hr (ceramic breeding blankets)	Tens of well-instrumented tests	Design confirmation--or: heat transfer/removal, structural effects, MHD pressure drops, off-normal (LOCF, LOCA) response.
Tritium Breeding/Recovery	~1/10 hr (lead lithium) ~10 hr (pure lithium) ~100 hr (ceramic breeders)	Tens of well-instrumented tests	Design confirmation--or: production/recovery rates, tritium accountability, permeation rates.
Early failure modes	1 - 100 hr	1,000-10,000 hr	Corrosion, sintering, mass transport, materials compatibility, weld performance, safety systems, any problems that lead to early system failures.
End of life	~100 hr	10,000-100,000 hr	Lifetime demonstration--or: radiation damage to structures, fatigue, embrittlement, swelling, all issues in the design which limit the design-life to ~10 MW/yr/m ² .

^aFlux level at least 1 MW/m².

Karlsruhe, FRG, with the assistance of the University of Wisconsin, unique among facilities that cost approximately one billion dollars. (1982 dollars, with contingency and all indirect costs).

Other characteristic test times could be defined. To qualify a blanket design for a next-generation device, rather than simply confirm a design, could require a few years' testing in TDF. End of life testing for radiation damage to structural materials requires an integrated fluence of $5\text{-}10\text{ MW}\cdot\text{yr}/\text{m}^2$ and is not possible in TDF.

A final part of the mission concerns the safe operation and maintenance of the device and the role of this facility in generating a data base that would be required to site and construct an ETR. One would expect to develop the necessary instrumentation and control for safe operation; establish operations and maintenance procedures based on experience in running the facility; and demonstrate the adequacy of systems for emergency events such as loss of flow or coolant, tritium release to secondary containment, and other such off-normal events. Experience gained here would be invaluable in establishing the safety of fusion power plants.

1.3. PHYSICAL DESCRIPTION

By limiting the central cell to the shortest length consistent with a minimal test area in the center of the machine, TDF can be comparable in size to present generation tandem mirrors. The magnet set is about as long as the Tandem Mirror Experiment-Upgrade (TMX-U) set, while the overall length is a little less than the length of the Mirror Fusion Test Facility (MFTF-B). High gas throughput for stream stabilization of the plasma results in a requirement for a large cyropanel surface area (similar to MFTF-B) at each end of the machine. TDF is, consequently, much longer than TMX-U even though the magnet lengths are about the same. Figure 1-1 shows the general layout of the magnets, vessel, beams, fueling and vacuum systems, and shielded central cell.

Testing space for blankets is in the 8-m-long central cell, with provision for two modules that are 1 m wide. An uncollided neutron flux of $1.4\text{ MW}/\text{m}^2$ at a 25-cm radius is generated by the fusion reactions in the 10-cm radius plasma. A total of 8 m^2 of test surface at this flux is available, about half of it readily accessible between solenoids. This central portion of the machine is shown in Fig. 1-2.

Figure 1-1. Cross section of TDF machine. Some components are shown rotated out of plane for clarity, and only one of the eight central cell beamlines is shown. (See Figs. 1-5 and 1-6 for the complete configuration).

Figure 1-2. Central portion of TFR, showing the two blanket module test locations, scheme for removing choke coils, beamline penetration through shielding, and beam dump access for repair. Also shown is the access to the high flux test area under the superconducting coils, which allows for inserting material samples for radiation damage studies.

Maintaining the high temperature and density of the central cell plasma requires the absorption of 52 MW of 80-kV beam power. Shielding of these beamlines and central cell to permit contact maintenance, access for maintenance of short life components, and accessibility and rapid change for test modules were important requirements to be met in designing the machine.

The end cells are 16 m in diameter and nearly 20 m long. These end cells are well isolated geometrically from the neutron "line source," which is the 8-m-long plasma column in the central cell. Only modest shadow shielding of the transition and anchor (superconducting) coils is required, because the copper choke coils at the ends of the central solenoid provide considerable axial shielding.

1.4. PHYSICS DESCRIPTION

Fusion reactions in TDF occur in the 4.5 T solenoid over the 8-m-long region between the 15-T copper choke coils. This beam driven hot plasma (37 keV ion energy) is magnetically confined with a lifetime of ~ 11 ms (see Table 1-2 for these and other key physical parameters). At a density of 6×10^{14} the fusion power density of neutrons is ~ 70 W/cm³ averaged over the cross section. Microstability of this population is provided by a warm background plasma (5 keV) with a density of 5×10^{13} and a lifetime less than 1 ms.

The warm plasma is electrostatically confined by a potential in the anchor which rises 4 keV above the 13-kV central cell potential. These poorly confined ions produce a large flow of streaming plasma through each anchor and guarantee their microstability according to experimental results taken from the 2XIIB and TMX mirror experiments.^{2,3}

Density, field, and potential profiles in TDF are given in Fig. 1-3. Notice that there is a substantial density drop between the choke coil and the anchor, and that trapped particle modes set a limit to this density drop. Further, the potential profile must be such that passing ions penetrate further into the anchor than do the electrons before being turned back (electrostatically) to the central cell. This axial difference in orbits leads to an orbit-averaged correction to the simple MHD trapped particle theory (somewhat analogous axially to the more familiar Larmor-orbit

Table 1-2. TDF physics parameters.

Central Cell

Plasma length (m)	8.0
Plasma radius (m)	0.1
Solenoid (T)	4.5
Peak beta	0.4
Hot-ion average energy (keV)	37.0
Hot-ion density (cm^{-3})	6×10^{14}
Beam voltage (kV)	80.0
Incident beam power (MW)	64.8 (1080 A/60 keV)
Trapped beam current (A) atomic	853.0
Injection angle (degrees)	65.0
Pellet gas feed (A) atomic	~800.0
Fusion power (MW)	~20.0
Neutron wall flux (MW/m^2) at $R = 0.25\text{m}$	1.4

Choke--Transition

P_{max} choke (T)	15.0
Plasma length (m)	4.75
Plasma density (cm^{-3})	$<3 \times 10^{12}$
Field at midplane (T)	1.0
Pumping NBI	80 kV, (59.2A/60 keV) 3.6 MW

Anchors

Plasma length (m)	3.3
Plasma density (cm^{-3})	$\sim 5 \times 10^{12}$
Field at mirror (T)	3.0
Field at midplane (T)	1.0
Sloshing neutral beams	80 kV, (4.2 A/60 keV) 0.25 W
ECRH	500 kW @ 35 GHz; 667 kW @ 60 GHz
Pumping NBI	80 kV, (2.9 A/60 keV) 0.174 MW

Figure 1-3. Profiles of magnetic field, potential, and density along the machine axis. The central cell population is nearly all magnetically trapped, and those ions that escape are confined by the 40kV confining potential.

corrections to MHD theory). This correction converts an otherwise unstable mode to a stable (negative energy) drift wave and provides stability criteria involving density ratios and certain anchor and transition lengths.

To provide the proper axial potentials that result in the desired orbits, we rely on the thermal barrier construct of TMX-U. The potential dip of the barrier reflects the passing electrons, while the tilted potential initiated by sloshing ions (and emphasized by ECRH) allows passing ions to turn beyond the barrier. Trapped ion modes should thus be stabilized. Again, we do not rely on the microstability of the thermal barrier, as we do in TMX-U and MFTF-B, rather we inject 800 A of gas in addition to the 850 A of beam (trapped in the central cell) to provide the stream stabilized flow necessary for stability. Recent data⁴ on TMX-U show that such a flow is not necessary when the ion sloshing distribution is set up. Consequently, we now know that TDF is designed too conservatively, that the gas flow could be reduced, the electron temperature increased above 2 keV, and the drag time on electrons increased. The higher Q that would obtain means that less beam current would be required.

To maintain the thermal barrier we require ECRH at 35 and 60 GHz, totaling about 1.2 MW. These systems would be very much like the present MFTF-B systems. Charge exchange pumping with neutral beams is required in both the anchor thermal barrier and in the transition to maintain the potential at values below the central cell potential. The pump beam current (30 A each end) is somewhat less than that available in TMX-U.

In summary, TDF is based on rather conservative physics. The millisecond containment times needed for Kelley mode operation³ are very short, and most of the fundamentals have been demonstrated experimentally. (Kelley mode refers to the two-component ion distribution in the central cell, with the majority species being the hot, magnetically trapped ions. This differs from the "two-component" or "wetwood burner" operation proposed in various Tokamaks, which have the hot component as the minority species. The mode is named after Dr. G. Kelley, formerly at ORNL, for his work on different modes of operation in mirror devices.)

From TMX-U we should verify that thermal barriers can be constructed, and from TMX-S (S for symmetric, denoting planned modifications to add a throttle coil) and TARA (an axicell tandem at MIT), we should discover whatever

constraints the trapped particle modes will impose. These latter two issues should be resolved very soon in the program and are the only physics areas where experimental verification is not in hand.

1.5. ENGINEERING DESCRIPTION

The engineering philosophy used in the TDF study was to provide a high reliability device for engineering demonstration and testing. We chose to use technology that was either in-hand or scheduled to be demonstrated in programs presently in place. This approach, coupled with the conservative physics assumptions that were used, should produce a machine with a high probability of fulfilling its mission in a timely manner.

The goal of the design effort was to describe each of the required subsystems in sufficient detail to allow one to do credible cost estimating and establish a configuration that meets interface requirements and points out critical technology issues.

The results of the 10 man-year design study⁴ can be summarized by saying that the TDF produces 20 MW of fusion power, developing an uncollided neutron flux of $1.4 \text{ MW} \cdot \text{m}^{-2}$ on an area of $\sim 8 \text{ m}^2$, while consuming about 0.1 gm of tritium per hour and 250 MW of line power, for a cost of about \$1000 million. A summary of the major engineering parameters are given in Table 1-3. Major participants in the study were LLNL, TRW, GD Convair, and the FEDC at Oak Ridge. Additional participation from Argonne, MIT, LASL, UCLA, SAI, Bechtel, and the University of Wisconsin was invaluable.

To describe TDF we begin with the central-cell region where virtually all of the neutrons are produced. The axial magnetic field of 4.5 T is produced by three Nb-Ti superconducting magnets spaced to allow the insertion of test modules up to 1 m in axial dimension. The ends of the central cell provide access for the central-cell beams that drive the fusion reactions. The main design considerations in this area are shielding for the solenoid magnets in the area adjacent to the beam ducts and the desire to keep the 15-T resistive choke coil close to the solenoids for field reinforcement. A detailed trade study between shielding the beam and magnet requirements in this area largely set the parameters for the rest of the design. The choke coil presents a novel design problem for fusion reactors because it must withstand a high

Table 1-3. TDF engineering parameters.

System		Quantity
● Overall machine		
Full power run length	(hr)	>100
Availability	(% life av.)	30
Design life	(Full power years)	5.4
Capital cost	(\$M)	~1000
Year of first operation		1990
● Plasma		
Length (central cell)	(m)	8.0
Radius	(m)	0.10
Peak beta		0.4
● Test zone		
First wall radius	(m)	0.25
Neutron wall load	(MW/m ²)	1.4
Area available	(m ²)	~8
Heat load	(W/cm ²)	50
Fusion power	(MW)	20
● Tritium		
Consumption rate	(gm/hr)	0.1
Inventory	(kg)	~0.3
● Vacuum		
Base pressure	(Torr)	5.0E-06
Total pump speed	(l/s)	1.3E08
● Magnets		
Superconducting		
Material		Nb-Ti
Peak cond. field	(T)	8
Peak center field	(T)	4.5
Resistive		
Material		Cu alloy
Peak center field	(T)	15
● Neutral beams		
Mode		Continuous
Maximum energy	(keV)	80
Power		
Central cell	(MW)	52
Pumping	(MW)	3.8
Sloshing	(MW)	0.25
● Radio frequency sources		
Type		ECRH
Frequency	(GHz)	35/60
Power	(MW)	1.2
● Pellet injectors		
Velocity	(m/s)	1500
Pellet rate	(/s)	500

radiation load and has high internal heat loads and magnetic stresses. The preferred design uses internally water-cooled copper alloy conductors chosen for high strength and acceptable resistivity. The useful lifetimes of these coils is limited not by physical damage but by the radiation-induced increase in resistivity that eventually causes heat generation in excess of the cooling capacity of the conductor.

The principal testing area is the wall of the central cell in the area between the central cell beam ducts (Figs. 1-1 and 1-2). There is direct access to two full-diameter test zones between the solenoid magnets, each capable of accommodating a 1-m-long test module. Figure 1-4 shows how TDF test modules would be inserted in this test area. Access to the areas inside the coil boundaries are also conveniently available to smaller test specimens, either as long-term resident samples for fluence testing or short-term samples inserted in "rabbit tubes." The major test capability offered by TDF is the ability to test full-size prototype blanket modules to qualify them for use in experimental power reactors. With steady-state run lengths of >100 hours, experimental components can be brought to thermal and tritium breeding equilibrium, and energy extraction may be demonstrated.

To generate the high-wall flux in TDF, despite having a modest plasma Q, we must provide 52 MW of neutral beams in the central cell. Figures 1-5 and 1-6 show how four beamlines are arranged at each end of the machine, with four sources in each beamline. The beams are designed to operate in a relatively high radiation environment for several hundreds of hours. The beamlines are located in an area where the background magnetic fields are as high as 2000 G. Superconducting magnetic shielding is employed to get the local fields down to the few gauss level needed for neutral beam source operations.

Each 80-kV source has a 70 A extraction current, of which 37 A is incident on the plasma. Only 24 sources are needed to provide the incident 1080 A (65 MW) necessary to trap 52 MW of power. For redundancy and reliability an extra source is provided in each beamline. Half of the beamlines use deuterium gas while the other half use tritium.

Because the TDF plasma is small in diameter, about 15% of the incident beam power passes through to beam dumps. These dumps have been designed for a peak power density of 1000 W/cm^2 . The dump, like the choke coil, is not a lifetime element and both are built into shielded modules designed for convenient removal and replacement.

Figure 1-4. Details of the blanket test area showing how modules could be readily removed.

Figure 1-5. Azimuthal view of the four beamlines at each end of the central cell.

Figure 1-6. An individual beamline with four sources, one of them a spare to improve reliability. The duct design and source optics are chosen to minimize neutron streaming without incurring beam blockage by high gas pressure in the duct.

Beyond the central cell are the transition and anchor regions, which stabilize the central-cell plasma. The fusion power in this region is small, and only minimal shielding is required. The magnets are C-shaped with a Nb-Ti conductor and are housed in an independent vacuum vessel. Three neutral beam systems deliver particles to this area: two are pump beams that enter at shallow angles; the other provides sloshing ions normal to the axis. The relatively complex magnet geometry coupled with rigorous beam geometrical requirements control the design in this area.

The end cell is the outboard part of the system, and its principal functions are to house the primary vacuum pumping and provide a dump for the plasma-leakage power. Vacuum pumping is provided by cycling cryopumps located above and below the plasma leakage fan. Pumps of the type called for here have been developed and tested by Batzer.⁵ There is redundant pumping capacity so that some of the pumps are always off-line being degassed. The internal tritium inventory can be controlled by the amount of excess capacity specified. The plasma dump is located at the extreme end of the machine, and its power-density limit largely sets the size of the end vessel. In this case, we limit the plasma power density at the dump to 250 W/cm^2 . This dump, like all those in the TDF, is water-cooled. The plasma startup guns are housed behind movable shielding at the end wall, and they inject a low-energy plasma along the field lines during startup.

The TDF is designed for maximum contact maintenance. Specialized tooling is provided for scheduled maintenance tasks, and general-purpose tooling is used for random failure repair. Therefore, contact operations can be performed in the vault during regular work schedules after a short cooldown with the magnets de-energized and the tritium removed. In this way, all the major connections can be made and broken in a contact mode, but components are removed with remote tooling because of the high flux of gamma rays coming from unshielded machine ports. Because the components are designed for high reliability and most maintenance tasks can be performed in this efficient contact mode, we are confident that the TDF will be a high-availability device with a specified 50% in the last half of its 15-yr life.

1.6. CONCLUSIONS

Early experience on nuclear technology can be gained in TDF. Its main feature is the ability to test blanket modules at a high wall flux for more than 100 hours per run. By using conservative physics and technology, the machine can be quickly built and made operational by the end of this decade. With the expected availability, the total first wall fluence in the machine will approach 5 MW/yr/m^2 , so that a number of blanket modules could be tested for 1,000-10,000 hours to determine if there are any early-life failure modes. With the engineering data base obtained in TDF, one could proceed confidently with an ETR during the 1990s.

REFERENCES--SECTION 1

1. T. K. Fowler and B. G. Logan, Tandem Mirror Technology Demonstration Facility, Lawrence Livermore National Laboratory, Livermore, CA, UCID-19193 (September 1981).
2. D. L. Correll, et al., "Production of Large-Radius, High-Beta Confined Mirror Plasmas," Nucl. Fusion **20**, 655 (1980).
3. R. P. Drake, et al., "The Effect of End-Cell Stability on the Confinement of the Center Cell Plasma in TMX," Nucl. Fusion **21**, 359 (1981).
4. T. C. Simonen, et al, TMX-Upgrade (TMX-U) Operation in the Sloshing-Ion Mode, Lawrence Livermore National Laboratory, Livermore, CA, UCID-19568 (September 1982).
5. T. H. Batzer and W. R. Call, A Continuous Cryopump for Steady-State Mirror Fusion Reactors, Lawrence Livermore National Laboratory, Livermore, CA, UCRL-87732 (September 1982). (Also given at the 29th National Vacuum Symposium, Baltimore, MD, November 16-19, 1982).

2.1. INTRODUCTION

The physics base for TDF is described by Fowler and Logan.¹ The basic premise is that a tandem mirror machine can be designed to yield a fusion reactor relevant neutron wall loading of $\Gamma_n > 1 \text{ MW/m}^2$, using generally conservative or established physics. The design is compatible with a relatively low cost, particularly if fusion $Q > 1$ is not also required. The plasma parameters that are needed in the central cell (of the tandem configuration) to yield the desired high D-T neutron wall loading are consistent with performance calculated for a neutral-beam driven, stream-stabilized tandem mirror design operated in a two-component "Kelley"-like mode with modest electron thermal conduction barriers and electrostatic ion plugs in the end minimum-B anchor cells. The physics requirements for stream-stabilized minimum-B or anchor operation have been established in experiments on 2XII-B² and TMX.³ The physics of tandem electrostatic ion plugging was established on experiments on TMX,³ TMX-U,⁴ Phaedrus⁵, and Gamma-6⁶. The general physics of various combinations of electron-cyclotron resonant heating (ECRH), ion-cyclotron resonant heating (ICRH), and sloshing ion population buildup are currently under study on TMX-U,⁴ Phaedrus,⁵ Gamma-6,⁶ and STM-1.⁷ The new physics requirements for neutral beam ion pumping and selective use of ECRH for barrier formation are to be tested in TMX-U⁸ and MFTF-B.⁹ Further supportive studies are expected from the TARA,¹⁰ Gamma-10,¹¹ and AMBAL-1¹² experiments.

Placing the major emphasis for TDF on attaining an uncollided 14 MeV neutron wall loading $\Gamma_n > 1 \text{ MW/m}^2$ and not simultaneously requiring a fusion $Q > 1$ (where $Q \equiv \text{fusion power/input power}$) eases the physics requirements for TDF. This allows us to ease the high voltage technology requirements for the neutral beams and create a relatively short minimum-cost design. Prior work on advanced mirror machine designs for a high-wall loading application include the 1974 FERF¹³ single yin-yang cell study and the 1982 TASKA tandem mirror study.¹⁴ The TASKA study, done by the joint Kernforschungszentrum Institute for Technical Physics (KFK) in Karlsruhe, Germany and the University of Wisconsin-Madison groups, considered a tandem mirror design with an ICRH, D-T neutral-beam fueled high mirror field central cell, use of neutral beam pumping to yield an electron thermal barrier in the transition sections, and use of ECRH and neutral beam injection in the

yin-yang anchor for magnetohydrodynamic (MHD) stability and electrostatic ion plugging. The superconducting magnet set for TASKA included an axisymmetric hybrid choke mirror coil with a normal copper insert producing a 20 T field. Our TDF design uses a magnet configuration similar to the TASKA design, but with D-T neutral beam injection as the main heating source, and pellet injection for fueling in the central cell. Pellet fueling is required for warm plasma stabilization of the central cell and allows conservative stream-stabilization of the anchors. The design also includes neutral beam pumping and the use of ECRH in each anchor, to yield an asymmetric electron thermal barrier and ion plug feature. This design refinement is required to ensure stability against possible trapped ion mode instabilities.¹⁵ The steady state operating feature of the tandem mirror design is very important to the utility of high wall loadings for blanket module engineering tests, since this allows the higher integrated wall damage exposures relevant to reactor materials testing, as well as tritium breeding and a test of thermal hydraulics under equilibrium conditions.

2.2. TDF EQUILIBRIUM PARAMETERS

2.2.1. Overview

The primary goal for TDF is to build up and maintain the plasma conditions that are needed to yield the desired D-T neutron wall loading Γ_n in the central cell with the minimum technology requirements on the supportive end cells and at the lowest overall cost.

To establish the general machine parameters, we first considered the line source neutron yield and first wall radius requirements needed to yield the desired Γ_n (discussed in Section 2.6.1). These requirements set lower limits on the needed high central cell magnetic field, high plasma density, and reacting ion temperature; this in turn constrains the plasma parameters needed for the supportive end cells for a given magnetic configuration. For a practical selected machine input configuration we then calculated the detailed equilibrium plasma requirements and the associated machine requirements with an adaptation of the MARS reactor code (discussed in Section 2.2.2). We then used the initial machine magnetic profile and the calculated first

approximation β ($\beta \equiv 8\pi \Sigma nkT/B^2$) to calculate the β -weighted integrals of normal and geodesic curvature. Finally, we iterated the machine magnetic profile until it was line average MHD-stable and had an acceptable minimum level of field curvature distortion to drive radial diffusion.

Tables 2-1 and 2-2 list the base case calculated machine requirements and the resultant point-equilibrium plasma characteristics for stream-stabilized operation of the final, iterated magnetic field configuration.

The code input parameters we used included:

Machine Plasma and Magnetic Geometry

- A minimum practical hot plasma radius $r_p \sim 10$ cm in the central cell. This design choice was based largely on the plasma target size needed to efficiently trap the ~ 0.4 deg (half-angle) $1/e$ divergence expected for 80-kV neutral beams (e.g., for a neutral beam at a distance of ~ 10 m the $1/e$ fall-off in neutral-beam footprint current density width occurs at a plasma radius of ~ 7 cm). We selected a relatively minimum $r_p = 10$ cm for the base case as a compromise of efficiency and power, since the required neutral beam current scales proportional to r_p^2 , and Γ_n scales proportional to r_p (approximately).
- A maximum magnetic field in the choke coil = 15 T. This was a practical choice based upon use of a normal copper coil with an 8.75-cm throat radius. This radius is needed to clear the magnetically mapped hot plasma column, including a halo of one 3.5-MeV alpha particle gyrodiameter (at $B \sim 15$ T).
- A central cell field of ~ 4.5 T. This is the maximum practical solenoid field preserving a high mirror ratio ($R_{vac} \sim 3.33$) with the 15-T choke coil, which is necessary for good confinement of the central cell hot ions.
- A minimum central cell wall radius $r_w \sim 25$ cm to clear both the hot plasma and a predicted halo plasma, scaling proportional to $(r_p + 2 r_\alpha)$, where $r_\alpha \equiv$ alpha gyroratio.
- A minimum central cell length of ~ 8 m to allow an adequate area for full-scale materials and blanket testing.
- Transition and anchor magnetic fields, chosen as needed for several purposes: (1) to clear the magnetic mapping of the hot plasma and halo, (2) to provide reasonable ECRH parameters, (3) to create

Table 2-1. Tandem mirror parameters for the central cell based on the stream-stabilized base case.

Parameter	Value
Plasma length (m)	8
Plasma radius, r_p (m)	0.1
Solenoid field, B_c (T)	4.5
Peak beta, $\hat{\beta}_c$	0.4
Electron temperature, T_{ec} (keV)	2.1
Hot ion average energy, $\bar{W}_{ih}$ (keV)	37
Ratio of plasma radius to mean hot ion gyroradius, r_p/a_i	10
Hot ion lifetime, τ (ms)	11.5
Hot ion density, n_{ih} (cm ⁻³)	6×10^{14}
Warm ion temperature, T_{iw} (keV)	5.3
Warm ion density, n_{iw} (cm ⁻³)	5×10^{13}
Beam voltage (kV)	80
Incident beam power (MW)	64.8 (1080 A/60 keV) ^a
Trapped beam atom current (A)	853
Injection angle (deg)	65
Beam particle trapping fraction, f_t	0.79
Pellets gas feed (A)	800
Fusion power (MW)	20
Ratio of fusion power yield to input power, Q	0.39
Neutron wall flux at $r_w = 0.25m$, Γ_n (MW/m ²)	1.4
Central cell potential, ϕ_e (kV)	13

^a The ion source beam output is taken as a 80%:15%:5% species mix for either $D^+ : D_2^+ : D_3^+$, or $T^+ : T_2^+ : T_3^+$, yielding a 55%:29%:16% D^+ (or T^+) energy mix at 80:40:27 keV for the neutral beam composition trapped in the central cell.

Table 2-2. Tandem mirror parameters for the TDF end cells based on the stream-stabilized base case.

Parameter	Value
<u>Choke and transition coils</u>	
Choke maximum field strength (T)	15
Plasma average length (m)	4.75
Plasma radius (m)	0.20
Plasma density (cm^{-3})	$<3 \times 10^{12}$
Magnetic field at midplane (T)	1
Peak beta, β_t	.01
Total pumping NBI ^{*,**}	80 kV, (59.2 A/60 keV) 3.6 MW
Injection angle (deg)/ magnetic field (T)	30/1 to 3
<u>Anchor coils</u>	
Plasma length (m)	3.3
Plasma average radius (m)	0.21
Plasma density (cm^{-3})	5×10^{12}
Magnetic field at mirror (T)	3
Magnetic field at midplane (T)	1
Barrier potential, ϕ_b (kV)	12.2
Sloshing neutral beams ^{**}	80 kV, (4.2 A/60 keV) 0.25 MW
Injection angle (deg)/magnetic field (T)	90/2.8 (at point a')
Beam trapping fraction	0.28
Electron-cyclotron resonant heating ^{**} (KW at GH_z)	500 at 35 (at point b) 667 at 60 (at point a)
Peak beta, $\hat{\beta}_b$	0.5
Hot electron average energy, $W_{e,h}$ (keV)	396
Warm electron temperature, $T_{e,w}$ (keV)	6.5
Maximum plug potential, $\phi_e + \phi_c$ (kV)	17
Total pumping NBI ^{*,**}	80 kV, (2.9 A/60 keV) .174 MW
Injection angle (deg)/magnetic field (T)	15/1 to 2.5

Note: See Fig. 2.1 for definitions of points a, b, a' and ϕ_e , ϕ_c and ϕ_b .

*An ion source output species mix of about 90, 7, and 3% is desirable for $\text{D}^+:\text{D}_2^+:\text{D}_3^+$.

**Total incident power for both ends.

sufficiently high mirror ratios (~ 3 in the anchor) to reduce neutral-beam requirements, and (4) to meet high β plasma stability requirements.

Plasma Input for Parameter Set

- Limit injection energy of neutral beams to 80 keV. This was so we could use existing MFTF-B neutral beam development, maintain good beamline ion to neutral conversion efficiency, and take advantage of the fact that D-T fusion reactivity $\langle \sigma v \rangle_{D-T}$ is nearly flat for mean ion energies of $\gtrsim 40$ keV. Calculations are generally based on an ion source species mix of 80% D^+ , 15% D_2^+ , and 5% D_3^+ (or $T^+ : T_2^+ : T_3^+$) for central cell neutral-beam injection.
- A limited peak $\beta_c \lesssim 0.4$ in the central cell (based on theory, Fokker-Planck calculations and previous experiments).¹⁶
- A limited peak $\beta_A \lesssim 0.5$ in the yin-yang anchor cell (based on theory and previous experiments).¹⁶
- Use of D_2 pellet injection in the central cell to provide fueling for stream stabilization of the anchor cell (described in Section 2.4.2) and warm plasma stabilization of drift-cyclotron-loss-cone (DCLC) modes in the central cell (as described in Section 2.4.3).
- Use of pumping neutral beams and ECRH to (1) form potential plugs at the outboard sides of the anchors and (2) form electron barriers at each anchor midplane to separate electrons in the central cell from those in the plug regions.
- Volume averaged calculations performed with an assumed cubic density profile of $n \propto n_0[1-(r/a)^3]$ where $a \equiv r_p$ and uniform radial temperature profiles. Predictions for the outer cool "halo" plasma shield are discussed in Section 2.3.4.
- Neutral-beam injection at 65 deg in the central cell to limit $\beta_\perp/\beta_\parallel < 2.5$ for Alfvén Ion Cyclotron (AIC) stability. Injection off the midplane at 90 deg in the anchor cell is used to develop two sloshing ion density peaks that will suppress the AIC or DCLC instability as discussed in Section 2.4.2.

The hot D-T ion reacting component ($\sim 92\%$ fraction-of-density) is magnetically confined in the 4.5-T central cell by the 15-T choke coils. The injected 80-keV, 40-keV, and 27-keV hot ions tend to drag down in energy on

the cool (few keV) central cell electrons until they are scattered into the loss cone and out over the choke coil, where almost all are directly lost out an end of the machine in a single transit time. The phase space of the minority, warm ions, is split into a magnetically-contained group in the central cell and an electrostatically-contained group in the full machine length between the potential end plugs in the anchor cells. The warm plasma density in the central cell is arranged through pellet fueling sufficient for a hot-plus-warm combined stable, nearly monotonic, ion distribution function and through sufficient loss flow of hot plus warm ions from the central cell for stream stabilization of the anchor cells. The ~10% fraction of the local density from the central cell loss flow of warm ions that pass through the transition and reflect on the anchor plug potential is sufficient to couple the central cell plasma to the MHD-stable anchors. This is predicted to stabilize any trapped mode instabilities.

Producing a plug potential in the anchor cells higher than the ambipolar potential of the central cell by a potential difference of the plug from the central cell $\Delta\phi$ with a common electron temperature T_e would normally require a higher plug plasma density than the central cell has, since $\Delta\phi \propto T_e \ln n/n_c$. However, TDF will increase $\Delta\phi$ by including a modest thermal barrier in the inboard side of the anchor cell. This will allow us to separate the electrons into different temperature regions, with a much higher T_e in the plugs.

We form the barrier using a combination of three methods: (1) ion pumping in the transition and anchor regions to locally enhance ion losses (into the central cell if possible), (2) local ECRH at the anchor midplane to produce a very hot magnetically-contained sloshing electron group with a density minimum to yield a barrier potential minimum, and (3) local ECRH of a hot electron group at the outboard sloshing-ion density peak to produce a higher T_e at the ambipolar maximum potential plug. Barrier physics is relatively new and has yet to be tested on TMX-U and MFTF-B experiments, but we feel that the requirements for a "barrier minimum to plug peak potential" difference of ~17 kV is sufficiently conservative for the 80-kV neutral-beam injection pump system to be a low-risk element for this study.

The sloshing-ion populations developed by 65 deg injection in the central cell and 90 deg injection off midplane in the anchors are designed to stabilize the AIC instability. Even without pellet injection, these sloshing

ion distributions will also tend to trap warm plasma for DCLC stable populations in these regions. An equilibrium case without the extra stream stabilization pellet fueling is therefore discussed in Section 2.5.

Figure 2-1 shows the base case axial magnetic field, plasma potential, and total plasma densities.

2.2.2. Code Base for Calculations

The parameters shown in Tables 2-1 and 2-2 are calculated using a code developed for calculating operating points for thermal-barrier, tandem-mirror reactors. The majority of the code equations are detailed in Ref. 17, Section IV. In general, the equations are statements of the particle and energy balance of central-cell ions and electrons, hot and warm plug electrons, and sloshing ions. There are energy exchanges between these different species, and particle sinks of one species can serve as particle sources for another. The relationships used to calculate neutral-beam and ECRH powers have changed since the report was published, reflecting improvements in modeling. These new scaling laws are described in Appendix A. The formula for the warm plasma density that passes into the end-plug region has also been improved on, using results based on work similar to that presented in Ref. 18. The configuration for TDF includes a thermal barrier, potential minimum, and a plug potential peak in the yin-yang anchor region. The transition region is pumped by a neutral beam, and the entire configuration is stable to the recently predicted "trapped particle modes."¹⁹

TDF was originally designed to operate in the Kelley mode; no thermal barriers were necessary, and we could attain a Q value of about 0.5. This was essentially the way TMX had been run. Therefore, it would require no new physics development and was quite attractive. However, the new trapped particle stability criterion required us to use a thermal barrier so that electrons could have their turning points inboard of the ion-confining peak. The presence of sloshing ions and hot electrons was then necessary to facilitate making the potential depression of the barrier. However, with the stream stabilization assumed in the original TDF, the sloshing ions need not be as robust as required when the stream current is absent; this is a conservative aspect of the TDF operating mode.

Figure 2-1. Base case axial magnetic field, plasma potential, and density profiles.

The code had to be modified in the central cell to include two species of ions: a warm stabilizing plasma and a hot mirror-confined plasma produced by neutral-beam injection. The confinement and energetics of these hot ions are described by the Logan-Rensink model,²⁰ which fits a number of Fokker-Planck cases.

The formulas used to compute the machine input powers required to sustain the TDF plasma are presented in Appendix A.

2.2.3. Startup Scenario

The startup scenario for TDF is only in a scoping phase. It is difficult to define startup of any tandem mirror, particularly one with thermal barriers, since there are several beam and ECRH sources which must be phased properly relative to each other. However, we have outlined two approaches for defining startup: (1) qualitatively stating the intermediate steps needed to go from the initial stream-gun-produced plasma to the moderate beta-neutron-producing plasma and (2) using the equilibrium code to compute sustaining powers to achieve plausible, intermediate operating points that might simulate a start-up sequence. We will describe both of these methodologies briefly.

Table 2-3 shows the steps needed to start up the TDF plasma according to the first method. After about 100 hours of glow discharge cleaning to remove oxygen and carbon impurity from the walls of the machine, we begin an initial phase lasting about 10 ms. The purpose of this phase is to produce a target plasma for beams and ECRH in both the central and end cells. This would be done by using at least two MFTF-B type plasma stream guns, each of which will inject around 226 A of plasma.

The next 20 to 100 ms make up the "tandem ϕ " phase and involve applying about 100 kW of 35 GHz ECRH in the end cells, while adding about 120 A of D_2 gas in each anchor and 170 A of D_2 gas in the central cell. The sloshing ion neutral beams are turned on during this period. The combination of the stream plasma leaving the central cell and the sloshing ions should produce microstability. This phase is intended to produce an anchor plasma of higher density (factor of 2 higher than initial phase), an electron temperature on the order of 1 keV, and anchors which are microstable.

Table 2-3. TDF startup scenario.

Topic	Operation	Goal	Equipment/procedure
1. Initial cleaning-- surface or after major open maintenance	Glow discharge cleaning, about 100 hours	To remove surface impurities such as oxygen and carbon, in the form of H_2O and CO_2 . Attempt to have liners and walls warm, desir- able magnetic field.	<ul style="list-style-type: none"> • Insertible anodes in each anchor and central cell. • Power supply of 1 kV for slow discharge of a few $\mu A/cm^2$ at the wall. • H_2 fill and pumping system for a pressure of a few Torr; system should have <1 hr gas recycle time.
2. Plasma buildup	A. Initial phase, 10 ms	To produce seed plasma of about $5 \times 10^{11}/cm^3$ in each anchor and about $10^{11}/cm^3$ in the central cell.	<ul style="list-style-type: none"> • At least two MTF-8 type plasma stream guns at each end wall having 4-cm inner diameters and 15-cm outer diameters. • Locate guns to map-in along B to the central cell. Use pulsed gun magnets for plasma-stream mapping only as needed for the first 10 ms. Each gun injects about a 20-A, 1 per s D_2 plasma (226 A equivalent) at a few eV average energy.
	B. Tandem potential, 20 to 100 ms	To produce anchor plasmas of about $10^{12}/cm^3$ with electron temperatures of ≥ 1 keV. Also to create μ -stable anchors.	<ul style="list-style-type: none"> • Bring on the 28-GHz ECRH power (≤ 100 kW) in each anchor. • Add to each anchor a D_2 plasma of roughly 10 T at a rate of 1/s. • Add to the central cell a D_2 plasma of roughly 15 T at a rate of 1/s.
	C. Barrier formation, 100 to 200 ms	To produce a potential barrier shape, with a stable B for both anchor and central cell. The B should be $\geq 10^{13}/cm^3$ for the central cell. Also to create efficient 80-kV trapping.	<ul style="list-style-type: none"> • Add sloshing neutral beam injection in anchor in sequence. • Add the main 56-GHz ECRH power (≤ 1 MW total). • Increase the D_2 plasma in the central cell to about 30 T at a rate of 1/s. • Add startup neutral beam injection of about 100 A at 80 kV in the central cell (possibly from a depleted, 80-kV spare neutral beam injection set).
	D. Barrier equilibrium, 100 to 500 ms	To create 1.4 MV/m ² fusion wall loading; create an equilibrium operating of n_e about 5.5×10^{14} and r_p of 70 cm. To have a pellet stream minimum for stability.	<ul style="list-style-type: none"> • Sequence-in part of the pumping neutral beam injection in the transition sections. • Reduce the anchor gas feeds to zero. • Sequence-in the 80-kV neutral beam injection to full power. Adjust the pumping neutral beam injection and pellets for the maximum nt, wall loading, and stability.

The next phase, which lasts about 100 to 200 ms, is the barrier formation phase. In this period the main 60-GHz tubes are turned on with a total power level not exceeding 1 MW. The central cell is fed by gas at a source strength of about 350 A, and the central cell startup neutral beams (40 keV) are turned on, delivering about 4 MW of power. Since the central cell warm density (fueled from the D_2 gas) is increasing during this period, the transition region pump beams should be started. These beams keep the source strength of warm ions trapped in the transition region and barrier small enough to let the potential wells form in these regions. During this phase, the gas feeds in the anchor are turned off. This is a critical phase, since it produces the thermal barrier and sloshing ions, both of which are necessary for plasma stability and high performance. Also during this phase, a high-density target plasma is provided for the 80-keV central cell beams.

In the final phase, which lasts 100 to 500 ms, we bring the 80-keV central cell neutral beams to full power, start to refuel the warm ions with frozen D_2 pellets (instead of gas), and adjust the barrier and transition region sustaining powers to achieve the equilibrium-stable plasma parameters shown in Tables 2-1 and 2-2.

The second approach to defining startup requirements is to run cases with the plasma performance code at plausible, intermediate density and temperature operating points and then calculate the sustaining powers required at these points. The calculations assume operation with thermal barriers, and hence really can only model startup during the last few hundred ms, while the barriers are being produced and afterwards. The goal is to find a start-up pathway that does not require powers greater than for the operating point. From an initial low-density (low beta) barrier and central-cell plasma, we build up the barrier beta by increasing the hot electron energy through ECRH. Increasing barrier beta by energy rather than by density helps lower the required ECRH power. This approach begins to build the barrier density (at a reduced electron energy) when the barrier beta is around one-half of the desired equilibrium value. Last, we begin to bring up central-cell beta to the desired value, while increasing the barrier beta from one-half of its final value to the full value.

To start up the anchor/barrier region independently from the central cell, we must control the densities in these regions separately. In the

current physics model, these densities are interrelated by g and G , defined by the ion-density ratios:

$$g = \frac{\text{passing from central cell} + \text{trapped in barrier}}{\text{passing from central cell}} ,$$

and

$$G = \frac{\text{passing from central cell} + \text{trapped} + \text{sloshing}}{\text{passing from central cell}} .$$

By increasing g and G in the barrier, we can increase the barrier density while keeping the central cell density fixed. By decreasing g and G after the barrier has been built up, the central cell density can be increased at fixed-barrier density by increasing central cell beta. These calculations give us the sustaining powers required and plasma temperatures needed for a given set of betas and of g values. The fueling requirements must then be computed, given: 1) the timescale for the startup, (2) the set of g values prescribed, and (3) the loss rates of the individual species. See Appendix B for an example.

In summary, the basic elements of TDF startup should not be drastically different than for MFTF-B, where we have given much thought to the problem and where some of the ideas expressed here originated. The quantitative approach is potentially fruitful. The first attempt at a scenario has yielded reasonable startup powers for central cell beams and charge-exchange pumping beams, as well as stabilizing stream currents. There remains a minor problem with the sloshing beams and ECRH powers--the startup power presently exceeds the equilibrium power. A revised scenario may remove this problem and will be considered in future work.

2.3. PHYSICS REQUIREMENTS FOR STREAM-STABILIZED OPERATION

This section briefly details the physics requirements for heating, fueling, and ion pumping for the TDF system. They are summarized in Table 2-4. The magnetic field geometry and associated physics requirements for stability are detailed in Section 2.4.

Table 2-4. Heating, fueling, and pump system listing. Under total, 1 T-1/s D₂ = 11.28 A (atomic).

Ion source voltage (KV)	Average neutral energy (keV)	Equivalent neutral current (A equiv.)	Input power (MW)	Injection angle (deg)	Trapping fraction no dimension	Frequency (GHz)	Gas flow total for ($\eta_g \sim 0.33$)
<u>Central cell</u>							
Fueling neutral beam injection system	80	60	1080	64.8	65	0.79	--
Pellet injection	--	--	800	--	--	--	--
<u>Transition cells</u>							
Pumping neutral beam injection system	80	60	60	3.6	30	--	--
<u>Anchor cells</u>							
Sloshing neutral beam injection system	80	60	4.2	0.25	90	--	--
Pumping neutral beam injection system	80	60	60	3.6	10	--	--
ECRH (a)	--	--	--	0.667	--	--	60
ECRH (b)	--	--	--	0.50	--	--	35
Total			70				2621 A D + 1620 A T or 232 T-1/s D ₂ + 14.4T-1/s T ₂

η_g = gas efficiency of the neutral beam.

(a) Point a is defined as the outboard sloshing ion density peak in the anchor.

(b) Point b is defined as the midplane sloshing ion density minimum in the anchor. (See Fig. 2-1.)

2.3.1. Heating and Fueling with Neutral-Beam Injection

The TDF central cell is operated as a high β , short prototype section of a D-T reactor intended to yield a steady-state uncollided neutron wall loading $\Gamma_n \sim 1.4 \text{ MW/M}^2$. The hot reacting D-T ions are the majority plasma species in the central cell, with only the minimum $\sim 8\%$ warm plasma added as needed to stabilize the microinstabilities. The effective $n\tau$ calculated from the Logan-Rensink model²⁰ for the hot ions is limited to $\sim 6.6 \times 10^{12} \text{ cm}^3/\text{s}$ because of the fairly rapid energy drag down on the $\sim 2.1\text{-keV}$ electrons and warm stabilizing ions; the partially cooled hot ion is scattered (by ~ 15 to 30 keV) into the loss cone and out over the 15-T choke coil. Establishing the high density D-T plasma of interest with such a low $n\tau$ requires intensive heating and fueling with the $\sim 70\text{-MW}$, 80-kV D^0 and T^0 neutral-beam injection (NBI) system.

Central Cell Neutral-Beam Injector Considerations. We selected extraction at 80 kV for the D^0 and T^0 NBI to take advantage of an existing 80-kV ion source technology developed for MFTF-B. This choice is also reasonably optimum for the high neutron yield needed to support Γ_n . The neutron yield is limited both by a basic β_c (or nkT) limitation for a fixed magnetic field and the fact that the $\langle\sigma v\rangle_{\text{D-T}}$ reactivity becomes relatively flat ($\sim 8 \times 10^{-16} \text{ cm}^3/\text{s}$) for mean ion energies $\bar{W}$ over 40 keV (but less than $\sim 200 \text{ keV}$). Fokker-Planck calculations for the stream-stabilized D-T system yield average ion energies $\bar{W}$ of about 37 keV for the 80-kV NBI system.

The NBI current requirements listed in Table 2-4 are based on Mars code calculations of the form:

$$I^0 \sim 0.45 n^2 a^2 L / (f_t n \tau) ,$$

where the factor 0.45 comes from integrating over the radial cubic density profile $n = n^0 [1 - (r/a)^3]$ assumed for a plasma column with a radius of a , cell effective length L is $\sim 7.2 \text{ m}$ from Fokker-Planck calculations, the particle trapping fraction is f_t , and $n\tau$ is calculated from the Logan-Rensink²⁰ Fokker-Planck model.

The NBI system is arranged to inject at an angle of 65 deg just inside the choke coils. The limitation to an angle ≤ 75 deg is based on Fokker-Planck requirements that the ion anisotropy must be reduced sufficiently for $\beta_{\perp}/\beta_{\parallel} < 2.5$ to be stable against the AIC instability. We selected 65 deg to allow better beam duct access between the choke coils and the first solenoid (with a minimum duct-to-first-solenoid clearance of ~45 cm needed for the neutron and associated gamma ray flux N, γ shielding for the superconducting solenoid), though this lower angle increased the NBI requirements. This NBI increase occurs because the effective mirror ratio R_{eff} is proportional to $(\sin \theta)^2$ and $n\tau \propto \log_{10} R_{\text{eff}}$.

Appendix C details effects of the ion source output species mix and trapping of various D^0 and T^0 energy components with an average trapping fraction ~0.79. For discussion here, we assume an ion source output mix of 80% D^+ , 15% D_2^+ , and 5% D_3^+ (or equivalent $T^+ : T_2^+ : T_3^+$ species) prior to dissociation to D^0 components at 80 KeV, 40 keV, and 26 keV. This yields an average input D^0 energy of ~60 keV for an 80-kV NBI system. Trapping is calculated separately for each species, using a one-dimensional Gaussian neutral beam with a divergence of 0.4 deg, incident on an axisymmetric plasma column with a cubic density profile and a radius of 10 cm. The trapping fraction is calculated using appropriate average energy-ion and electron-ionization cross sections, corrected for a limited number of generations of charge-exchange events prior to final ionization. The f_t value quoted is the minimum trapping expected; it is made up of 55% 80-KeV, 39% 40-KeV, and 16% 26-KeV D^+ input.

In Appendix D we discuss a more detailed Monte Carlo calculation that can correctly handle the relatively forward-peaked, velocity-dependent charge-exchange "scattering" in the very limited geometry where the approximate plane parallel incident neutral beam is about the same width as the trapping plasma column diameter. This scattering occurs in each of several possible generations of charge-exchange events, since charge-exchange cross sections are typically higher than those used for ionization. Details of pellet injection and preliminary pellet range calculations are described in Appendix E.

Anchor Sloshing NBI Considerations. The NBI heating and fueling requirements for the anchor cells are relatively modest because of lower cell densities and smaller volumes. However, we need a sloshing double-peaked ion density distribution, together with local ECRH, to facilitate formation of a local minimum-potential electron barrier at the anchor midplane and a maximum-potential peak at the outboard sloshing ion density peak. The use of 80-kV NBI appears satisfactory for the predicted modest relative central cell ion plugging potential $\phi_c \sim 4$ kV and the limited barrier to plug peak potential, which has a difference of ~ 6.2 kV. The high anchor β required for central cell MHD stabilization is produced largely by ECRH-heated electrons. It is desirable to use only a deuterium neutral beam here, to avoid high D-T neutron production locally, since this would require extra N, γ shielding on the yin-yang superconducting coils. Significant D-D neutron production will occur but the heating and damage is calculated to be low enough that it will not require extra shielding.

Injection is at 90 deg, but it is off the midplane at the inboard ~ 27 mirror region to achieve the needed sloshing density distribution. NBI and neutral beam dump access is very limited, though some variation in injection point and angle is available. Injection is at point a' (inboard sloshing-density-peak conjugate to the plug-density-peak) to reduce cold electron loading for plug ECRH at point a. Trapping of the anchor sloshing-NBI is similar to that just described for the central cell, except that the trapping chord geometry is generally along the major axis of the mapped hot-plasma ellipse (even then the f_{nd} and hence f_t is relatively low).

2.3.2. Pellet Fueling for Stream Stabilization

Since we decided to use established physics for TDF, we had to allow for additional fueling for stream stabilization of the anchors in the base case. In Section 2.4.3 we discuss the relevant streaming current density required in the anchor midplane. We have planned deuterium pellet injection into the central cell to fill in the loss cone and yield the end-loss currents needed for stream stabilization.

We considered several types of warm plasma sources, including gas-box fueling and D_2 pellet injection (D_2 is sufficient, since almost all of the central cell D-T yield is from hot-ion hot-ion collisions). Pellet injection appears the most desirable approach for higher gas efficiency ($\sim 100\%$), input

fueling throughout the pellet range in the hot core (instead of mostly at the surface), and much lower charge-exchange losses. These losses are estimated from calculations²¹ indicating that hot electrons play the dominant role both in evaporating (or ablating) the pellet and ionizing the atoms.

Section 3.4 discusses the details of the centrifugal type of extruded D_2 pellet injector. The physics constraints with pellet injection include:

- A limited maximum size of the D_2 pellet. This is because the number of atoms Δn in a single pellet needs to be much less than the total number of hot ions N in the central cell to limit the perturbation of the cold plasma input on the equilibrium hot plasma. For $\Delta n/N < 10\%$ the required diameter of a D_2 pellet is < 0.6 mm.
- A repetitive pellet injector rate of at least 0.64 kHz, to furnish the necessary average stream current of up to ~ 800 A.

2.3.3. Neutral Beam Pumping and ECRH for Barrier Formation

Our initial design involved only basic tandem mirror physics, using an axisymmetric, high-density, simple two-coil mirror plug at each end of the central cell and outboard yin-yang anchors for MHD stabilization. This design would not have required neutral beam pumping or ECRH. However, the possibility of the trapped particle modes described by Rosenbluth et al.¹⁹ led to our current more complicated end barrier and plug design. It uses a single axisymmetric 15-T choke, or throttle, mirror coil at each end of the central cell to magnetically contain the main group of high-energy ions undergoing fusion and a relatively modest barrier and outboard potential plug in each anchor cell to electrostatically contain a minority warm plasma component. This warm component is needed to suppress loss cone instabilities. The design also offers enough warm plasma connection between the anchors and the central cell to avoid trapped-particle instability modes.

Before settling on this design, we considered using an axicell pair of 15-T coils. However, the NBI power and neutron shielding requirements appeared excessive for the minor gain in performance, so the baseline design was simplified to a single choke coil.

In our current design the transition cell contains the major bad curvature region, so a low β weighting of the various magnetic field

curvature integrals is essential there and is discussed in Section 2.4.1. To provide this low β we needed some form of ion pumping in the transition region to reduce the local ion density (and thus the β) to the minimum passing ion density requirements for MHD connection and anchor stream stabilization. This is discussed in Section 2.4.3. We have used charge-exchange neutral-beam pumping, though we are considering other techniques to reduce power.

The requirement for a pumping neutral beam is that the ion resulting from neutral beam charge-exchange on the plasma ion is located in the loss-cone region of phase space. Ideally the resultant ion would pass into the central cell for fueling and heating, but this is difficult to arrange with the high mirror ratio (about 15) for the transition to choke coil. This is particularly true for the fractional energy species components in the pumping neutral beam. So most of the transition neutral-beam pumping power is lost through reflection of the charge-exchange ion on the choke coil field and passage back through the anchors and out along field lines to the end walls.

The phase space plot for the Z ~6.5 m position of the transition section is shown in Fig. 2-2, with the 80-kV pumping neutral beam oriented at 30 deg. Neutral-beam injection angles of <16 deg would be required for pumping over the 15-T choke coil field. The on-axis phase space plot shows that using 80 kV for the pump neutral beam allows both the full- and half-energy beam components to pump and permits only fractional trapping of the third-energy component at some beam-plasma intersections. The use of 80 kV rather than the minimum of ~30 kV needed for full energy component pumping, and the pump neutral beam requirement for >90% D^+ purity from the special ion source (< 7% D_2^+ and < 3% D_3^+) are conservative steps we took to ensure efficient pumping along the 3-D intersection of the pump beam with the elliptical plasma column. The total incident pumping beam current requirement is based on code calculations described in Section 2.2.2 and shown in Table 2-4. The calculated potential depression (shown in Fig. 2-1) in the transition to ϕ ~5 kV represents a substantial ~8 kV barrier for the ~2.1 keV central-cell electrons.

The anchor cell must furnish both the β weighted MHD stabilizing term for line curvature integrals and the electrostatic plugging function with $\phi > 13$ kV to contain the warm ion loss cone stabilization population in

Figure 2-2. Phase space plot for the $Z \sim 6.5$ m position of the transition section. The positions of the 30-deg pumping neutral beam components are shown by the circles.

the central cell. For maximum stabilization of the trapped particle modes in the central cell, the anchor should also have an asymmetric axial potential distribution, allowing the stabilizing warm ions to penetrate as far as possible in the anchor cell before reflection. This second requirement is satisfied by forming a barrier minimum potential near the anchor midplane and a plug potential maximum on the outboard side of the cell. This dual potential function requires production of a sloshing plasma to furnish a double-humped ion density population with an ion density peak for the outboard potential maximum and a central density minimum for the anchor midplane minimum potential barrier. An anchor pump neutral beam prevents the barrier ion density minimum from trapping passing ions and filling up. The β -corrected phase space plot for the anchor midplane is shown in Fig. 2-3. With an end fan injection angle < 10 deg, the full- and half-energy pump neutral beam components would be pumped through the transition section into the central cell.

The anchor pump beam requirements are also shown in Table 2-4. Again we are conservative, pumping with an 80-kV NBI system, orienting the injection angle < 16 deg, and requiring a high-purity ($> 90\%$) D^+ fraction from the neutral beam ion source. This ensures efficient pumping all along the neutral beam/anchor plasma intersection path. It also has the advantage of allowing us to use common 80-kV neutral beam components throughout.

To form the deep anchor barrier minimum (~ 12 -kV barrier) and ~ 17 kV-anchor plug potential peak for the base case requires the addition of ECRH in local regions. ECRH power near the anchor midplane helps form a deep barrier minimum when electron energies are driven high enough for magnetic containment of the electrons. The vacuum field of 1 T will be β ($\beta \sim 0.5$) depressed to $\bar{B} \sim 0.84$ T, since $B = B_{vac} (1 - \beta)^{1/2}$ and the volume averaged $\bar{\beta} = 0.3$. ECRH is planned at ~ 35 GHz for equilibrium, first and second harmonic, off-axis, near-midplane heating of the anchor. ECRH at 28 GHz would also be useful for startup.

The second ECRH region is at the sloshing ion density peak below 2.0 T on the outboard side of the anchor. The goal here is to moderately heat electrons that are electrostatically trapped by the ion density peak and thereby raise the peak's ambipolar potential proportional to T_e . In this case the fundamental frequency appears most appropriate so we use ~ 60 GHz

Figure 2-3. The β -corrected phase space plot for the anchor midplane. The stars show the approximate location of the sloshing neutral beam input components produced by 90-deg injection off the midplane at the 2-T a' field point; the crosses show the approximate locations of the 10-deg pumping neutral beam.

gyrotrons. All the equilibrium ECRH power requirements are given in Table 2-4. The neutral beam pumping and ECRH are approximate incident powers with expected losses taken into account.

2.3.4. Vacuum and Halo Requirements

The central cell vacuum requirements for TDF are based upon the requirement that the hot ion charge-exchange loss time τ_{cx} be much longer than the predicted Logan-Rensink Fokker-Planck model²⁰ scattering loss time over the 15-T choke coil. The 11.5-ms hot ion central cell loss time (from the $n\tau$ shown in Table 2-1) then requires $(n_0 \langle \sigma v \rangle)^{-1} > 11.5 \times 10^{-3}$ or $n_0 < 10^9$ for $\langle \sigma v \rangle_{cx} > \sim 9 \times 10^{-8} \text{ cm}^3/\text{s}$. For an expected operating wall base pressure of 10^{-5} Torr this model requires a neutral gas attenuation factor of >350 . Similarly, the transition and anchor regions require that the total input trapping rate of penetrating cold gas (by ionizing or charge-exchanging on the extant warm or hot plasma) be much less than the effective pumping rate to prevent filling in the low density barrier regions. Again the result is a requirement that $n_0 < 10^9$ and the halo produce an attenuation factor $>10^2$.

The 2XIIB, TMX, and TMX-U²² experiments have all shown formation of an outer cool plasma "halo" that pumps most of the cool gas from the walls or beam dumps to the end walls. This attenuates the neutral energy gas flux, which penetrates into the hot plasma interior. The attenuation would require a halo plasma layer $\int n_0 dl > 4 \times 10^{13}/\text{cm}^2$, with $T_e > 40 \text{ eV}$ so that the electrons could efficiently ionize the Frank-Condon neutrals of about 4 eV, which result from short mean-free-path dissociation (or dissociative ionization) of D_2 or T_2 molecules recycling from the wall. Appendix F describes the physics model of the halo predicted for the TDF case with a main halo thickness in the central cell of $\sim 7 \text{ cm}$. This is about equal to a 3.5-MeV alpha fusion reaction product orbit diameter (all W (I)) at the 15-T choke coil field limit when mapped back to the central cell. The attenuation we require agrees well with the expected halo, though it suggests that even higher attenuation and a more efficient halo plasma would offer a longer hot ion lifetime and less gas throughput. However, further increase in halo

$\int n_e dl$ is limited, because it would also become a significant attenuation loss for the incident neutral beams and increase the resultant wall heat loading.

2.4. DESIGN SELECTION FOR STABILITY

The TDF design involves a combination of magnetic and electrostatic features in the end cells of a tandem mirror configuration. This section briefly describes the associated vacuum-magnetic field design needed for flute and ballooning stability. We also discuss the expected axial pressure profile, the warm plasma fraction or stream current needed to stabilize the DCLC instability, and the pressure anisotropy limit required to stabilize the Alfen-Ion-Cyclotron (AIC) instability.

2.4.1. The Magnetic Field Arrangement Used to Provide Stability from Flute and Ballooning

We used computer studies by R. Bulmer, L. Pedrotti, and R. Wong²³ to help design the TDF vacuum-magnetic field such as to reduce parallel currents and associated quadrupole flux surface distortions to a minimum level acceptable for the TDF base case η . The MHD plasma equilibrium and stability were evaluated using the approach of Pearlstein et al.²⁴ Figure 2-4 shows the marginal flute and ballooning stability boundaries, calculated by R. Wong with the TEBASCO code under base case conditions. Note that the design point for a peak anchor $\hat{\beta}_a = 0.5$ and central-cell peak $\hat{\beta}_c = 0.4$ is comfortably stable. This good design stability margin is aided by the fact that only the low- β warm ion component in the central cell is contained by the anchor plugs and recirculates through the transition section (for a transition $\beta_t \lesssim 0.012$), while the main high- β_c component in the central cell is generally lost when scattered over the 15-T choke coil field, and its single-pass density makes a negligible contribution to the β_t in the unfavorable curvature transition section.

The design flexibility in the relatively simple transition magnet section between the choke coil and the anchor allows reducing parallel currents to an acceptably low value, but not $J_{||} \sim 0$ as attained in MFTF-B under

Figure 2-4. Marginal flute and ballooning stability boundaries calculated by R. Wong with the TEBASCO code under base case conditions.

selected conditions with a more elaborate system of transition magnets. However, the remnant quadrupole moment induced radial losses should be minor compared to axial losses.

2.4.2. Neutral-Beam Orientation Necessary To Avoid Alfven Ion Cyclotron Instabilities

The electrostatic AIC instability is produced by strongly anisotropic ion velocity distributions. AIC was an important limiting factor for TMX³; we suppressed it in TMX-U⁴ using angled NBI to produce a sloshing-ion distribution.

We have taken this same approach for TDF. The design uses 65 deg injection (90 deg would be normal to the magnetic axis) in the central cell to attain a Fokker-Planck value of $\langle \beta_{\perp} / \beta_{\parallel} \rangle \sim 2.5$ for a hot sloshing-ion distribution predicted to be stable for the finite plasma diameter of the central cell (calculations by M. Rensink²⁵). The NBI for the anchor cell is arranged for 90 deg injection, but off the midplane to attain a similarly stable sloshing-ion distribution.

2.4.3. Stream Stabilization of Drift-Cyclotron-Loss-Cone Modes

Loss-cone ion distributions tend to drive several instabilities, such as those of the axial-loss cone (ALC) and drift-cyclotron-loss cone (DCLC). The DCLC mode appears to have the more stringent instability limit for TDF conditions. The base case TDF design satisfies the DCLC quasi-linear theoretical requirements²⁶ for the ratio of the warm-ion density (in the loss cone) to the hot ion density $n_w/n_h \sim 0.08$ in the central cell with $r_p/a_i \sim 11$. Fokker-Planck runs also show that this results in an almost monotonic ion energy distribution function. In the anchor, we predict that DCLC stabilization requires a stream-stabilizing plasma flow of about 1600 A. This flow will come from total central cell losses, including about 800 A from D₂ pellet fueling. The stream current density required at the midplane of the anchor is²⁷

$$j_s \approx \frac{1.9 \times 10^{-11} n(\text{cm}^{-3}) (\Delta\phi/4.5)^{3/2} (\text{kV}) \{a_i/r_p\}^{4/3}}{M^{1/2} (\text{AMU}) E (\text{keV}) (R-1)},$$

where j_s is in A/cm², R is the effective mirror ratio, r_p is the plasma radius, a_i is the ion gyroradius, and $\Delta\Phi$ is the approximate average difference in ambipolar potential between the anchor plug and the outboard mirror.

2.4.4. Tandem Mirror Magnetic and Barrier/Plug Combination Used to Avoid Trapped Particle Modes

The magnetic design and operation of the tandem mirror depend upon line-averaged β -weighted field curvature integrals for stability. The approximately neutral stability in the central cell and certain bad curvature regions, such as the transition, will be stabilized by their connection to the good curvature high- β anchor cells. Recent studies by Rosenbluth et al.¹⁹ have shown the possibility of trapped-particle instability modes isolated from the MHD anchors. To avoid these modes, Pearlstein et al.¹⁵ note that the main ion potential plug at the outboard side of the anchor must be arranged so that a sufficient population of the contained warm ions (about 10%) passes through the machine, including the good curvature anchors, before recirculation. This will satisfy:

$$\omega_{*i}/\gamma_c > \frac{4(1 + A_r)}{A_r^2} ,$$

where the Rosenbluth factor

$$A_r = (r_p/a_i)^2 \frac{B_c}{B_a} \left(\frac{g_b n_p}{n_c} \right) \times \left(\frac{2L_a}{L_c} \right) ,$$

- ω_{*i} is the diamagnetic drift frequency,
- γ_c is the MHD characteristic instability growth rate,
- r_p/a_i is the ratio of the plasma radius to the ion Larmor radius in the central cell,
- $(g_b n_p/n_c)$ is the ratio of total density on the transition to the central cell density,
- L_a/L_c is the ratio of anchor stabilizing length to the central cell length.

We meet the need for a warm ion turning point using all of the good curvature in the anchor by running the axisymmetric choke coil only as a magnetic barrier for the central cell hot ion plasma components and forming an asymmetric anchor potential with a minimum potential electron barrier at the anchor midplane and the maximum potential warm ion plug at the anchor sloshing-ion outboard density peak. Figure 2-1 shows the machine magnetic and electrostatic potential axial distribution, and defines the input beam and ECRH locations at points a, b, and a'.

2.5. EFFECT OF FULL THERMAL BARRIER OPERATION

DCLC requirements for the anchor cells determine the streaming current for the base case. However, the sloshing-ion distribution produced by off-midplane NBI into the anchor for AIC stability will also trap and fill in a warm plasma. This plasma will tend to build up a monotonic ion distribution similar to that of a central cell of a simple tandem, though the anchor pump beam will prevent the loss cone from being completely filled. The resultant distribution should markedly reduce the stream current density required for stability.

We are continuing to make Fokker-Planck calculations of particle balance and streaming requirements for this case. Here we explore the impact of using only an effective streaming flow (hot ion loss) of about 800 A from the central cell, derived from the code of Section 2.2.2, with a no-pellet injection case. In the initial calculations we found that the lower stream flow allowed both T_e and T_{ic} to increase but required a density decrease for $\beta_c \lesssim 0.4$. However, since $\langle \sigma v \rangle_{D-T}$ is relatively flat for average ion energies $W > 40$ keV, we reduced the neutral beam voltage and increased the density to maintain the same average β and hot ion energy in the central cell (just below 40 keV), but over a larger 15-cm radius plasma. This requires higher neutral-beam input current but about the same power to obtain a predicted higher wall loading of $\Gamma_n \sim 2.1 \text{ MW/m}^2$. The parameter set for this neutral-beam, high-voltage-adjusted, larger-plasma-diameter, no-pellet injection case is shown in Tables 2-5 and 2-6. We expect further optimization for this case.

Table 2-5. Design levels for the tandem mirrors of the TDF central cell, without a stream plasma.

Parameter	Value
Plasma length (m)	8
Plasma radius (m)	0.15
Solenoid field (T)	4.5
Peak beta, $\hat{\beta}_c$	0.4
Electron temperature, T_{ec} (keV)	4.1
Hot ion average energy, W_{ih} (keV)	37
Hot ion lifetime, τ (ms)	26
Hot ion density, n_{ih} (cm ³)	5.25×10^{14}
Warm ion temperature, T_{iw} (keV)	10
Warm ion density, n_{iw} (cm ⁻³)	5.25×10^{13}
Warm ion density times lifetime, $n\tau$ (cm ⁻³ s)	6.14×10^{12}
Beam voltage (kV)	55
Trapped beam power (MW)	51 (1244 A/41 keV)
Injection angle (deg)	65
Beam trapping fraction, f_t	0.94
Fusion power (MW)	35.6
Neutral wall flux at R = 0.27 m, Γ_n (MW/m ²)	2.1
Central cell potential, ϕ_e (kV)	25

Table 2-6. Design levels for the tandem mirrors of the TDF end cells, without a stream plasma.

Parameter	Value
<u>Choke coil</u>	
Maximum magnetic field strength (T)	15
Plasma length (m)	4.65
Plasma radius (m)	0.45
Plasma density (cm ⁻³)	4.25×10^{12}
Magnetic field at midplane (T)	1
Pumping NBI*	80 kV, (92 A/68 keV) 6.3 MW
Injection angle (deg)	30
Barrier potential, ϕ_b (kV)	20
<u>Anchor coils</u>	
Plasma length (m)	3.3
Plasma radius (m)	0.28
Plasma density (cm ⁻³)	8×10^{12}
Magnetic field at mirror (T)	3
Magnetic field at midplane (T)	1
Anchor cell potential, at a' (kV)	20
Sloshing neutral beams*	80 kV, (11.3 A) 0.68 MW
Beam trapping fraction	0.29
ECRH (total absorbed) (MW at GHz)	1 at 35, 0.560 at 60
Hot electron temperature (keV)	466
Warm electron temperature (keV)	21.2
Confining potential, ϕ_e (kV)	12
Pumping NBI*	40 kV, (14.8 A/34 keV) 500 kW

* Total powers, incident to both ends. Pumping beam requirements are for a 90%:7%:3% ion source output species mix.

2.6. FUSION REACTOR CONDITION WALL LOADING

In designing TDF our major goal was to produce fusion reactor-like wall loading for engineering materials testing. The primary feature of this wall loading is the 14.1-MeV neutron flux $\Gamma_n \geq 1.4 \text{ MW/m}^2$. A secondary feature is the front surface heat loading from exposure to the hot and halo plasmas, expected to average $<50 \text{ W/cm}^2$, but with locally much higher heat loads near NBI as discussed in Appendix D.

2.6.1. D-T Neutron Yield and Wall Loading

The machine parameters listed in Table 2-1 include a calculation of the D-T neutron yield and wall loading for the axisymmetric central cell. The neutron wall loading has the form

$$\Gamma_n \left(\frac{\text{MW}}{\text{m}^2} \right) = \frac{0.45 n^2 \langle \sigma v \rangle_{\text{DT}} \pi r_p^2}{4 \times 2 \pi r_w} \times \frac{14.1 \text{ MeV} \times 10^4}{6.25 \times 10^{18}},$$

where

n is total peak density in cm^3 ($n_D = n_T = n/2$),
 $\langle \sigma v \rangle_{\text{DT}}$ is the calculated D-T reactivity in cm^3/s ,
 r_p is plasma radius in cm,
 r_w is the wall radius in cm.

The factor 0.45 comes from integrating the assumed cubic density profile $\{\alpha[1-(r/r_p)^3]^2\}$ over the plasma radius, and the final factor converts the reaction rate loading/ cm^2 to MW/m^2 . We based the initial calculation for $\langle \sigma v \rangle_{\text{D-T}}$ on a simplified hot ion distribution function generated by considering the trapping and electron drag-dominated equilibrium for each of the three neutral beam input energy components separately to form a 50% D^+ and a 50% T hot reacting plasma with an average energy of $\bar{W} \sim 39 \text{ keV}$. Calculations of $\langle \sigma v \rangle_{\text{D-T}}$ for this case made with the SIGMAV code developed by DeVoto and Slaughter²⁸ yielded a hot-plasma reactivity of $\langle \sigma v \rangle_{\text{D-T}} \sim 7.8 \times 10^{-16} \text{ cm}^3/\text{s}$. We found the hot ion D-T fusion

reaction with the 78% warm DCLC stabilizing deuterium ion background to be negligible. Therefore, for use with the total (hot and warm) plasma density n , the corrected average reactivity is $\langle\sigma V\rangle_{D-T} \sim 6.8 \times 10^{-16} \text{ cm}^2/\text{s}$ for a mean total plasma density average ion energy of $W \sim 36 \text{ keV}$.

This particular D-T reactivity for the hot plasma with $W \sim 39 \text{ keV}$ is in excellent agreement with previous calculations by Marx et al.²⁹ for a simple mirror contained plasma with a mirror ratio $R = 5$. However, more recent Fokker-Planck calculations of W for the TDF case show an average energy W dependence on the warm-ion net confining potential ϕ_C . For $\phi_C = 4 \text{ kV}$, Rensink's Fokker-Planck calculations yield $W = 29 \text{ keV}$, with a slightly lower ϕ_C required for a self-consistent fit of ion losses to the stream current we used in the base case parameter set of Table 2-1. The calculations could be iterated further; however, the resultant value of Γ_n turns out to be about 1.4 MW/m^2 in either case; this is because a lower W allows a higher n (since $\beta_C \sim 0.4$ is regarded as fixed) and $\langle\sigma V\rangle_{D-T}$ is relatively insensitive to energy, near or above 40 keV . The base case line source neutron yield calculated for D-T, D-D, and T-T reactions is shown as a function of Z in Fig. 2-5.

2.6.2. First Wall Thermal Loading

The first wall in the central cell is also subject to substantial thermal radiation from its exposure to the hot reacting plasma. The front first-surface loading includes:

- An energetic neutral-particle flux from hot plasma ions undergoing charge-exchange on either of the attenuated neutral beams, on superthermal neutrals penetrating through the outer halo plasma, or on incompletely stripped impurities. We estimate the average heat loading to be from 10 to 30 W/cm^2 for expected vacuum conditions, with a strong dependence on wall positions near the NBI system and on halo attenuation of wall-recycled gas.
- Vacuum ultraviolet-line radiation estimated at an average of a few W/cm^2 at the wall. This radiation is caused mostly from the excitation of moderate- to high- Z impurity ions that are partially stripped. Thus, such loading is strongly dependent on the

Figure 2-5. The line source neutron yield calculated for D-T, D-D, and T-T reactions, as a function of Z.

fractional impurity density, charge q-state, and T_e . The effective charge q-state is modified from expected coronal equilibrium by NBI and by any charge-exchange source for electron pickup. The oxygen impurity accumulation expected in TDF is discussed in Appendix G.

- Electron-cyclotron emission (ECE) from the hot electrons gyrating in the high magnetic field. Losses with expected self-absorbing and wall reflection are estimated to be a few W/cm^2 . Power is proportional to $n_e T_e B^2$.
- Ordinary hot plasma electron bremsstrahlung is predicted to be $< 1 W/cm^2$ proportional to $n_i n_e Z_i^2 (T_e)^{1/2}$.
- Local high-power fluxes caused by forward-peaked neutral-beam charge-exchange neutral-beam "scattering" in regions near the remnant neutral beam dump and near ECRH horns (or refracted or reflected paths). Loading will depend on NBI design, divergence, and the plasma β . Total loads are likely to be several hundred W/cm^2 near neutral beam dumps. This topic is discussed further in Appendix D. The results there indicate an unexpectedly high local heat load requiring a larger (75cm) radius first wall in the NBI input region.

APPENDIX A, CODE FORMULATION FOR THE TDF CASE

We used the MARS base code.¹⁷ Its expression for the transition region charge-exchange pumping power is derived from the trapping current given by Futch and Lodestro.³⁰ In the form the MARS code uses:

$$P_{\text{pump}}(\text{MW}) = 4.49 \times 10^{-33} V_t \bar{n}_p^2(t) \frac{[1 + 0.0441 R_t]^{3.33} g_p f_{\text{ion}}}{P_{\text{cx}} T_c^{3/2} (g_t - 1)} \quad (\text{A-1})$$

$$(x) \frac{g_t^{3.33}}{(g_t - 1)^{2.33} A} \bar{E}_{\text{pump}} \frac{(L_t + 0.78 L_a)}{L_t}$$

where

- V_t = the volume of the transition region (cm^3),
 $n_p(t)$ = the passing ion density at the bottom of the transition region (cm^{-3}), with the bar above n_p^2 representing a radial profile average using cubic profiles,
 R_t = the plasma mirror ratio between the barrier field and transition midplane field,
 f_{ion} = the factor ~ 1.2 used to model the fact that some of the charge-exchange neutrals will be reionized before leaving the plasma,
 p_{cx} = the probability that an interaction between a beam particle and the background plasma is a charge-exchange event,
 T_c = the central cell warm ion temperature (keV),
 g = the ratio of the trapped-plus-passing density to the passing density, with subscript t the transition value and subscript p the pump-beam location value ($g_p = 1.16 g_t$),
 A = atomic mass of the background plasma (amu),
 E_{pump} = the pump beam injection energy (keV),
 L_t = the transition region length (cm),
 L_a = the anchor region length (cm).

The expression for the power required to sustain the sloshing ions at the thermal barrier may be written:

$$P_{nb,a}(\text{Mw}) = 4.1 \times 10^{-32} \frac{(G_b - g_b)^2 n_p^2(b) E_{inj,a} v_b}{A_s E_{inj,a}^{3/2} \log_{10}(R_{eff,s})} \frac{B_b}{B_a}, \quad (\text{A-2})$$

where:

- G_b = the ratio of total ion density (passing + trapped + sloshing) to passing ion density,
 g_b = the same as g_p and g_t in Eq. (A-1) but at the barrier midplane,
 $n_p(b)$ = the passing ion density at the barrier midplane (cm^{-3}),
 $E_{inj,a}$ = sloshing ion beam energy at the injection point (keV),

- V_b = thermal barrier volume (cm^3),
 $B_{b,a}$ = plasma magnetic field at the barrier (point b) and potential peak (point a),
 A_s = atomic mass of sloshing ions (amu),
 $E_{inj,a}$ = sloshing ion energy at the potential peak (keV),
 $R_{eff,s}$ = the effective sloshing ion mirror ratio, including the effects of expelling potential.

The formula used to compute the power required to sustain the hot electrons is:

$$P_{ECRH,b}(\text{MW}) = 1.52 \times 10^{-31} (1 - F_{ec}) \frac{\overline{n_p^2(b)} E_{eh}^{-1/2} V_b}{\log 10 R_{beff}} + P_{sync} \quad , \quad (\text{A-3})$$

where the previously undefined symbols are:

- F_{ec} = the cold electron fraction at the barrier,
 E_{eh} = hot electron energy, (keV),
 R_{beff} = the effective hot electron mirror ratio,
 P_{sync} = the synchrotron radiation power, given by:

$$P_{sync}(\text{MW}) = 1.21 \times 10^{-19} B_{b,vac}^2 \overline{n_p^2(b)} V_b \left(\frac{E_{eh}}{10^3} + \frac{E_{eh}}{10^3} \right)^2 \quad (\text{A-4})$$

The power formula for the warm electrons which exist at the potential peak (point a) is:

$$P_{ECRH,a}(\text{MW}) = 5.85 \times 10^{-31} \frac{\overline{n_{ew}^2(a)} V_b (T_{ew} - T_{ec})}{T_{ew}^{3/2} [\exp(\delta\phi_a/T_{ew}) - 1]} \left(1 + \frac{B_b}{E_a} \right)^{-1} \quad , \quad (\text{A-5})$$

where

- $n_{ew}(a)$ = warm electron density at point a (cm^{-3}),
 T_{ew} = warm electron temperature (keV),
 T_{ec} = central cell electron temperature (keV),
 $\delta\phi_a$ = the potential difference between the bottom of the thermal barrier and the potential peak.

The power required to sustain the hot ion population in the central cell is the largest single requirement and is given by

$$P_{nb,cc} \text{ (MW)} = 1.6 \times 10^{-22} \frac{n_{H,c} n_{cc} E_{inj,c} V_{cc}}{\langle n_T \rangle_{H,cc}}, \quad (\text{A-6})$$

where

- n_H = hot ion density in the central cell (cm^{-3}),
- n_{cc} = total plasma density in the central cell (cm^{-3}),
- $E_{inj,c}$ = average beam injection energy in the central cell (keV),
- V_{cc} = central cell volume (cm^3),
- $\langle n_T \rangle_{H,cc}$ = hot ion confinement parameter (s/cm^3), computed from the Logan-Rensink model.²⁰

APPENDIX B, AN EXAMPLE OF PLASMA STARTUP IN PROGRAMMED STEPS

We have considered an example case in which the initial condition places both the central and anchor cell betas at 1/10 of their equilibrium values. Since the ratios are roughly equal, we set g for the central cell and G for the anchor cell at their equilibrium values for the initial conditions. Based on pressure balance in the barrier, the initial, computed, hot electron density and temperature are similar to that in Elmo bumpy torus (EBT) operation, and those techniques might apply here.

Tables B-1 and B-2 show the set of cases run with the equilibrium code to model startup. Notice that they do not mention time. The time scales are determined once the source strengths are specified (or vice versa) to give the quoted β and g values.

Table B-1 defines the parameters of Table B-2. The first three steps increase barrier beta, β_b . This keeps the central cell and barrier densities approximately fixed (by keeping g and G fixed), while the added ECRH power at b increases the hot electron energy E_{eh} to almost 1 MeV. We need the slight increase in g to keep E_{eh} below 1 MeV. At this point the absorbed power needed to sustain the hot ions in the central cell (P_{cc}) is quite modest, as is the power required to pump out the barrier and transition region (P_{pump}). This is because the central cell density is still quite low, and both of these powers are proportional to the square of this density. The streaming current requirement j_{st} is also about an order of magnitude below the steady-state value. Notice that in this initial phase, the trapping fraction of the sloshing beam is quite low.

In the next three steps, which are virtually the same, we increase the density in the barrier by raising g and G at fixed barrier β . This lowers the hot electron energy and the warm electron temperature and dramatically increases the ECRH powers required, but not above the steady-state powers. The central cell beam power and pumping power remain at modest values.

The last four steps show the sequence of events that occurs when the central cell β is ramped up along with the barrier β to the operating points. Comparing this with the data of Tables 2-1 and 2-2 shows that the first powers are incident for both ends, whereas these are absorbed for both ends. Also note that since internally the code assumes no half- and third-energy beam components, the pump beam power is lower than that presented

Table B-1. Definition of the parameters used in Table B-2.

Symbol	Definition
g	Ratio of the sum of the local trapped + passing ion densities to the passing ion density. It applies in the anchor and transition regions.
G	Ratio of the sum of the local trapped + sloshing + passing ion densities to the passing ion density. It applies in the anchor region.
β_c	Beta value in the central cell.
β_b	Beta value in the anchor/barrier region.
T_{ew}	Warm electron temperature at the potential peak (keV).
T_{ec}	Central cell electron temperature (keV).
E_{eh}	Hot electron energy at the barrier midplane (keV).
j_{st}	Warm plasma stream current required for stability (A).
P_{cc}	Neutral beam power in the central cell (MW).
P_{pump}	Charge-exchange pump beam power in the transition region (kW).
P_{slosh}	Sloshing-ion neutral beam power (kW).
$P_{ECRH,a}$	The ECRH power required at the potential peak, point a (kW).
$P_{ECRH,b}$	The ECRH power required at the thermal barrier midplane, point b (kW).

* All powers are absorbed in plasma; end cell powers are for both ends.

Table B-2. Values of the parameters used in a TDF-startup scenario.

Sequence of steps	Parameter values												
	q	G	B_c	B_h	T_{ew} (keV)	T_{ec} (keV)	E_{ph} (keV)	J_{ct} (A)	P_{cc} (MW)	P_{pump} (kW)	P_{slush} (kW)	$P_{ERCH,a}$ (kW)	$P_{ERCH,b}$ (kW)
Phase I													
1.	3	6	0.04	0.05	4.2	1.1	205	106	1.7	39	8 (0.04)*	9	15
2.	3	6	0.04	0.1	4.11	1.2	443	104	1.7	39	5.4 (0.04)	8.8	20.6
3.	3.8	7.6	0.04	0.25	3.7	1.1	968	116	1.8	37	3.5 (0.05)	14	73
Phase II													
4.	9	18	0.04	0.25	3.45	0.87	383	237	2.7	56	17.5 (0.12)	100	161
5.	9.75	19.5	0.04	0.25	3.43	0.86	351	253	2.8	59	20.4 (0.13)	121	180
6.	10	20	0.04	0.25	3.54	0.85	349	261	2.9	61.5	21 (0.13)	125	182
Phase III													
7.	9.23	18.5	0.13	0.3125	3.95	1.7	151	966	15.6	536	154 (0.33)	734	789
8.	5.45	10.9	0.22	0.375	4.5	1.61	209	1150	26.2	907	126 (0.31)	501	526
9.	3.9	7.8	0.31	0.4375	4.8	1.96	274	1264	36.5	1650	100 (0.3)	379	378
10.	3	6	0.4	0.5	6.6	2.2	400	1536	50.1	2440	74 (0.28)	296	227

Note: The goal is met for the largest power requirement ($P_{cc} + P_{pump}$). We need to accomplish further work where work where Phase II and Phase III meet. The $q = q(B_c, B_h)$ needs to be optimized.

*The numbers in parentheses are the slushing-beam-trapping fractions.

in Table 2-2. To account for the 90% full-, 7% half-, and 3% third-energy components for the special pump beams, we need to multiply the P_{pump} column by 3/2; also, the ECRH powers must be multiplied by 2 to account for a conservative 50-50, extraordinary-ordinary mode mix that comes out of the gyrotron and is divided by the 90% deposition fraction. When the central cell β is increased, both the central cell beams and transition pumping beams, which comprise the largest power requirement, are monotonically increasing during the buildup. This is consistent with our goal that startup should not take more power than the final state.

However, the sloshing beam power and the ECRH power pose a problem because they grow larger than for the operating point by a factor of between 2.5 and 3.5. This occurs once the barrier density has built up, the electron energy has been reduced, and the central cell and barrier betas have been increased. It suggests that the central cell beta we chose for a given barrier beta was too large for our g and G values. Comparing cases 6 and 7 we see that the g and G values are virtually the same, but the ratio of β_c to β_b is larger by a factor of 2.6. (The barrier density in case 7 is also a factor of 2.6 larger.) The higher density suggests a lower electron energy; both translate to higher ECRH power.

A possible solution would be to raise β_c more slowly, keeping β_c/β_b fixed at fixed G and g , until barrier beta reaches its equilibrium value. Then we could raise β_c while lowering g and G and keeping $\beta_c g$ and $\beta_c G$ constant. This would maintain the barrier parameters, and hence the sustaining powers, somewhat constant. These ideas should be tested further.

APPENDIX C, FACTORS RELATED TO NEUTRAL BEAMS

The Ion Source Species Mixture

The arc plasma in an ion source that uses a molecular gas typically involves several ion states, with the specific ion species mix depending on the arc conditions and wall effects. For a positive ion source involving D_2 or T_2 gas, the main full energy $w \equiv eV_0$ extracted ion species are D^+ , D_2^+ , and D_3^+ . Then total ion current $= I^+ (f_1 + f_2 + f_3)$, where $f_1 \equiv$ the fraction of D^+ , $f_2 \equiv$ the fraction of D_2^+ , $f_3 \equiv$ the beam fraction of D_3^+ . This is true when we ignore possible impurities and negative ions.

In the gas-filled charge-exchange neutralization cell, the D^+ fraction at the full energy can undergo electron pickup with the cross section $\sigma_{10}(w)$, and the resultant D^0 can undergo electron loss with the cross section $\sigma_{01}(w)$. The equilibrium neutral fraction is the product of the full energy neutral fraction $F_\infty^0 \equiv \sigma_{10}(w)/[\sigma_{10}(w) + \sigma_{01}(w)]$ and a gas-cell beam attenuator or thickness parameter $f_\pi(w) \equiv 1 - \exp(-\sigma_T/ndl)$, where the total cross section $\sigma_T(w) = \sigma_{10}(w) + \sigma_{01}(w)$. This assumes we neglect small negative ion fractions. The molecular ions must first undergo one or more dissociative collisions before forming a beam of half-energy or one-third-energy D^0 or D^+ components; each of which have a similar equilibrium neutral fraction at the appropriate energy. For relatively thick neutralizer cells in this case, we assume that the molecular ions completely dissociate into atomic fragments with relatively short mfp. Then each of the resultant fractional energy neutral beams have the same simple parameter for cell thickness and the same form:

$$\begin{aligned} I^0(w) &= I^+ \times f_1 \times F_\infty^0(w) \times f_\pi(w) , \\ I^0(w/2) &= I^+ \times f_2 \times 2F_\infty^0(w/2) \times f_\pi(w/2) , \\ I^0(w/3) &= I^+ \times f_3 \times 3F_\infty^0(w/3) \times f_\pi(w/3) . \end{aligned} \tag{C-1}$$

The average neutral beam energy $w = \Sigma p_i / \Sigma I_i^0$, with $p_i = I_i w_i$ for $i = 1$ to 3, or

$$\bar{w} = w_0 \left[\frac{f_1 F_\infty^0(w) f_\pi(w) + f_2 F_\infty^0(w/2) f_\pi(w/2) + f_3 F_\infty^0(w/3) f_\pi(w/3)}{f_1 F_\infty^0(w) f_\pi(w) + 2f_2 F_\infty^0(w/2) f_\pi(w/2) + 3f_3 F_\infty^0(w/3) f_\pi(w/3)} \right].$$

It is common to operate the gas cell thickness $\Delta n l$ so that $f_\pi(w) \approx 0.95$; this limits the neutralizer gas flow and minimizes scattering, which results in $\Delta n l \sim 3/\sigma_T(w)$. Evaluating the various cross sections for a typical 80-kV ion source mix of 80:15:5 for $D^+:D_2^+:D_3^+$, we get an average neutral beam energy of just over 60 keV, with full energy, half energy, and one-third energy D^0 currents of 55:29:16 respectively. Figure C-1 shows curves for $\bar{w}$ for typical species mix combinations of D^0 or T^0 beams.

The Neutral Beam Trapping Fraction

The factors involved in calculating the neutral beam trapping fraction f_t are (1) the input neutral beam optics, including minimum divergence; (2) the chord length and interaction geometry of the plasma with the target beams; and (3) the appropriate relative velocity-dependent ionization cross sections σ_{ii} and σ_{ei} (plus a correction for one or more generations of σ_{cx} events prior to final reionization).

We take the neutral beam profile to be a perfectly focused nearly plane parallel beam. It has a focal length of about 10 m, with an intrinsic beam of $1/e$ at a half-angle divergence of $\theta_0 \approx 3.5 \text{ deg}/(v_0)^{1/2}$ or 0.4 deg for an 80-kV NBI system. Fractional energy components have a slightly higher divergence, which we ignored. For the relatively long TDF plasma column target, we will consider only a centered-on-axis, 1-D beam focus (transverse to the axis) and a near-Gaussian minimum divergence. The divergence-limited, neutral beam current density then has the form

$$j = j_0 \exp[-(\theta/\theta_0)^2] \quad (C-2)$$

Figure C-1. Average D^0 energy for an 80-kV NBI system with a D_2 gas fill in the charge-exchange cell.

For a large focal distance L and for small angles, $\theta = y/L$, where y is the transverse dimension in the plasma column and θ_D (Rad) = $0.4/57.3 \sim 0.007$. For the case of $L \sim 10$ m and with y in cm,

$$j = j_0 \exp [-(y/7)^2] \quad (C-3)$$

This simple Gaussian approximation to the beam makes it clear that a plasma with a radius of 10 cm will have $f_t < 0.95$, simply because the Gaussian wings of the beam are missing the plasma column. The comparatively-sized beam and plasma target dimensions require that the beam attenuation calculation include the geometry of the beam current density profile, the plasma target density profile, the changing beam chord length, and corrections for forward-angle-weighted charge-exchange in a relatively small geometry. Neglecting this charge-exchange correction, f_t for an axisymmetric plasma has the form

$$f_t = \frac{\int_0^{10} j_0 \exp [-(y/7)^2] \cdot \left[1 - \exp \left(\frac{-\int_{-x_m}^{+x_m} n \langle \sigma v \rangle dx}{(\sin \theta) v_b} \right) \right] dy}{\int_0^{\infty} j_0 \exp [-(y/7)^2] dy} \quad (C-4)$$

where

$a \equiv$ plasma radius,

$\langle \sigma v \rangle_i \equiv (\langle \sigma_{ii} v_{iR} \rangle + \langle \sigma_{ie} v_e \rangle)$,

$v_b =$ beam component velocity; v_{iR} , equals relative ion-neutral collision velocity,

$\theta =$ angle of injection relative to machine axis,

$x_m(y) = (a^2 - y^2)^{1/2}$,

$n(y) = n_0 \left[1 - \left(\frac{x^2 + y^2}{a^2} \right)^{3/2} \right]$,

$$\int n \cdot dx = 2n_0 \left\{ \frac{3\sqrt{a^2 - y^2}}{4} - \frac{3y^2}{8a^2} \left[\sqrt{a^2 - y^2} + \frac{y^2 \ln[(\sqrt{a^2 - y^2} + a)/a]}{a} \right] \right\}.$$

Adding a correction for one or more generations of charge-exchange events prior to a final reionization definitely complicates the analytical calculation of the total f_t for this very finite geometry. A more detailed Monte Carlo code calculation for this problem appears in Appendix D.

APPENDIX D, A MONTE CARLO STUDY OF CHARGE-EXCHANGE FIRST WALL POWER DENSITIES FOR THE TDF CENTRAL CELL BEAMS

Overview

Sets of four central cell neutral beams at each end of TDF provide a total of 31.43 MW of injected power. A fraction of these incident beams are ionized and trapped in the plasma. The remaining neutrals either penetrate with no interaction and are absorbed by the beam dumps or undergo charge-exchange with the plasma ions. The secondary neutral atoms formed in the charge-exchange process either escape and impact the first wall or undergo ionization or further charge-exchange interactions with the plasma. Therefore, considering particle and energy balance, a fraction of the incident beam power is absorbed in the plasma, a fraction escapes as shine-through to the beam dumps and the remainder appears as charge-exchange neutral bombardment of the surrounding first wall. Depending on the beam and plasma parameters, the charge-exchange neutral power to the first wall can be a significant fraction of the incident beam power. Thus, it is important to accurately predict the power density distribution of the charge-exchange flux over the first wall surface.

In general, modeling the steady-state neutral-beam/plasma/wall system in three spatial dimensions is a very complex process. Various analytical and numerical investigations have incorporated simplifying assumptions regarding system parameters.^{31,32} However, we must use Monte Carlo methods^{33,34} if we wish to formally investigate charge-exchange phenomena in a three-dimensional system and retain those phase-space anisotropies inherent in the interaction of a neutral beam with mirror-confined plasmas. Accordingly, we adapted a Monte Carlo neutral beam interaction code, MCNEUT, and employed it to model charge-exchange production and transport for the TDF central cell beams. In addition to computing general particle and power balances for neutral beam interaction, we mapped charge-exchange power density distributions over the first wall surface in both the axial and azimuthal directions.

Monte Carlo Modeling of the TDF Beam-Plasma Interaction

MCNEUT was originally written at LLNL to model the energy spectra and spatial origination positions of charge-exchange fluxes in the 2XIIB minimum-B mirror device.³⁵ The authors configured the code for single-species beams on single-species plasmas and modified it later to accommodate multi-species beams on multi-species plasmas and to allow for more than one incident beam direction.

The code follows many neutral particle histories in three dimensions from their emission through a target plasma, which is represented by a bi-Maxwellian distribution function in steady-state. We simulate ionization and charge exchange events by Monte Carlo techniques and then follow descendent generations of neutrals until each particle is either ionized and integrated into the background plasma or escapes and bombards the first wall.

For the TDF central cell neutral beams, we modeled the plasma as a cylinder 5 m long with a 0.10 m radius. We configured four 80-keV neutral beams spaced 50 deg apart to subtend an angle of 65 deg relative to the z-axis. The intersection was at the midpoint of the 5-m-long cylindrical plasma, 2.5 m from one end. For convenience, this intersection point is designated as $z' = 0$ for defining the axial location of charge-exchange flux distributions, different from the $z = 0$ datum coordinate at the center of the machine. We portioned the target plasma radially into 32 annular zones, assuming constant plasma properties in each. Escaping charge-exchange neutrals were scored on a cylindrical first wall detector grid with 32 azimuthal bins in the θ direction and 20 axial bins in the z' direction; the bin widths were $\Delta\theta = 10$ deg and $\Delta z' = 5$ cm, respectively.

Table D-1 lists the input parameters we employed in the code. Note that the plasma density profile is Gaussian, whereas the TDF density profile is cubic with a plasma radius of 10 cm. Accordingly, we chose the scale length of the Gaussian profile such that, when it was normalized to the same peak (on-axis) number density and some radial cutoff, both profiles yielded the same plasma number density per unit length in the z-direction. One other feature should be noted: the incident beams are modeled as right-circular cones emanating from point sources with a divergence half-angle defining the standard deviation of a Gaussian beam profile. We chose the distance from

Table D-1. Beam-plasma first-wall parameters for the TDF charge-exchange wall flux computation.

Parameter	Value
Number of central cell beams at each end	4
Injection angle (deg)	65
Angle between beams (deg)	50
Beam energy at full energy component (keV)	80
Beam fractions at full, 1/2, 1/3 energy components (%)	55, 29, 16
Beam species	D-T
Injected particle current per beam (A)	131
Total injected current at each end (A)	525
Beam profile	Gaussian
Beam source--plasma distance (m)	5.55
Beam half-angular divergence (deg)	0.4
Cylindrical plasma length (m)	5
Beam intersection point from one end (m)	2.5
Plasma radius (cm)	10.0
Plasma profile	Gaussian
Plasma radial scale length (cm)	10.0
Plasma density (cm^{-3})	6.5×10^{14}
Temperature perpendicular to the magnetic field (keV)	34
Temperature parallel to the magnetic field (keV)	4
Electron temperature (keV)	2.1
On-axis magnetic field strength (T)	4.5
Number of annular plasma zones	32
Plasma radial zone width (cm)	0.31
Cylindrical radius of first wall (cm)	25
Dimensions of first wall detector grid	32 x 20
Grid azimuthal bin width (deg)	10
Grid axial bin width (cm)	5
Number of neutral histories	40,000

each beam point source to the $z' = 0$ point on-axis such that, with a divergence half-angle equivalent to the expected beam divergence of ~ 0.4 deg, less than 1% of the beam footprint fell outside the plasma diameter.

Results

Table D-2 summarizes overall particle and power balances as determined by MCNEUT for the input shown in Table D-1. Note that these results are for the four central cell beams at one end of the machine. Note carefully the definitions of trapping fraction, total trapped current and net trapped current. The Monte Carlo statistics for these integral quantities are $< 0.6\%$.

Also note that the ratio of the net trapped current to incident current does not equal the ratio of the net trapped power to the incident power. This is because an incident beam particle of fixed energy can charge-exchange with plasma ions that have a distribution of energies. Since some of these secondary neutrals can charge-exchange or escape before becoming ionized, this will, in general, result in a plasma energy trapping that does not scale directly with the particle trapping. Finally note that, because of the model we used for the neutral beams (i.e., conical Gaussian distributions emitted from a point source), the particle and energy balances do not necessarily coincide with those calculated earlier in this physics section. There, we assumed rectangular Gaussian beam footprints of $\sim 1 \times 0.14$ m, whereas the Monte Carlo model employs circular Gaussian beam footprints with diameters of ~ 0.14 .

Besides the integral quantities in Table 1, we obtained charge-exchange power density distributions at the 32×20 detector grid on the first wall surface. Figure D-1 is a polar plot of the charge-exchange power density distribution in the xy plane for one of the four beams at a z' value of -0.025 m; in other words, this is the azimuthal variation of charge-exchange power density over the first wall at an axial position displaced 2.5 cm from the beam intersection point at $z' = 0$. The direction of the incident beam on this plot is at $\theta = 90$ deg. Also, because the beam injection angle is 65 deg relative to the machine Z axis, this incident beam on this plot is inclined 25 deg out of the xy plane of the figure. Figure D-2 is the corresponding polar plot when all four beams are included. It shows the four individual distributions and their composite distribution. The maximum power density is $\sim 2369 \text{ W/cm}^2$ at $\theta = 185$ deg.

Table D-2. Particle and power balances for the TDF central-cell beams, determined from four beams at one end of the device.

Measurement	Result
<u>Particle balance (s^{-1})</u>	
Incident current	3.27×10^{21}
Trapping fraction ^a	0.99
Total trapped current ^b	3.24×10^{21}
Net trapped current ^c	2.66×10^{21}
<u>Power balance (MW)</u>	
Incident power	31.4
Shine-through power to dumps	0.33
Charge-exchange power to walls	4.0
Net trapped power	27.0

^a Fraction of incident particles that undergo at least one interaction (charge-exchange or ionization) with the plasma.

^b Number of particles/s that undergo at least one interaction (charge-exchange or ionization) with the plasma.

^c Number of particles/s that are ionized and become trapped in the plasma. This is equivalent to a fueling rate.

Figure D-1. Polar plot of charge-exchange wall flux in the x-y plane for one of four D-T beams on D-T plasma. The beam direction is 25 deg out of plane and $Z = -0.025$ m.

Figure D-2. Composite polar plot of charge-exchange wall flux in the x-y plane for all four D-T beams on D-T plasma. The beams are 25 deg out of plane and $Z = -0.025$ m.

From the code output, it is possible to construct polar plots similar to Fig. D-2 for each of the 20 axial grid positions along the first wall surface. However, it is more instructive to pre-process this data and select only the maximum, minimum, and average charge-exchange power densities at each axial position and then plot these as functions of the corresponding z' coordinate. Accordingly, these data are shown in Fig. D-3. Peak values of the maximum, average, and minimum power density distributions are 2380, 1875, and 1270 W/cm² at the axial positions of $z' = -2.5, -1.5$ and -0.5 cm, respectively.

Discussion of Results

One of the major features of the charge-exchange wall flux for the single beam incident on a thick target such as the central cell of TDF is the strongly backward-peaked distribution; in other words, maximum charge-exchange power densities occur at ~ 180 deg relative to the incident beam direction. It is also interesting to note that the distribution is quite symmetric about the y-axis. This is to be expected in view of the incident beam direction. The slight deviations from true symmetry are, of course, attributable to Monte Carlo statistics ($\sim 6\%$ here).

The strong backward peaking of the single beam distribution in the composite has been somewhat ameliorated by the superposition of four beams 50 deg apart. However, the composite wall flux is still dominant in the general backward direction. Note that when the machine passes through startup, neutral fluxes progress from being strongly forward-peaked at low plasma densities to backward-peaked at full plasma conditions.

This backward peaking should be contrasted with the forward peaking observed³⁴ in the Mirror Advanced Reactor Study (MARS) for the pump beam charge-exchange distributions.³⁶ Generally, for a single charge-exchange interaction, the product neutral is emitted in the forward direction because the charge-exchange cross section is large for particle pairs with small relative velocities.³⁴ However, those forward-peaked neutrals directed into the bulk of a dense plasma have a strong chance of undergoing a further interaction--either charge-exchange or ionization. In addition, the incident neutral beam will be attenuated exponentially in the plasma; thus, the primary

Figure D-3. Plot of an 80-keV beam on D-T plasma.

charge-exchange production rate decreases along the chord length of the incident beam through the plasma. Therefore, the polar charge-exchange wall distribution is generally forward-peaked for a high energy neutral beam, a low plasma line density, or both. Conversely, for lower energy beam/higher plasma line density combinations, the wall distribution tends toward a maximum in the backward direction. As the beam trapping fraction approaches 100%, strong backward peaking would be expected. This would then account for the backward peaking of the TDF charge-exchange wall distributions compared with the forward peaking in the MARS reactor. The MARS plasma density is only $1 \times 10^{14} \text{ cm}^{-3}$, compared with $6.5 \times 10^{14} \text{ cm}^{-3}$ for TDF. In addition, the MARS neutral beams are 97 keV (negative ion) with no half- or third-energy components,³⁶ whereas the TDF beams are 80 keV (positive ions) with appreciable half- and third-energy components (29% and 16% respectively). Therefore, the bulk of the TDF charge-exchange neutrals are produced on the beam side of the plasma, and forward-directed products are strongly reabsorbed.

A significant feature of the axial plots of maximum, minimum, and average charge-exchange power densities is the tendency of the distributions toward peaks. Again, in comparison with the MARS reactor, TDF has considerably less total charge-exchange power loss to the walls (4.077 MW from each end compared with 19.28 MW for MARS). However, in MARS this power is distributed over a greater axial extent in the z-direction, resulting in lower peak values of the maximum wall flux ($\sim 705 \text{ W/cm}^{-2}$ compared with 2380 W/cm^{-2} for TDF).

Again, this can be explained by the different beam/plasma conditions already discussed and by noting that the temperatures perpendicular and parallel to the magnetic field ($T_{\perp}$ and $T_{\parallel}$) in MARS are 58 and 59.5 keV, respectively, compared with 34 and 4 keV in TDF. Clearly, there is a high probability of producing a charge-exchange neutral in TDF with a direction closely normal to the z-axis. These factors all contribute to the sharpness and the very high peak values of the TDF axial distributions. Further details on charge-exchange wall fluxes in the MARS reactor can be found in Ref. 34.

The combination of these high-peak charge-exchange power densities and predominantly backward-directed emissions is rather unfortunate for TDF. At least in the case of forward-directed distributions, maximum charge-exchange flux densities go toward the beam dump surfaces. Clearly, heat loads of nearly 2400 W/cm^{-2} are too large for even near-term high-heat flux technologies. Beam dumps based on the "hypervaporation" principle are

currently being incorporated on the JET tokamak for heat loads of $\sim 15 \text{ kW/cm}^2$ under quasi steady-state ($\sim 20 \text{ s}$) conditions.³⁷ However, such dumps have rather bulky configurations and could not be integrated in the first wall area of TDF, where space is at a premium. In practice, heat loads to water-cooled first wall surfaces should be limited to no more than 500 W/cm^2 . In the STARFIRE tokamak reactor design for example, maximum surface heat loads to the water-cooled limiter were restricted to $\sim 240 \text{ W/cm}^2$.³⁸

An alternative to reconfiguring beam parameters or geometries to reduce the peak heat loads to acceptable levels is to simply increase the radius of the first wall from its original value of 25 cm. This would be necessary only in the vicinity of the high charge-exchange power densities and therefore would not be detrimental to neutron wall loading levels in the bulk of the central cell.

Accordingly, we re-ran the beam/plasma parameter set with two new values for the first wall radius: 50 and 75 cm. Figure D-4 is a plot of the resulting maximum charge-exchange heat loads at each axial position as a function of the z' coordinate. For comparison we show the axial distribution at the original wall radius of 25 cm. Note that the peak values have been reduced considerably from $\sim 2400 \text{ W/cm}^2$. In fact, these reductions, caused by the distributed nature of the charge-exchange emission, are greater than the $1/r$ scaling expected from a line source.

One other interesting feature of Fig. D-4 is that the wall fluxes at the wings of the distributions are actually greater for the larger wall radii. This can be explained by considering charge-exchange neutrals produced by neutral beam interactions in a given xy plane and emitted at large angles relative to this plane. If these neutrals are to contribute to the charge-exchange flux at a given z' coordinate in the wings of the distribution, they must travel through a larger plasma chord length to bombard the wall at the smaller radii than when bombarding the larger wall radii at the same z' coordinate. Thus, for coordinates sufficiently removed from $z' \approx 0$, lower attenuation within the plasma compensates for the apparent loss in intensity caused by the increased wall radius. In the wings it is obviously preferable to keep the wall radius relatively small.

Figure D-4. D-T beams on D-T plasma. The maximum charge-exchange wall fluxes with Γ_w as a parameter. The beam injection point is here defined as $Z = 0.0$.

Conclusions

In conclusion, Monte Carlo calculations have shown that

- Peak charge-exchange power densities from the TDF central cell neutral beams are about 2400 W/cm^{-2} for a first wall radius of 25 cm.
- The heat loads have a predominantly backward-peaked distribution.
- The total charge-exchange power loss from the plasma is 4.077 MW at each end of the device.
- One effective method of reducing the peak power densities to the first wall is to increase the wall radius from 25 cm to at least 75 cm in the vicinity of $z' = 0$ and taper it back to 25 cm at axial coordinates where heat loads have fallen to acceptable levels.
- Such a revised design would keep maximum charge-exchange heat loads to less than 300 W/cm^{-2} and could be accommodated by fairly conventional water-cooled first wall designs.

APPENDIX E, PELLET FUELING CALCULATIONS

The requirements for warm ion flow stabilization are given in Table E-1. To satisfy them, we must inject 800 A of deuterium deep enough that warm ions exist over more than half of the plasma volume. Deuterium gas injectors can provide warm ions near the plasma's surface only. Frozen pellet injectors, such as those currently under development, have the potential of meeting the plasma penetration requirement of $>0.35 a$, where a is the plasma radius.

Large pellets will penetrate deeper than small pellets, but, as the table indicates, the pellet size is limited to 10% of the ion population in the central cell. The frozen deuterium pellet can therefore be only about 0.6 mm. We can also increase depth of penetration by injecting into the neck of the plasma near the throat of the central cell, where the plasma radius is only 5.5 cm. However, coil restraints limit the injection point to the thick part of the neck, where the plasma radius is about 8 cm. Since these two avenues posed problems, we explored the effect of increased pellet velocity, performing the analysis described below.

We used a plasma physics transport code (Ref. 21) to investigate the ablation of frozen pellets of several sizes injected into the plasma at several velocities. Figures E-1 and E-2 are typical results that show the effect of pellet ablation on the plasma density for an acceptable and unacceptable size pellet. The densities were taken immediately after the complete pellet ablation. The perturbation disappears before the next pellet arrives a few ms later.

Figure E-3 shows some normalized parameters as functions of the plasma depth. At the plasma surface where $r = a$, the parameters have a zero reference. In the center where $r = 0$, they are maximum. The plasma has a cubic density distribution that determines the cumulative plasma mass distribution from the outside of the plasma to the center. Note that half of the volume lies between the outside of the plasma and $r = 0.7 a$, i.e., the pellets must penetrate 30% of the plasma radius to directly influence 50% of the plasma volume. It also shows that the pellets need to penetrate 43% of the plasma radius to directly influence half of the plasma mass.

Figure E-4 shows the needed pellet velocity in relation to the cumulative plasma volume and plasma mass of Fig. E-3. The three velocity curves correspond to the expected variation in pellet mass ($\pm 10\%$). It is evident

that the lightest pellet with a velocity of 1500 m/s will directly supply 60% of the plasma volume and 43% of the plasma mass. This more than meets the minimum requirements. It is also apparent that a pellet velocity of 2000 m/s will directly supply more than 66% of the plasma volume.

Pellet injectors currently under development reach about 1200 m/s and have the potential of achieving injection velocities of 2000 m/s.

Table E-1. Pellet injector requirements for the central cell.

Description	Value
Continuous operation fusion power(MW)	20
Hot ion confinement time, τ (ms)	14
Plasma length (m)	~ 8
Plasma radius, r_p (cm)	
$Z = 0$ (at midfield of central cell)	10
$Z = +2.0$ m	10
$Z = +3.6$ m	8.5
$Z = +4.0$ m	5.5
Peak ion density, n_0 (cm ⁻³)*	6.5×10^{14}
Flat temperature distribution	
Mean hot ion energy, W_{ih} (keV)	37
Electron temperature, T_{ec} (keV)	2.1
Helium generation rate (s ⁻¹)	7×10^{18} with 1.14 A at 0.171 g/h
Fractional burnup (T_2) injectors	7×10^{-4} of T_2 to neutral-beam
Tritium fueling rate to neutral- beam injectors (s ⁻¹)	1.01×10^{22} with 1620 A at 182 g/h
Deuterium fueling rate to neutral beam injectors (s ⁻¹)	1.01×10^{22} with 1620 A at 120 g/h
Deuterium fueling rate to pellet injectors (s ⁻¹)	0.5×10^{22} with 800 A at 58 g/h
Fraction of plasma ions allowed in a pellet (%)	$\leq 10\%$
Pellet penetration of the plasma radius (cm)	$\geq 0.35a$

*Density distribution $n(r) = n_0[1-(r/a)^3]$, where $a = r_p$.

Figure E-1. Pellet deposition in the TDF plasma where the plasma radius is 8 cm and the pellet diameter is 0.57 mm.

Figure E-2. Pellet deposition in the TDF plasma where the plasma radius is 8 cm and the pellet diameter is 0.72 mm.

Figure E-3. The TDF normalized plasma volume and mass parameters as a function of $q(1 - r/a)$.

Figure E-4. The pellet velocity and plasma penetration, superimposed on normalized plasma parameters.

APPENDIX F, VACUUM AND HALO REQUIREMENTS

A plasma halo fills the region between the main central cell plasma and the first wall. It shields the main plasma from impurities and cold gas that arise from wall outgassing, beam dump sputtering, and other such processes.

The halo model we used consists of two zones. The first extends from the main plasma at radius r_c to the last magnetic field line r_h to clear the high-field choke coils at the ends of the central cell. The second extends from r_h to the first-wall radius r_w . Within each zone we assumed constant, equal density and temperature for both electrons and ions. Essentially all shielding of the main plasma occurs in zone 1, so shielding criteria set the zone 1 parameters. Zone 2 must have a low density and temperature to keep sputtering at the end wall to a minimum.

Here we will discuss the required attenuation of cold gas and sputtered beam-dump neutrals, particle and power balance, and TDF halo parameters. Units are CGS, with energies in electron volts and powers in watts unless otherwise stated.

Beam Dump Sputtering

Table F-1 contains our neutral-beam and beam-dump parameters. The sputtering yield is based on work by Bohdansky.^{39,40} For normal incidence (0 deg), the sputtering field of molybdenum (Mo) atoms per incident deuterium ion peaks at about 10^{-2} for a deuterium energy of about 2 keV; it drops to 10^{-3} for a deuterium energy of about 60 keV, as shown in Fig. F-1.³⁹

Only Mo is considered here; other possible beam-dump materials for TDF give similar yields. Extrapolating from the H and D data, T will give sputtering yields perhaps a factor of 2 above those of D. Accounting for the half- and third-beam energy components, the sputtering yield for normal incidence is approximately $Y_0 = Y_D + Y_T = 3/2(0.8 \times 10^{-3} + 0.15 \times 2 \times 10^{-3} + 0.05 \times 3 \times 10^{-3}) = 1.9 \times 10^{-3}$ atoms/incident particle.

The dependence of the sputtering yield on the incident beam angle is³⁹

$$Y(\phi) = Y(0) \cos^{-f} \phi, \quad (F-1)$$

Table F-1. Neutral-beam parameters for the central cell.

Parameter	Value
<u>Neutral beams</u>	
Total beam current (A)	1080
Total power (MW)	64.8
Average beam energy, W (keV)	60
Beam energy mix, for ion sources 80:15:5	
mix of $D^+ : D_2^+ : D_3^+$ (keV)	
55%	80
29%	40
16%	27
Beam angle (deg)	65
Trapping fraction, f_t	0.79
<u>Beam-dumps</u>	
Surface material	Molybdenum
Angle of dump to beam-off normal (deg)	69
Surface binding energy to deuterium (eV)	86

Figure F-1. Sputtering yield vs ion energy for molybdenum (Mo). The incident particle species are called out.

where f ranges from 1 to 2. For very low- or high-energy beam particles f is 1, and 80 keV is on the borderline of the high-energy regime, so $f = 1$ is used here. The dependence of sputtering yield on energy is peaked at about $E = 2E_b/3$, where E_b is the surface binding energy and is proportional to E^{-2} .³⁹ For very high-energy incident particles the sputtered atoms have a $\cos \theta \cdot \cos \zeta$ distribution, where θ and ζ are 0° for normal incidence.

Combining these dependencies, the approximate sputtering yield is

$$Y(E, \theta, \zeta, \phi) = Y_0 \left(\frac{\cos \theta \cos \zeta}{\cos \phi} \right) \left(\frac{2E_b}{3E} \right)^2. \quad (F-2)$$

A very rough sketch of beam dump geometry is shown in Fig. F-2. Assuming that the neutral beam is strongly peaked at its center, the sputtered atom must have $\theta > 42$ deg and $\zeta < 11$ deg to reach the halo. The probability of the sputtered atom penetrating the halo is $e^{-\lambda}$, where λ is the ratio of path length to mean free path. This ratio is given by

$$\lambda = 7.15 \times 10^{-7} \left(\frac{n \ell \langle \sigma v \rangle_{Mo} \mu_{Mo}}{E^{1/2}} \right)^{1/2}, \quad (F-3)$$

where n is the density of hot ions in the main plasma, ℓ is the path length, and μ_{Mo} is the ratio of Mo mass to proton mass. Although ℓ actually depends on θ , it is taken as a constant here. The ionization rate is $\langle \sigma v \rangle_{Mo}$, given by⁴¹

$$\langle \sigma v \rangle_{Mo} = \left[\frac{10^{-5} (T_e/I_{Mo})^{1/2}}{I_{Mo}^{3/2} (6 + T_e/I_{Mo})} \right] \exp(-I_{Mo}/T_e), \quad (F-4)$$

where T_e is the halo electron temperature, I_{Mo} , which equals 7.3 eV, is the ionization potential for Mo, and the reaction rate has been averaged over Maxwellian electrons.

CENTRAL CELL PLASMA

Figure F-2. Halo model geometry and particle balance terms.

The number of sputtered Mo atoms per second that reach the main plasma is thus given by

$$\dot{N}_{Mo}^S = \frac{\dot{N}_{bd} \gamma_0 E_b}{\cos \phi 4} \int_0^{11^\circ} d\zeta \int_{42^\circ}^{90^\circ} d\theta \int_{E_b}^{E_{inj}} dE \frac{\cos \theta \cos \zeta}{E^2} \exp(-\lambda) \approx \quad (F-5)$$

$$\frac{2.8 \times 10^8 \dot{N}_{bd}}{(n\lambda \langle \sigma v \rangle_{Mo})^2} \left(1 + \frac{7.0 \times 10^{-6} n\lambda \langle \sigma v \rangle_{Mo}}{E_{inj}^{1/2}} \right) \exp \left(- \frac{7.0 \times 10^{-6} n\lambda \langle \sigma v \rangle_{Mo}}{E_{inj}^{1/2}} \right),$$

where $\dot{N}_{bd}$ is the particle source to the beam dump, the normalization factor is $9E_b/16$, and $\phi \approx 69$ deg.

To find the equilibrium density of Mo in the main plasma, we solve an Mo particle balance equation. Since Z for Mo is high, Pastukhov confinement^{42,43} is very good--on the order of $(n\tau)_{Mo}^{PAS} \sim 10^{28} \text{ cm}^{-3} \text{ s}$. The minimum confinement time is, therefore, set by classical radial transport, for which

$$D_{\perp}^{Mo} \sim v_{\perp}^2 \rho^2 \sim \frac{2.8 \times 10^{-6} \mu_{DT}^{1/2} Z_{Mo}^2 n_c}{\mu_{Mo} E_h^{1/2} T_{Mo}} \left(\frac{102 \mu_{Mo}^{1/2}}{Z_{Mo} B_c} T_{Mo}^{1/2} \right)^2 \sim$$

$$2.3 \times 10^{-11} \frac{n_c}{E_h^{1/2}} \sim 71 \text{ cm}^2/\text{s} \quad . \quad (F-6)$$

The radial loss time is approximately

$$\tau_{Mo} \sim \frac{r_c^2}{2D_{\perp}^{Mo}} \sim 0.70 \text{ s} \quad , \quad (F-7)$$

and the radial loss rate is

$$\dot{N}_{Mo}^r \sim f_{rpl} \frac{n_{Mo}}{T_{Mo}} v_c \sim 2.2 \times 10^5 n_{Mo} \text{ s}^{-1}, \quad (\text{F-8})$$

where f_{rpl} , which equals 0.6, is a radial profile factor.

A density of impurities in the main plasma n_{Mo} will reduce the fusion neutron production to a fraction of its $n_{Mo} = 0$ value:

$$\left(\frac{n_c - Z_{Mo} n_{Mo}}{n_c} \right)^2 = (1 - 7 \times 10^{-14} n_{Mo})^2. \quad (\text{F-9})$$

Using particle balance, $\dot{N}_{Mo}^r = \dot{N}_{Mo}^s$, which implies $n_{Mo} = \dot{N}_{Mo}^s / 2.2 \times 10^5$, the fusion power reduction factor is: $1 - (1 - 3.2 \times 10^{-19} \dot{N}_{Mo}^s)^2$ if all the sputtered Mo reaching the plasma edge penetrates and is distributed evenly in the main plasma.

Neutral gas. Neutral gas attenuation is modelled by proportionality constants taken from Fig. F-3 for $T_e > 10$ eV and scaled by $\langle \sigma v \rangle_e$ for

$T_e < 10$ eV, where $\langle \sigma v \rangle_e$ is the electron impact ionization rate.

For example, above 40 eV (and assuming that the neutral density at the boundary between zones 1 and 2 is n_w) the neutral density a distance ℓ into zone 1 is given by $n = n_w / (5.5 \times 10^{-2} n_1 \ell)$, where n_1 is the constant density in zone 1.

Particle Balance

Radial transport. We assume classical radial transport for lack of a detailed transport model. Only hot central cell ions are considered; the warm ions are neglected. The central cell ion radial transport coefficient is

$$D_r \sim v_{\perp} \rho_i^2 \approx \frac{0.105 n_{hc}}{E_{hc}^{1/2} B_c^2} \sin^2 \theta_{inb}, \quad (\text{F-10})$$

where n_{hc} is the central cell hot ion density, E_{hc} is the hot ion energy, B_c is the central cell magnetic field, and θ_{inb} is the neutral beam injection angle.

Figure F-3. Attenuation of neutral gas vs the halo line density.

The radial loss time is approximately

$$\tau_r \sim \frac{r_c^2}{2D_r} \quad , \quad (F-11)$$

so the radial loss rate from the central cell is

$$\dot{N}_{ro} \sim f_{rpl} \frac{n_c}{\tau_r} V_c \quad . \quad (F-12)$$

For the TDF reference case, this is, $N_{ro} \sim 2.4 \times 10^{20} \text{ s}^{-1}$. Similarly, the loss rate from zone 1 to zone 2 is given by

$$D_r \sim \frac{0.053 n_1}{T_1^{1/2} B_c^2} \quad , \quad (F-13)$$

where n_1 and T_1 are the density and temperature in zone 1. The factor-of-two difference between the coefficients of Eqs. (F-10) and (F-13) is because of isotropic thermal distribution in zone 1.

Neutral beam ionization. The neutral beam trapping fraction for a region with density n , total path length λ , ionization rate $\langle \sigma v \rangle_{inb}$, and charge-exchange rate $\langle \sigma v \rangle_{cxb}$ is

$$f_{nb} \sim 1 - e^{-\lambda_{nb}} \quad , \quad (F-14)$$

where

$$\lambda_{nb} = 7.15 \times 10^{-7} \left[\frac{n \lambda (\langle \sigma v \rangle_{inb} + \langle \sigma v \rangle_{cxb})}{E_{inb}^{1/2}} \frac{m_i}{m_h} \right]^{1/2} \quad (F-15)$$

is the ratio of path length to the mean free path,

E_{inb} is the neutral beam energy,

m_i is the ion mass,

m_h is the proton mass,

λ is the path length on one side.

For incident current I_{nb} on an annular zone with trapping fraction f_{tr} in the central circle, the total trapping rate is

$$N_{nb} \approx \{f_{nb} + f_{nb}[1 - f_{nb} - f_{tr}(1 - f_{nb})]\} I_{nb} . \quad (F-16)$$

The fraction of this current, which ionizes and fuels the halo zone, is

$$\dot{N}_{nbi} = \frac{\langle \sigma v \rangle_{inb}}{\langle \sigma v \rangle_{inb} + \langle \sigma v \rangle_{cnb}} \dot{N}_{nb} . \quad (F-17)$$

Ionization of background gas. As mentioned previously, the neutral gas density a distance ℓ into a halo zone from the outer perimeter is given by

$$n_g = \frac{n_w}{c_A n \ell} , \quad (F-18)$$

where n_w is the density at the border, c_A is a constant taken from Fig. F-3, and n is the halo density.⁴⁴

The rate for ionization of gas is

$$\dot{N}_{ig} = dV n_g n \langle \sigma v \rangle_{ig} , \quad (F-19)$$

where $\langle \sigma v \rangle_{ig}$ is the ionization rate and the integral is over the halo volume. Equation (F-18) is only a good approximation until $c_A n \ell \approx 1$, so the integral is broken into two parts. For zone 1,

$$\begin{aligned} \dot{N}_{ig} 2\pi L_c \int_{r_c}^{r_h - \Delta r} \left[r dr \frac{n_w n \langle \sigma v \rangle_{ig}}{c_A n (r_h - r)} \right] + 2\pi L_c \int_{r_h - \Delta r}^{r_h} r dr n_w n \langle \sigma v \rangle_{ig} = \frac{2\pi L_c}{c_A} n_w \langle \sigma v \rangle_{ig} \times \\ [r_g \ell n \left(\frac{r_h - r_c}{\Delta r} \right) - (r_h - r_c - \Delta r)] + \pi L_c n_w n \langle \sigma v \rangle_{ig} [2r_h \Delta r - (\Delta r)^2] , \end{aligned} \quad (F-20)$$

where $\Delta r = 1/(c_A n)$. For zone 2, a similar formula applies. Of course, when Δr is greater than the radial extent of the zone, $n_g = n_w$ throughout the zone.

Reionization of charge-exchanged neutrals. Some fraction f_{ion} of neutrals from charge-exchange events in the halo will reionize. To find the fraction we used the very rough model that follows. Our two major assumptions were: (1) slab geometry suffices and (2) the main plasma is ignored because of its smaller volume.

The mean free path for reionization of a charge exchanged neutral is

$$\lambda_{mfp} = \left[\frac{7.15 \times 10^{-7} n_{<ov>_i (m_i/m_H)^{1/2}}}{E^{1/2}} \right]^{-1}, \quad (F-21)$$

where $<ov>_i$ is the ionization rate and E is the neutral's energy. The very simple assumption used here is that the neutral escapes if its path length to escape the halo ℓ is longer than λ_{mfp} . Focusing on zone 1 and including the main plasma as part of zone 1, $\ell = r_h - r_{cx}$ is greater than λ_{mfp} if a vector with direction $\vec{V}/V$ and magnitude λ_{mfp} lies on a zone of height h on a sphere of radius λ_{mfp} . The geometry is shown in Fig. F-4a. The ionization probability then, is, the area of this zone or zones divided by the total surface area of the sphere. The three possible cases are shown in Fig. F-4b. For case 1, $\lambda_{mfp} < r_h - r_{cx}$, and $f_{ion} = 1$. For case 2, $r_h - r_{cx} < \lambda_{mfp} < r_h + r_{cx}$, and $f_{ion} = 0.5 + (r_h - r_{cx})/2\lambda_{mfp}$. For case 3, $r_h + r_{cx} < \lambda_{mfp}$, and $f_{ion} = r_h/\lambda_{mfp}$.

We can find charge-exchange on the neutral beam N_{nbcx} and on the background gas N_{cxg} by replacing $<ov>_{inb}$ by $<ov>_{cxnb}$ in the numerator of Eq. (F-17) and $<ov>_{ig}$ by $<ov>_{cxg}$ in Eq. (F-20). The source of halo ions caused by reionization of charge-exchanged neutrals is then

$$\dot{N}_{icx} = f_{ion} (\dot{N}_{nbcx} + \dot{N}_{cxg}) . \quad (F-22)$$

End loss. There are two end loss regimes, delineated by the ratio of ion-ion scattering time $\tau_{\perp}$ to the bounce time $\tau_{||}$. For $\tau_{\perp} < \tau_{||}$, the halo is in the collisional regime, and the flow time to the ends dominates end loss, resulting in

$$(n\tau)_{e\ell} = (n\tau)_{flow} \approx \frac{R_c L_c/2}{\sqrt{2\pi T/m_i}} , \quad (F-23)$$

where R_c is an appropriate mirror ratio and the π comes from taking a directed Maxwellian velocity distribution. For $\tau < \tau_c$, the halo is in the collisionless regime and

$$(n\tau)_{el} = (n\tau)_{scat} \approx n\tau_{ii} \ln R_c, \quad (F-24)$$

where the collisional scattering time is $\tau_{ii} \approx 4.9 \times 10^5 (\tau^{3/2})/n$. To choose the regime the computer code compares $(n\tau)_{flow}$ to $(n\tau)_{scat}$, rather than τ_{ii} to τ_c directly. This is for simplicity.

End loss is given by

$$\dot{N}_{el} = \frac{n^2}{(n\tau)_{el}} V_h. \quad (F-25)$$

Particle balance equation. Overall particle balance requires

$$\dot{N}_{ro} + \dot{N}_{nbi} + \dot{N}_{ig} + \dot{N}_{icx} = \dot{N}_r + \dot{N}_{el}, \quad (F-26)$$

where $\dot{N}_{ro}$ is the gain and $\dot{N}_r$ is the loss due to radial transport.

Power Balance

Radial transport. For zone 1, the power gain due to radial transport is

$$P_{ro} \approx \dot{N}_{ro} E_{hc} \approx 1.4 \text{ MW}. \quad (F-27)$$

The loss is

$$P_r \approx \dot{N}_{r1} T_1. \quad (F-28)$$

For zone 2, the gain is

$$P_{ro} \approx \dot{N}_{r1} T_1, \quad (F-29)$$

Figure F-4. Our rough model for the reionization of charge-exchanged neutrals, showing (a) the model and (b) three possible special cases.

and the loss is

$$P_r \approx \dot{N}_{r2} T_2 \quad . \quad (F-30)$$

Alpha particle heating. Drag by halo electrons on alpha particles whose orbits intersect the halo will cause some heating to occur. The calculation for this is similar to that for reionization of charge-exchanged neutrals.

If an alpha particle is born at radius r in the main plasma and with angle θ with respect to the z axis, two cases exist: if $0 \leq r \leq 2\rho_\alpha - r_c$, where 2ρ is the maximum alpha particle Larmor radius, then

$$f_\alpha(r) = \frac{r_c}{2\rho_\alpha} \quad . \quad (F-31)$$

If $(2\rho_\alpha - r_c) \leq r \leq r_c$, then

$$f_\alpha(r) = \frac{1}{2} \left(1 - \frac{r_c - r}{2\rho_\alpha} \right) \quad . \quad (F-32)$$

Since the electron temperature in the halo is low, we assume that any alpha particle whose orbit intersects the halo transfers all of its energy to the halo. This is not true if the halo density gets very low, since the slowing down time is proportional to $T_e^{3/2}/n$.

For the radial density profile

$$n(r) = n_0 \left[1 - \left(\frac{r}{r_c} \right)^3 \right] \quad , \quad (F-33)$$

and the fraction of the alpha particle energy heating the halo is

$$f_{\text{oh}} \sim \int_0^{r_c} \left[1 - \left(\frac{r}{r_c} \right)^3 \right] f_\alpha(r) \frac{r dr}{r_c^2} \sim 0.13 \quad . \quad (F-34)$$

The total power to the halo is thus

$$P_\alpha \approx 0.2 P_{\text{fus}} f_{\text{oh}} \approx 0.5 \text{ MW} \quad (F-35)$$

for the reference case.

Line radiation. Line radiation is neglected here.

Neutral beams. A halo zone causes a power drain of $\dot{N}_{nb} E_{inb}$ on the neutral beam. However, if the charge-exchange time on background gas τ_{cx} is less than the drag time τ_{dr} , this power will not significantly heat the halo zone. The pertinent times are

$$\tau_{cx} \sim \frac{1}{n_n \langle \sigma v \rangle_{cx}} \quad (F-36)$$

and

$$\tau_{dr} \sim \frac{(m/m_{iH}) T^{3/2}}{3.2 \times 10^{-8} n} \quad (F-37)$$

For $\tau_{cx} \ll \tau_{dr}$, which is the regime of both zones, charge-exchange on the background gas causes an energy drain of $1.5 \dot{N}_{nb} T$. The initial charge exchange events on the neutral beams also cause a small power drain, given by $1.5 \dot{N}_{nbcx} T$. Also, the power gain caused by beam particles slowing down before they charge-exchange is approximated here by $\dot{N}_{nb} E_{inb} \tau_{cx}/\tau_{dr}$. The total power change due to the neutral beams is thus

$$P_{nb} = \dot{N}_{nb} E_{inb} \frac{\tau_{cx}}{\tau_{dr}} - (\dot{N}_{nb} + \dot{N}_{nbcx}) \frac{3}{2} T \quad (F-38)$$

Charge-exchange on background gas. For a background gas temperature T_g the power loss caused by charge-exchange is $P_{cxg} = 3/2 \dot{N}_{cxg} (T - T_g)$. We assume that the background consists mainly of Franck-Condon neutrals, with $T_g \sim 3$ eV.

Reionization of charge-exchanged neutrals. The reionization of charge-exchanged neutrals adds a power of $P_{icx} = 3/2 \dot{N}_{icx} T$. We assume that all reionization occurs in zone 1.

End loss. End loss causes a power drain of $P_{el} = 3/2 \dot{N}_{el} T$.

Power Balance Equation. Overall power balance is governed by $P_{r0} + P_{\alpha} + P_{icx} = P_r + P_{nb} + P_{cxg} + P_{el}$.

Halo Parameters for the Reference Case

We chose the reference case because it shielded the main plasma from sputtered beam dump atoms and cold gas while keeping neutral beam attenuation small. All of the shielding occurs in zone 1, whose parameters are given in Tables F-2 and F-3. Zone 2 has a very low density and temperature. We will discuss it only briefly and then concentrate on zone 1.

The primary power inputs into zone 2 are radial transport from zone 1 and the 3-eV ion from the ionization of Franck-Condon neutrals. Since only a small mirror ratio keeps these ions from hitting the wall at the ends of the central cell, the confinement time for an ion is very small--about 5×10^{-4} s for a 3-eV ion. The electrons are cold when they enter the plasma, and their thermal equilibration time is much longer than the ion loss time, so they remain cold at about 0.025 eV. At this temperature, the ionization cross section is virtually zero, which implies that very few events occur, and thus the halo density in zone 2 is negligible.

The ions entering zone 2 by radial transport are also quickly lost before they lose significant energy or transport very far radially. Thus, they form a tenuous boundary layer plasma at 189 eV between zones 1 and 2. Although the question is too complex to address here, the details of the radial transport are important for assessing the sputtering these ions cause at the end walls.

We considered these major factors in arriving at the zone 1 halo parameters:

- (1) Sputtered beam dump atoms should be shielded from the main plasma.
- (2) Neutral gas should be kept from the main plasma.
- (3) Neutral beam attenuation should be as small as possible.

Considerations (1) and (2) are at odds with (3).

The chief parameter controlling the system is the base pressure, which gives the neutral gas density n_w at the first wall. The effect of varying n_w on both density and temperature is shown in Fig. F-5. Since the ionization cross section is almost constant in this temperature range, the neutral beam attenuation has the same functional form as the density. We chose $n_w = 2.9 \times 10^{12} \text{ cm}^{-3}$ for the reference case since, at the minimum n , it was considered tolerable to have fusion power degradation of 1.8% caused by Mo and the power loss of 0.2 MW caused by charge-exchange of the main plasma on cold gas. We could have used a somewhat lower n_w , however, without greatly altering the halo parameters.

Table F-2. General halo parameters for zone 1.

Parameter	Value
Density, n (cm^{-3})	6.2×10^{12}
Temperature, T (eV)	189
Inner radius, r_c (cm)	10
Outer radius, r_h (cm)	17
Surface heat to first wall from halo, where radius $r_w = 25$ cm	
at ~ 60 keV (W/cm^2)	18.3
at 189 eV (W/cm^2)	3.4
at 37 keV (W/cm^2)	1.5
Total (W/cm^2)	23
Particle flux to first wall	
at ~ 60 keV (cm^{-2}/s)	1.9×10^{15}
at 189 eV (cm^{-2}/s)	0.1×10^{17}
at 37 keV (cm^{-2}/s)	2.6×10^{14}
Beam-dump sputtering	
Equilibrium Mo density in main plasma, n_{Mo} (cm^{-3})	1.3×10^{11}
Fusion power degradation (%)	1.8
Neutral density	
at main plasma edge (cm^{-3})	1.1×10^{10}
at wall, n_w (cm^{-3})	2.9×10^{12}
at wall--base pressure (torr)	8.8×10^{-5}
Charge-exchange of neutral beam ions	
Charge exchange time, τ_{cx} (s)	5.0×10^{-4}
Drag time, τ_{dr} (s)	3.3×10^{-2}
Reionization of charge-exchanged neutrals	
Mean free path (cm)	62
Reionization fraction	0.27

Table F-3: Particle and power balance parameters for zone 1.

Parameter	Value
Product of density times lifetime ($\text{cm}^{-3}\cdot\text{s}$)	
for end loss, $(n\tau)_{el}$ ($\text{cm}^{-3}\cdot\text{s}$)	1.3×10^9
for transit flow loss, $(n\tau)_{flow}$ ($\text{cm}^{-3}\cdot\text{s}$)	1.2×10^9
for scattering loss, $(n\tau)_{scatt}$ ($\text{cm}^{-3}\cdot\text{s}$)	1.3×10^9
Particle gain	
Radial transport, $\dot{N}_{ro}$ (s^{-1})	2.4×10^{20}
Neutral-beam ionization, $\dot{N}_{nbi}$ (s^{-1})	1.1×10^{20}
Neutral-gas ionization, $\dot{N}_{ig}$ (s^{-1})	8.63×10^{21}
Reionization of charge-exchanged neutrals, N_{icx} (s^{-1})	5.33×10^{21}
Particle loss	
Radial transport, $\dot{N}_r$ (s^{-1})	2.3×10^{17}
End loss, $\dot{N}_{el}$ (s^{-1})	1.43×10^{22}
Power gain	
Radial transport, P_{ro} (MW)	1.4
Alpha particles, P_α (MW)	0.5
Reionization of charge-exchanged neutrals, P_{icx} (MW)	0.24
Neutral beams, P_{nb} (MW)	0.02
Power loss	
Radial transport, P_r (MW)	2.1×10^{-5}
Neutral-gas charge exchange, P_{cxg} (MW)	0.87
End loss, P_{el} (MW)	1.3
Total neutral beam attenuation in halo (%)	3.5

Figure F-5. Halo density and temperature vs neutral density at the first wall.

The halo's particle balance is dominated by end loss and by ionization and charge-exchange of the cold neutral gas. For power balance, on the other hand, end loss is balanced by radial transport and alpha particle power, neither of which can be very accurately assessed. Figure F-6 shows the effect of increasing radial transport over classical transport. The neutral beam attenuation is f_{nb} from Eq. (F-14); the total attenuation is given in Eq. (F-16). T is essentially linear with the transport ratio. Thus, should some process increase transport over classical, the cost would be in increased neutral beam attenuation. Note that n_w was not optimized here, which would help slightly.

The surface heat load of 23 W/cm^2 is somewhat higher than we anticipated. It has been spread over the whole first wall, since the bounce time for ions resulting from the neutral beams is less than the charge-exchange time.

In conclusion, the halo parameters are in the expected range. To maintain the present neutral beam current to the main plasma requires an increase of about 3.5% in total current, to compensate for halo attenuation. No serious problems have been identified, but if zone 2 begins with a thin boundary layer of 189-eV ions, some sputtering of the end wall may occur.

Figure F-6. Halo density and beam attenuation vs relative transport rate from the central cell.

APPENDIX G, IMPURITY BUILDUP IN TDF

The major sources of impurities in TDF are expected to be 1) Thermal oxygen and carbon desorbed from the wall and heavier metals such as iron sputtered from the walls and 2) Energetic impurities such as oxygen injected with the neutral beams ($\sim 0.1\%$ oxygen now expected for steady-state NBI, unless purified by magnetic analysis). The thermal components from surfaces are predicted to have a very short mean-free-path for ionization and therefore to be highly attenuated by the halo plasma and pumped out to the end chamber walls. The energetic impurities, however, are predicted to penetrate, be trapped and stripped to a high charge state, with a relatively prolonged lifetime due to their high charge state and end plug potential confinement. The brief calculations for the main source of energetic impurities follows.

1. Total impurity input. The total incident neutral beam current for TDF ≈ 1150 A (atomic equivalent from central cell beams, sloshing ion beams, and pump beams). If this includes 0.1% oxygen the total input is ~ 1.15 A oxygen or ~ 0.6 A/end.

2. Trapping and equilibrium charge state. In general the cross section for ionization of impurities is $\geq$ that for deuterium so a value f_t (oxygen) $\geq f_t$ (D-T beams) is predicted. Here we assume a trapping fraction f_t (oxygen) ~ 1 as the worst case. For low q levels, the lifetime for ionization $\tau_{\text{ioniz}} \ll \tau_{\text{scatt}}$ or τ_{cx} for typical plasmas, so the impurity is rapidly stripped to a higher and higher charge state q , until the electron loss rate (ionization $q \rightarrow q + 1$) is equal to the electron pickup rate (charge-exchange $q \rightarrow q - 1$), i.e., $\tau_{\text{ioniz}}(q) \approx \tau_{\text{cx}}(q)$. Hulse et al. in Ref. 45, shows $\bar{q} \sim 6$ for oxygen in a plasma with $T_e \sim 2.1$ keV and a ratio of neutral to electron densities $n_0/n_e \sim 10^{-4}$ as expected with the TDF strong neutral injection and leakage of background super thermal or Frank Condon neutrals through the halo.

3. Impurity lifetime τ . The initial stripping process steps to $q \sim 5$ are so fast as to leave the magnetic moment or total kinetic energy unchanged. The step $q + 5$ to 6 (or 7) is slower so some O^5 loss occurs by scattering out the loss cone, before the oxygen has cooled (from dW/dx in the central cell to the bulk 2.1-keV electrons) such as at $W \leq 20$ keV, since $\phi_c \sim 4$ keV. Rough calculations indicate at least 50% of the impurity ions are lost during

the magnetically confined cooling phase, but here as a worst case we assume 100% trapped as $\bar{q} \sim 6$. Once cooled the O^6 ions are electrostatically confined between the anchor plugs and tend to accumulate in either the transition or anchor barrier regimes. The lifetime for Pastakov electrostatic confinement has the form⁴²:

$$\tau_p = \tau_{qD} q(R) \frac{qe\phi}{T_i} \exp(qe\phi/T_i) ,$$

$$\text{where } \tau_{qD} = \frac{\tau_{DD} (M/2)^{1/2}}{q^2}$$

and $q(R)$ is a function of the mirror ratio R but of an order of unity.

4. Impurity density buildup. The maximum total input current of oxygen in Section I, the minimum warm plasma input of 800 A D^+ from pellet fueling, and the assumption of similar Pastakov confinement for both warm ion groups then allows calculation of the equilibrium ratio of $n(O^{6+})/n(D^+)$, since in equilibrium $n \approx (If_t \tau)/\text{Volume}$. Therefore here the maximum impurity fraction

$$f_o \equiv n(O^{6+})/n(D^+) \approx \frac{1.2 \times 1 \times (16/2)^{1/2}}{800 \times 1 \times 6} \left(\frac{e\phi}{T_i} \right) \exp \left[\frac{e\phi}{T_i} (5) \right] \approx 0.1 \text{ for } e\phi_i/T_i \sim 1.$$

This would be prohibitive for radiation and more importantly scattering losses of D^+ and hence pumping requirements. Since they are proportional⁴⁶ to $(1 + f_o q^2)$ or here $(1 + 0.1 (6)^2) \sim 4.6$. However, more detailed calculations including oxygen scattering losses in the central cell and transition anchor regions (since there is essentially no cooling of oxygen Yushmanov trapped in the transition anchor regions, because the electron temperatures are so high), reduce the net oxygen trapped current by a factor of ~ 10 to only $\sim 0.1A$ with a more precise predicted result of $f \sim 1\%$, which still yields $(1 + f_o q^2) \approx 1.36$. This would still result in a 36% reduction in D^+ confinement $n\tau$ and a 36% increase in pumping current requirements.

Therefore further work is indicated to (1) refine the above calculations, (2) consider use of higher purity neutral beams with impurity content $<< 0.1\%$ (e.g. magnetically separate out heavy impurities from D-T), (3) study ICRH or other methods for impurity pumping for removal, or (4) consider quasi-steady-state operation with remote dumps of ϕ_c to empty out impurities.

REFERENCES--SECTION 2

1. T. K. Fowler and B. G. Logan, Tandem Mirror Technology Demonstration Facility, Lawrence Livermore National Laboratory, Livermore, CA, UCID-19193 (1981).
2. D. Correll et. al., Lawrence Livermore National Laboratory, Livermore, CA, UCRL-50051-78-2 (1978).
3. T. C. Simonen and TMX group, Summary of Results from the Tandem Mirror Experiment (TMX), Lawrence Livermore National Laboratory, Livermore, CA, UCRL-53120 (1981).
4. F. H. Coensgen et. al., TMX Upgrade Major Project Proposal, Lawrence Livermore National Laboratory, Livermore, CA, LLL-Prop-172 (1980).
5. R. J. Breun et. al., "Initial Results from the Tandem Mirror Phaedrus," in Proc. 8th IAEA Conf. Plasma Phys. and Cont. Nucl. Fusion Res. (Nuclear Fusion Supplement, Vienna, 1981) Vol. 1, pp. 105-112.
6. S. Miyoshi et. al., "Plasma Confinement in Central Mirror of Gamma 6 (Tandem Mirror)," in Proc. 8th IAEA Conf. Plasma Phys. and Cont. Nucl. Fusion Res., (Nuclear Fusion Supplement, Vienna, 1981), Vol. 1, pp. 113-118.
7. N. H. Lazer, "Electron and Ion Cyclotron Heating in a Symmetric Tandem Mirror STM-1, Bull. Am. Phys. Soc., 27, 961 (1982).
8. D. Grubb, et. al., "Measurements of Particle and Energy Confinement in Initial TMX-U Sloshing-Ion End Experiments," Bull. Am. Phys. Soc., 27, 997 (1982).
9. Physics Basis for an Axicell Design for the End Plugs of MFTF-B, D. E. Baldwin and B. G. Logan (editors), Lawrence Livermore National Laboratory, Livermore, CA, UCID 19359 (1982).
10. R. E. Klinkowstein et. al., "The TARA Tandem Mirror Experiment," Bull. Am. Phys. Soc., 27, 967 (1982).
11. T. Kawaki, University of Tsukuba, Tsukuba, Japan, private communication (November 1982)..
12. G. I. Dimov, Institute of Nuclear Physics, Novosibirsk, USSR, private communication (November 1982).
13. T. H. Batzer et. al., Conceptual Design of a Mirror Reactor for Fusion Engineering Research Facility (FERF), Lawrence Livermore National Laboratory, Livermore, CA, UCRL-51617 (1979)

14. B. Badger et. al., TASKA, Tandem Spiegelmaschine Karlsruhe, A Tandem Mirror Fusion Engineering Facility: A Preliminary Report, University of Wisconsin, Madison, WI, UWFD-500 (1982).
15. L. D. Pearlstein et. al., "Stabilization of Tandem Mirror Trapped Particle Modes by Incomplete Cancellation of Trapped Particle Drifts," in Sherwood Meeting, Annual Contr. Fusion Theory Conference, 1982 (Santa Fe, NM, 1982).
16. B. G. Logan, Lawrence Livermore National Laboratory, Livermore, CA, private communication (1981).
17. G. A. Carlson et. al., Comparative End-Plug Study for Tandem-Mirror Reactors, Lawrence Livermore National Laboratory, Livermore, CA, UCID 19271 (1981).
18. B. M. Boghosian, R. B. Campbell, and J. M. Gilmore, A Potential Solver for Sloshing-Ion Thermal Barriers, Lawrence Livermore National Laboratory, Livermore, CA, UCID 19215 (1981).
19. M. N. Rosenbluth, H. L. Berk, D. E. Baldwin, H. V. Wong, and T. M. Antonsen, "Fast-Growing Trapped Particle Modes in Tandem Mirrors," in Sherwood Meeting, Annual Contr. Fusion Theory Conference, 1982 (Santa Fe, NM, 1982) paper 32A.
20. B. G. Logan, A. A. Mirin, and M. E. Rensink, "An Analytical Model for Classical ion Confinement in Tandem-Mirror Plugs," Nucl. Fusion, **20**, 1613 (1980).
21. W. L. Houlberg, "Pellet Ablation and Plasma Penetration Studies," Oak Ridge National Laboratory, Oak Ridge, TN, private communication (March 1982).
22. G. P. Grubb et. al., "Energy Confinement Studies in the Tandem Mirror Experiment (TMX): Power Flow," Phys. Fluids, **26** (7), 1987 (1983).
23. R. L. Wong, Lawrence Livermore National Laboratory, Livermore, CA, private communication (1982).
24. L. D. Pearlstein, T. B. Kaiser, and W. A. Newcomb, "Analytic Equilibrium with Quadrupole Symmetry in the Paraxial Limit," Phys. Fluids, **24** (7), 1326 (1981).
25. M. E. Rensink, Lawrence Livermore National Laboratory, Livermore, CA, private communication (1982).

26. L. D. Pearlstein, G. R. Smith, and W. M. Nevins, in Physics Basis for MFTF-B (D. E. Baldwin, B. G. Logan, and T. C. Simonen, eds.), Lawrence Livermore National Laboratory, Livermore, CA, UCID 18496-part 2 (1980).
27. J. E. Osher, "Minimum Streaming Current Requirements for MFTF-B, Revised by Recent R_p/a ; Scaling in 2XIIB, Lawrence Livermore National Laboratory, Livermore, CA, UCID-18060 (1979); also Ref. 3.
28. D. Slaughter, Fusion Reactivities for Several Ion Distributions, Lawrence Livermore National Laboratory, Livermore, CA, UCRL 87416 (1982).
29. K. D. Marx, et. al, "Calculation of $\langle \sigma v \rangle_{DT}$ for Anisotropic Mirror and Toroidal Distributions," Nucl. Fusion Lett., **16**, 702 (1976).
30. A. H. Futch and L. L. LoDestro, Collisional Trapping Rates for Ions in a Magnetic and Potential Well, Lawrence Livermore National Laboratory, Livermore, CA, UCRL-87249 (1982).
31. J. Hovingh and R. W. Moir, "Efficiency of Injection of Neutral Beams in Thermonuclear Reactors," Nucl. Fusion **14**, 629 (1974).
32. G. A. Carlson and G. W. Hamilton, "Wall Bombardment Due to the Charge Exchange of Injected Neutrals with a Fusion Plasma," Lawrence Livermore National Laboratory, Livermore, CA, UCRL-75306 (1974).
33. R. L. Miller, "Monte Carlo Simulation of Neutral Beam Injection for Mirror Fusion Reactors," Ph.D. Thesis (University of Illinois, 1979).
34. L. J. Perkins and T. B. Kaiser, "Monte Carlo Studies of Charge-Exchange First Wall Heat Loads from Neutral-Beam Injection in Tandem Mirror Fusion Reactors," University of Wisconsin, Madison, WI, Fusion Engineering Program report to be published (1983).
35. T. B. Kaiser, MFE Quarterly Report, Lawrence Livermore National Laboratory, Livermore, CA (1978).
36. Mirror Advanced Reactor Study (MARS) - Interim Report, Lawrence Livermore National Laboratory, Livermore, CA, UCRL-53333 (1983).
37. Joint European Torus (JET) Annual Report, JET Joint Undertaking (1981).
38. C. C. Baker et al., "STARFIRE - A Commercial Tokamak Fusion Power Plant Study," Argonne National Laboratory, Argonne, IL, ANL/FPP-80-1 (1980).
39. J. Bohdansky, J. Nucl. Mater. **93, 94**, 44 (1980).
40. J. Roth, J. Bohdansky and W. Ottenberger, Data on Low Energy Light Ion Sputtering, Max-Planck-Institut fur Plasmaphysik, Garching, Germany, Report lpp 9/26 (1979).

41. D. L. Book, NRL Plasma Formulary Naval Research Laboratory, Washington, D. C. (1980).
42. V. P. Pastukhov, Nucl. Fusion **14**, 3 (1974).
43. R. H. Cohen, M. E. Rensink, T. A. Cutter and A. A. Mirin, Nucl. Fusion **18**, 1229 (1978).
44. T. C. Simonen, Lawrence Livermore National Laboratory, Livermore, CA, private communication (6/25/82).
45. R. A. Hulse, D. E. Post, and D. R. Mikkelsen, "Charge-Exchange as a Combination Mechanism in High-Temperature Plasmas," J. Phys. B: Atom. Molec. Phys., **13**, 3895 (1980).
46. P. Poulsen, Lawrence Livermore National Laboratory, Livermore, CA, private communication (1983).

SECTION 3.0. ENGINEERING

3.1. CONFIGURATION OVERVIEW

3.1.1. Introduction and Summary

A major component in the National Mirror Fusion Program Plan, TDF is a small, driven (power input greater than fusion power output) tandem mirror machine, which promises higher neutron flux (1 to 2 MW/m²) and fluences at a lower cost than competing designs. Based on physics results from current machines, construction of TDF could be initiated in FY 1985 and completed in about 5 years. Data from this 59.4-m-long device will then be used in the final design of an economically attractive fusion reactor design.

This machine has been designed to meet two primary objectives:

- Provide nuclear fusion engineering data that demonstrates the engineering feasibility of steady-state operation, furnishes sufficient testing area under meaningful wall loads, and demonstrates the total fuel cycle operation, including tritium breeding and recovery.
- Demonstrate the construction, safe operation, and "maintainability" of a device that incorporates technologies usable in future fusion devices.

Figure 3-1, a cutaway view of the facility, shows the major systems and subsystems of TDF. In this drawing only one component from each system is shown, and for clarity these have been rotated into the plane of the drawing. Using this approach, we can portray the interfacing of the various components. For example, only one beamline is shown in the central cell at the right of the centerline; there are actually four beamlines symmetrically located on each side, and each of these is skewed to the plane of the drawing. Figure 3-2 shows the precise location of these beamlines.

Furthermore, the plasma and halo outlines depicted in Fig. 3-1 are actually oriented at 45 deg to the plane of the drawing. Accordingly, each of the components in the anchor and end cells is also displaced by that angle. The end view in Fig. 3-2 shows the proper orientation of these components.

Fig. 3-1. The TDF configuration: an evolved design that meets the requirements of all system components. For clarity, one component of each system is shown rotated from its true position.

TDF **TECHNOLOGY DEMONSTRATION FACILITY**

Fig. 3-2. Side elevation and end views of the nuclear engineering device.

The TDF configuration has three main sections: the central cell, the anchor cells, and the end cells. Figure 3-3 illustrates these three main areas and indicates the basic dimensions of the device.

The 8-m-long central cell is measured from the centerline of the choke coils. In addition to these two high-field choke coils, the central cell also contains: the vacuum boundary, solenoid coils (S0 and S1), nuclear shielding (S/S), test modules, beamlines, beam dumps, and pellet injectors. The diameter of the central cell--7 m--was established primarily by the geometry of the two solenoid magnets.

The 6-m-long anchor cell is measured from the centerline of the choke coil to the intersection of the end-cell dome. This area includes the sloshing beamline, sloshing beam dump, transition beam dump, nuclear shield of heavy concrete, vacuum vessel, transition coil (T1), and the anchor coil set (M2 and M3). The diameter of the anchor cell, measured to the outside of the vacuum boundary, is approximately 7 m. This dimension allows for a reasonable clearance between the plasma halo and the inner surface of the anchor-cell shielding.

The end cell is approximately 16 m in diameter and 19-1/2 m in length, measuring to the shield plug covering the plasma guns. This outermost area contains the anchor pumping beamline, transition pumping beamline, particle dump, cryopump panels, nuclear shield of ordinary concrete, halo scraper, helium pumps, and plasma guns. Its final size is based on limiting the peak thermal load on the particle dump to 300 W/cm^2 and containing 576 m^2 of cryopanel surface in each cell. We have determined that by meeting the requirements for the peak thermal load we can provide sufficient volume for the cryopanel. We discuss these considerations in more detail in the following section on physical characteristics of the device.

3.1.2. Design Approach and Requirements

The overall layout of the TDF configuration was based on three considerations: modularity of each component, access to each component, and integration of all systems to ensure total compatibility. At the same time these are the requirements that guarantee a configuration with "maintainability."

ORNL DWG 87-3246A4 FED

Fig. 3-3. The three areas of the machine: the central, anchor, and end cells.

Modular Design. Each component of TDF is modular, designed to be replaced individually. The module sizes depend on the particular component and the available access. For example, removal of the shield plug on the end-cell dome provides ready access to the small plasma gun. Hence, plasma-gun replacement involves removing only two small modules.

The sloshing beam dump located on the side of the anchor cell is also a module, conveniently mounted to the shield plug. This design allows us to replace the beam dump by handling only one relatively small component that incorporates the shield plug and the dump panel as well as the coolant lines. This same approach was used to design the larger central cell beam dumps for ease of access. Incorporating the sloshing beam dump into the shield plug was actually a necessity, however, because this dump panel is virtually inaccessible except by disassembling a portion of the central cell.

Not all components can be subdivided into small modules. The cryopanel is arranged so that each end cell has six assemblies, each containing six double-sided panels measuring 2 x 4 m. Therefore, replacing the cryopanel involves handling a module approximately 11 m long, 4 m wide, and 2 m deep.

The central cell neutral-beam system is perhaps the best example of modularity because it embodies both small and large modules. The ion sources are expected to require frequent replacement and are designed to be accessible and replaceable without disturbing the other components in the beamline. Replacement of the larger internal components requires disassembly of larger modules (such as the rear superconducting magnetic shield, a portion of the nuclear shield, and finally, the total internal assembly of the beamline) in one large module, which is then transported to a hot cell.

Access. Through such modularity we have provided for replacement access for each component. In the central cell the beamlines are arranged below the device midplane to permit overhead access for any replacement of the center components. The clustered beamline arrangement allows ready access to the

choke coils, the central cell beam dumps, and the pellet injectors. Fortunately, access to the test modules is not affected by the arrangement of the beamline because their injection angle is 65 deg.

In the anchor cell, as we mentioned, access to the sloshing beam dump is through removal of the shield plug to which it is attached. Access to the sloshing beamline is possible by removal of the rear portion of the nuclear shield. The transition pumping beam, similar in design to the central cell dumps, is accessible through the bottom of the anchor cell.

Numerous access ports are provided in the end cell--six in each upper midplane, one for each cryopanel assembly, and shielded covers for each set of helium pumps. The plasma guns, as well as the anchor pumping beamline, can be reached through a small shield plug. The particle dump is accessible by removing the upper portion of the protruding shield that is mounted on the end-cell dome. Note that this access under the end cell was a configuration requirement; therefore, there is a 2-m clearance to the building floor. This dimension is sufficient to permit comfortable hands-on operations under the end cell; at the same time it can accommodate penetrations into the device and also any equipment that must be installed under the end cell.

Systems Integration. We integrated each of the systems and subsystem components by considering their functional requirements, the frequency of replacement, and their requirements for interfacing with adjacent components. This approach was particularly important in the central cell, where many components are competing for a limited amount of space. For example, the area along the plasma axis contains five components in close proximity: the four beamline drift tube openings and their corresponding beam dumps, the choke coil, the transition pumping beam dump, and the pellet injector. Figure 3-4 locates these components and gives their dimensions.

3.1.3. Selected Physical Characteristics

This discussion concentrates only on those physical characteristics pertinent to an overview. For additional information, refer to the section on the specific component.

Fig. 3-4. Five components integrated into a limited portion of the central cell. The entire machine has been designed to permit easy access through its modular construction.

Overall Device. We based our nuclear bulk shielding design for TDF on the requirement that calls for hands-on maintenance operations, 24 hours after shutdown, provided the shield remains intact. In the central cell, where the deuterium-tritium (D-T) fusions occur, the shield also provides protection to the superconducting coils to counter the effects of neutron heating, insulation damage, and increased copper resistivity. We determined that 115 cm of stainless steel plus 5 cm of lead cladding was sufficient to meet the biological activation limit (2.5 mrem/hr). Of this total, 90 cm of stainless steel placed between the plasma and the magnet bore is adequate to meet the requirements for coil protection.

The anchor cells and end cells also have a biological shield. However, because these areas of the device receive essentially collimated neutrons streaming out of the central cell, the material and thickness requirements are less demanding. The anchor cell requires 60 cm of heavy (borated) concrete, whereas the end cell requires 60 cm of ordinary concrete.

The primary vacuum boundary is externally located around the device, both to simplify the vacuum joint interfaces for penetrations (i.e., those of the test modules and beam dumps) and to take advantage of the hands-on mode of operation for inspection and repair. The exception to this approach is at the neutral beamlines, where the vacuum boundary is located inside the beamline shielding.

We have provided for secondary vacuum containment around the superconducting magnets to avoid the additional downtime needed to thermally cycle these coils when the primary vacuum is lost during either routine changeouts of test modules or replacement of components (such as the choke coils or the beam dumps). Each of the solenoid coils has an individual vacuum vessel concentrically located around the structural case. Figure 3-1 shows this secondary vessel, as well as the transition and anchor coils that share a common vacuum containment. This common containment was required because of the coils' proximity.

Central Cell. The central cell is a compact arrangement of many major systems that comprise a tandem mirror device. The area allocated for engineering testing is of particular importance and is briefly discussed here. The test module region, as shown in Fig. 3-3, is 4.5 m along the plasma axis, and with the test surface located at $R = 0.25$ m, this yields a total available area of 7 m^2 . One-half of this area has ready access from between the solenoid coils; because the remainder is positioned in the plane of these coils, it is less accessible. Access to this latter test area requires removal of the test modules between the coils. Therefore, we feel that any tests that require frequent changeout should be located between the coils and any long-term tests be located in front of the coils. This will minimize downtime.

The use of rabbit tubes into the less accessible test areas of the central cell provides an alternate means of introducing small test specimens without disturbing other test modules. Figure 3-1 shows two such penetrations.

Anchor Cell. The anchor cell consists primarily of the sloshing beamline and its beam dump, the transition pumping beam dump, and the magnet system in the common vacuum vessel. The anchor cell is sized to accommodate two requirements; its length was established by the support requirements for the magnet vessel and its diameter was based on maintaining a 0.5-m clearance to the plasma halo. These sizing relationships, which represent a minimum volume for this cell, can be seen in Fig. 3-1.

One of our design objectives was to develop an overall configuration that minimized the building size for the reactor. This was partially achieved by designing a "foreshortened" anchor cell that does not allow for overhead access and removal of the magnet vessel assembly. Because this component requires removal through the end cell, it reduces the overall length by approximately 5 m.

End Cell. The end cell contains the largest component assembly that may require removal--the cryopanel. As previously mentioned, each assembly of six double-sided panels has an overall size of 11 x 4 x 2 m. The orientation of the panels (Fig. 3-2) is at 45 deg to the building floor and parallel to the plasma sheet. This skewed arrangement results from the orientation of the transition and anchor coils (also at 45 deg) because of the pool-boiling design of these superconducting magnets. Each of the end and anchor cells has an orthogonal symmetry relative to each other.

The center of the particle dump is located at $z = 28$ m and results from limiting the peak thermal load in this area to $<300 \text{ W/cm}^2$. This linear dimension, in conjunction with the diameter required to contain the plasma edge, is sufficient to contain the 36 cryopanel.

3.2. MAGNETS

3.2.1. Overview

We have completed a reference magnet configuration for TDF. It consists of 11 coils, compatible with the design criteria shown in Tables 3-1 and 3-2. Three superconducting solenoids in the central cell provide a 4.5-T field and allow test module clearance. Water-cooled choke coils provide a mirror peak field of 15 T, while allowing neutral-beam access to the central cell. The yin-yang pair flank a single transition coil, providing a 3:1 mirror ratio with a 3-T peak and allowing adequate clearance for neutral beams. The coils are designed to meet the coil engineering criteria of cooling, stability, and structural supports using state-of-the-art technology. The radiation shielding has been examined and is shown to be conservative.

Magnet Field Requirements. The field requirements are summarized in Table 3-1. Additional design criteria that directly affect the configuration are included in Table 3-2. The resulting field profile is shown in Fig. 3-5.

Magnet Configuration. The reference magnet configuration is shown in Fig. 3-6. It includes the coil geometry, cross section, position, and current density, as well as a description of the spacing between the S1 and the M1 coils, which provides 35 cm of neutron shielding for the S1 coil while allowing neutral-beam access to the central cell. The configuration is based on niobium-titanium conductor technology for all superconducting and water cooling for the choke coils.

External Field. Figure 3-7 is a plot of total field contours in the principal view of the machine. The large area covered encompasses both the neutral-beam sources and the end region of the machine. Also shown is the flux tube, which corresponds in size to the 17-cm radius of the plasma halo.

Conductor Fields. The peak fields are shown in Table 3-3.

Table 3-1. TDF magnet field requirements.

Central cell	
Plasma length (m)	8
Plasma radius (cm)	10
Solenoid field (T)	4.5
Peak beta	0.4
Field variation (%)	<5
Mirror region	
Field at mirrors (T)	15
Anchors	
Plasma length (m)	2.87
Field at mirrors (T)	3.0
Field at midplane (T)	1.0
Peak beta	0.5

Table 3-2. Design criteria that affect the magnet configuration.

17-cm plasma halo clearance.
Circular flux lines in central cell and anchor.
Beam access to central cell, transition region, and anchor.
1.25-m test-cell clearance in central cell.

Figure 3-5. Center-line field profile.

The image shows a document with a grid-like structure, possibly a ledger or a form. It contains several sections with headings and handwritten entries. The text is mostly illegible due to the quality of the scan, but some words like "CASE", "REPORT", and "DATE" are visible. The document is divided into several sections by lines and boxes.

Figure 3-6. Reference magnet configuration.

Figure 3-7. TDF external fields.

Table 3-3. Peak fields in each conductor bundle.

Coil	Field (T)
S-0	6.7
S-1	7.6
M-1	--
T-1	4.3
M-2	4.7
M-3	4.7

Stored Energy. The energy stored in the magnetic field of each coil is shown in Table 3-4.

Halo Clearance. The coils were designed to allow the plasma halo clearance through the machine. The halo, which has a 17-cm radius at the center line of the machine, clears all coils. It comes closest to the choke coil cryostat, as shown in Fig. 3-8.

Magnetic Forces. The magnetic forces calculated by the EFFI code are shown in Fig. 3-9. These nominal forces are calculated for the base case but do not represent the peak forces that must be analyzed as part of a detailed fault-case analysis.

3.1.2. Summary Description of Central Cell and Choke Coil Magnet System

This system consists of three superconducting solenoids in the central cell with two resistive choke coils located symmetrically about it.

The layout of the magnet system is shown in Fig. 3-10. The design of the central cell is based on a reasonable extrapolation of existing technology, the magnets can be fabricated by the methods used to produce the solenoid magnets for MFTF-B.

The choke coils represent a design challenge because they produce a higher magnetic field than currently available coils and because their operational life is much longer than that of similar existing high field coils. The radiation environment in which the choke coils operate is also more severe than that for present technology coils. Developmental work is required to increase the operational life of the choke coils and to ensure their integrity in the radiation environment.

Central Cell Solenoids. The central cell superconducting solenoids produce a 4.5-T magnetic field with less than a 4.5% ripple, as required to confine the plasma. They have an inner diameter of 3.75 m and are separated by 1.25-m gaps that allow access to the cell interior for blanket module manipulation or for replacing portions of the shield. The current density in the windings is graded in three regions to achieve a more uniform stress distribution for the

Table 3-4. Energy stored in each coil.

Coil	Energy (MJ)
M-3	12.47
M-2	12.89
T-1	13.73
M-1	8.1
S-1	700.0
S-0	342.0

Figure 3-8. Flux bundle through the choke.

Solenoids

	Axial force (lb)	Solenoidal radial force (lb/m of circumference)
S-0	0	2.1×10^7
S-1	-22.9×10^6	3.2×10^7
M-1	-5.14×10^5	1.00×10^7

Quadrupole coils

	Axial force	Large lobe opening force (lb)
T-1	-5.20×10^4	3.49×10^6
M-2	-1.09×10^5	3.18×10^6
M-3	-3.26×10^5	2.97×10^6

Figure 3-9. Baseline magnet system forces.

Figure 3-10. TDF magnet arrangement.

most cost-effective design. The conductor consists of niobium-titanium superconductor joined to the copper stabilizer. The cryostable conductor is cooled with pool-boiling liquid helium at 4.2 K.

Choke Coils. The choke coils are located 4 m along the axis on either side of the solenoid center line. Each coil produces a field of 12 T, or a total field of 15 T when energized in conjunction with the central cell. We chose a resistive coil for this application because it uses current technology and satisfies magnetic field requirements within the space limitations. Our baseline configuration for this study uses a polyhelix-type coil with externally-cooled conductor; however, as a result of the trade-off studies described in this section, we recommend that an internally cooled conductor be considered for the final design effort.

The deionized water used as a coolant for the coil with the externally cooled conductor passes axially through the coil. The current density is 4800 A/cm^2 . We chose to make the conductor of zirconium copper (AMZIRC), a material of relatively high yield strength and high conductivity--93% International Annealed Copper Standard (IACS). Spinel (Al_2MgO_4), was chosen as the insulator because it exhibits low swelling under radiation, is insensitive to chemical attack, and can be formed into the required shapes by sintering. The total power requirement for one coil is 26 MW.

3.2.3. Central Cell Solenoids

This section presents the proposed conceptual design for the central cell solenoids. Design considerations and requirements, as well as trade-off studies, are presented as introductory topics. Engineering analyses come afterward.

Design Considerations and Requirements. The major design constraints on the magnets are determined on the basis of both plasma physics and space requirements for machine access, neutral-beam injection, and radiation shields. As a result of these constraints, the central magnetic field is 4.5 T. The magnetic field ripple is limited to 4.5%, the inner diameter of the magnet is 3.75 m. The dimensional constraints must also allow sufficient space for support structures and the magnet cryostats.

The magnets must also provide low-cost, reliable, and safe operation. Therefore, it is desirable to base the magnet design on such proven superconductor technology such as copper-stabilized niobium-titanium superconductor cooled at 4.2 K in the pool-boiling mode. Grading of the conductor reduces cost because of the effective use of the superconducting material.

The central cell faces an intense neutron environment (2 MW/m^2 of neutron wall loading) that can adversely affect material properties. For example, the electrical resistivity of an unshielded stabilizer in this environment is increased and the mechanical and dielectric strengths of the insulator are reduced. In addition to these material properties effects, the critical parameters of the superconductor (critical current density, magnetic field, and temperature) are, in time, reduced. The neutron heat loads on the magnet winding also affect the cost of the cryogenic system. Therefore, the central magnets are provided with enough shielding to keep radiation damage and neutron heating within acceptable limits for the full performance of the magnets over their design life of 5.4 full power years (FPY).

The radiation damage that increases the stabilizer resistivity is partially or wholly recoverable by annealing at room temperature. Damage to the insulators and superconductor is cumulative and nonrecoverable. Gamma-ray irradiation affects organic insulators. The radiation resistance of the structural materials is four orders of magnitude higher than that of the stabilizer, insulator, and superconductor. The structural materials will outlast any of these components.

The requirements and considerations were analyzed, and a consistent set of requirements was defined and is summarized in Table 3-5. Requirements for the choke coils are also included in the table, but are not discussed in detail until Section 3.2.4. The most stringent constraints for the magnets include dimensional constraints, magnetic fields, and operational lives.

Engineering Trade-Off Studies. Our engineering trade-off studies were focused primarily on the selection of the coolant. Cooling with HeI in the pool-boiling mode at 4.2 K was chosen for the baseline design because it

Table 3-5. Solenoid magnet requirements. The American Society of Mechanical Engineering (ASME) pressure-vessel design code is used both for the central solenoids and choke.

Parameter	Central solenoids		Choke
Coil	S-0	S-1	M-1
Central field (T)	4.5 (combined with S-1)	4.5 (combined with S-0)	15.0 (combined with S-0 and S-1)
Type of cooling	LHeI	LHeI	Water
Conductor type	Super	Super	Am ₂ zirc
Operational life (FPY)	5.4	5.4	1.0
Clear distance to S-1 (m)	1.25 minimum	N/A	N/A
Inner winding radius (m)	2.0	2.0	0.122
Clear bore radius (m)	N/A	N/A	0.104
Z-axis location (m)	0.0	2.585	4.0

simplifies conductor and coil engineering and uses simpler cryogenic equipment. This option is also considered more reliable because it is based on proven superconductor technology.

Additional trade-off studies show that neutron heat deposition in the winding significantly impacts the capital and operating costs of the cryoplant. Proper selection of materials and shield geometry limits neutron heat deposition in the magnets, reduces neutron and gamma damage to the coil materials, and considerably reduces the total cost of the cryoplant.

Choice of Coolant. Cooling with LHeI in the pool-boiling mode has been compared to cooling with LHeII at 1.8 K. Figure 3-11 shows the coil configurations for both cases.

Cooling with LHeII allows a 30% reduction in magnet cross section because of the superior heat-transfer properties of HeII. As a result, both the weight and cost of the magnet can be reduced. Overall cryostat dimensions do not change appreciably, however, because more space is needed to accommodate an additional radiation shield cooled with HeI.

Table 3-6 compares the characteristics of conductors cooled with LHeI to those of conductors cooled with LHeII.

The LHeII-cooled conductor incorporates stainless steel reinforcement to support the hoop loads at a maximum stress of 40,000 psi. It also allows a smaller winding than the LHeI conductor, but the conductor geometry is more complex and requires special manufacturing techniques and additional quality-assurance procedures.

Table 3-7 compares the costs of HeI and HeII central cell solenoids and associated cryogenic equipment.

While cooling with LHeII does reduce magnet size and cost, several factors weigh against its use at this time. The use of LHeII is not a developed technology, so developmental work would be required. Also, the service stack is more complex for LHeII, and more refrigeration is necessary. Because these factors add to the total cost, we concluded that the use of LHeI is preferred for this machine.

Effect of Neutron Shielding on Cryoplant Cost. Neutron heat deposition in the windings directly affects the cost of the refrigeration equipment because a larger capacity refrigerator is needed to remove the heat.

Figure 3-11. Coil configurations for HeI and HeII cooling schemes.

Table 3-6. Characteristics of LHeI- and LHeII-cooled conductors.

Coolant	LHeI	LHeII
Superconductor	niobium-titanium	niobium-titanium
Stabilizer	Half-hard OFHC copper	Half-hard OFHC
copper		
Reinforcing material	N/A	Stainless steel
304LN		
Allowable stress (psi)	28,000	40,000
Average current density (A/cm^2)	2,100	2,750
Heat flux (W/cm^2)	0.25	1.00

Table 3-7. Cost trade-off study of LHeI vs LHeII for central cell solenoids.

Costs are in millions of dollars.

Coolant	LHeI (Pool-boiling)	LHeII (Superfluid)
Cost of coil, including liquid helium case	9.20	6.85
Cost of liquid helium thermal radiation shield	N/A	0.009
Cost of cryogenic plant		
With LHeI	0.49	0.31
With LHeII	<u>N/A</u>	<u>0.56</u>
Total capital cost	9.69	7.72
Operating cost per lifetime (5.4 FPY at \$0.10/kWh)	<u>0.89</u>	<u>1.54</u>
Total lifecycle cost ^a	10.58	9.26

^aCost estimate does not include stack, LN₂ shield, vacuum vessel, cryogenic plumbing, or power and control system.

Table 3-8 gives the total life cycle cost of the cryoplant per coil as a function of heat input per unit volume in the cold magnets.

As shown, cryoplant cost decreases rapidly with lower neutron heating. Smaller heat input also means less radiation damage to the conductor and insulation. The magnets have a required operational life of 5.4 FPY. This requirement is satisfied by choosing a shield that limits the neutron heating below 0.06 mw/cm^3 , which gives a life cycle operating cost of approximately \$1.8 million per coil.

Selected Design Approach. This section presents the preferred design concept for the TDF central cell solenoids. The essential features of the design are described, and key engineering analyses are discussed.

Design Description.

Coil Configuration. The coils are layer-wound on thick steel bobbins. Layer winding was chosen to keep layer tension constant, since the conductors carry their own hoop forces, and to allow current density grading by varying conductor size. (Section 3.4.2 shows that a single series-wound conductor is feasible.) This approach requires only one power supply for each magnet. The insulation chosen is polyimide fiberglass, and the niobium-titanium superconductor is stabilized with half-hard OFHC copper.

The conductors are cryogenically stabilized to tolerate small mechanical disturbances during coil charging and to ensure full current recovery of a normal zone. The heat-transfer rate from a normal region in each of the three grades has a maximum value of 0.25 W/cm^2 , which is consistent with present technology.

The coil's hoop forces are carried by the copper stabilizer. The axial coil loads have not been completely identified for all operating and fault conditions, but a large nominal axial force does exist on S-1 acting toward S-0. This large force of 23×10^6 lbs requires thick side plates on the inner sides of S-1; these loads are carried by cold intercoil struts to the inner and outer shells of S-0.

The design reflects the basic philosophy of using proven technology to provide an economical and reliable set of coils that meets overall requirements. Figures 3-12 and 3-13 show cross sections, and Table 3-9 shows major characteristics of the S-0 and S-1 solenoids.

Table 3-8. Cryoplant cost as a function of neutron heat input in a typical central cell coil.

Peak neutron heating (mW/cm ³)	3.0	1.4	0.5	0.03	0.006
Neutron heat load (W)	7884.1	3678.1	1312.8	78.7	15.7
Other heat load (W)	217.5	217.5	217.5	217.5	217.5
Total heat load (W)	8101.6	3895.6	1530.3	296.2	233.2
Cryoplant power factor (W_e/W)	668.0	668.0	668.0	668.0	668.0
Cryoplant power requirement (MW)	5.4	2.6	1.0	0.20	0.16
Operating cost per year (\$) at \$0.1/kWh (\$M)	4.74	2.28	0.895	0.173	0.136
Lifetime operating cost, 5.4 years total on time (\$M)	25.6	12.3	4.8	0.93	0.73
Capital cost of cryoplant at 1.75 kW (\$M)	14.2	6.8	2.7	0.52	0.41
Total life-cycle cost (\$M)	39.8	19.1	7.5	1.45	1.14

KEY FEATURES

- PEAK FIELD 6.7 T
- LHe 1 COOLED (POOL BOILING, 4.5K)
- THREE WINDING SECTIONS USING GRADED CONDUCTORS
- 6 cm THICK SIDE INSULATION FOR SPLICES AND LEADS
- CONDUCTORS REACT OWN HOOP FORCES (28 KSI, MAX.)
- APPROXIMATE WEIGHT OF MAGNET COLD MASS IS 104 TONS

VACUUM VESSEL
RADIATION SHIELD
HELIUM VESSEL
INSULATION

ALL DIMENSIONS IN CENTIMETERS

Figure 3-12. Cross section and characteristics of the S-0 solenoid.

KEY FEATURES

- PEAK FIELD 7.6 T
- LHe I COOLED (POOL BOILING, 4.5K)
- THREE WINDING SECTIONS USING GRADED CONDUCTORS
- 6 cm THICK SIDE INSULATION FOR SPLICES AND LEADS
- CONDUCTORS REACT OWN HOOP FORCES (28 KSI, MAX.)
- CASE SIDE EXTRA THICK (12.7 cm) TO REACT LARGE SIDE LOADS (NOMINALLY 5.0 cm)
- APPROXIMATE WEIGHT OF MAGNET COLD MASS IS 163 TONS

ALL DIMENSIONS IN CENTIMETERS

Figure 3-13. Cross section and characteristics of the 5-1 solenoid.

Table 3-9. Major characteristics of the solenoid magnets.

Parameter	Central solenoids		Choke
	S-0	S-1	M-1
Coil	S-0	S-1	M-1
Conductor material	NBTI superconductor with half-hard OFHC copper stabilizer		AMZIRC
Ampere turns (10^6 A)	7.63	14.17	6.91
Conductor current (A)	9,107	7,839	14,106
Current density (A/cm^2)			
Grade I	1,235	1,065	4,800
Grade II	1,588	1,379	--
Grade III	2,111	2,192	--
Peak field at conductor (T)	6.7	7.6	15.0
Operating temperature max.	4.2 K	4.2 K	120 °C
Conductor bundle			
size (m wide x m high)	0.545 x 0.865	0.860 x 0.997	0.48 x 0.30
Weight of winding (tons)	56.5	106.6	2.0

The characteristics of the choke coils are also included; a detailed discussion of choke coils is presented in Section 3.2.4. The S-0 solenoid conductor bundle is 0.545 m wide by 0.865 m high and produces 7.63×10^6 A-turns. The S-1 solenoid bundles are 0.86 m wide by 0.997 m high and each produces 14.17×10^6 A-turns. The winding cross section of S-1 is larger than that of S-0 because more ampere turns are required. Peak magnetic fields are 6.7 T for S-0 and 7.6 T for S-1. The winding consists of three regions of graded conductor. The current density in the inner two sections is limited by the conductor-allowable stress of 28 ksi, while that of the outer section is limited by the cryostability of the conductor.

Six-centimeter gaps on each side of the winding provide space to accommodate side insulation, splices, and leads. A plenum located radially outward from the winding stores additional helium for cooling and provides space for collecting helium vapor formed by evaporation at the surface of the conductors. The winding is enclosed in a 304LN stainless steel vessel that also contains the helium coolant.

The vacuum vessel and associated LN_2 -cooled radiation shield provide vacuum and radiation barriers that reduce liquid helium consumption. The helium vessel and vacuum vessel are made by welding prefabricated parts around the winding after the completion of winding operations. Approximate weights of the winding of S-0 and S-1 are shown in Table 3-9.

The construction of the S-1 solenoid is similar to that of the S-0 solenoid, except that the helium vessel incorporates a 12.7-cm-thick lateral plate that supports the axial magnetic loads that act on the winding.

Detailed design features of S-0 and S-1 are shown in Figs. 3-12 and 3-13.

Conductor and Insulation. The conductor consists of a multistrand niobium-titanium/copper cable or monolith soldered onto the copper stabilizer. The conductor is graded for each winding region. The inner region of S-1 employs a conductor 2.00×2.99 cm. The size of the conductor decreases to 1.71×2.68 cm and 1.35×2.11 cm in the outer regions to accommodate higher current densities.

The basic insulation concept uses "button-on-a-string" insulation for turn-to-turn insulation and "snow-fence" type insulation for layer-to-layer insulation. Turn-to-ground insulation is about 1 cm thick.

Conductor Cooling. The conductor is cooled with pool-boiling HeI at 4.2 K and 1 atm. Cryogenic stability is achieved for each graded zone. The maximum current density in the niobium-titanium is always less than 75% of the short sample value at the maximum operating temperature of 4.4 K. The analysis on which conductor current recovery is based is presented later in this section.

Coil Casing. The coil casings designs are shown in Fig. 3-14. Each casing is made of 304LN stainless steel and consists of a bobbin and an outer ring. The outer ring is closeout-welded to the bobbin after completion of the winding operation. The casing supports the winding and contains the liquid helium used to cool the coil.

The casing of S-0 has an inner diameter of 3.88 m, is 1.18 m high x 0.77 m wide in overall cross section, and has 5-cm-thick walls. The casing of S-1 has an inner diameter of 3.88 m and is 1.31 m high x 1.16 m wide in overall cross section. All walls but the lateral facing S-0 are 5 cm thick. This wall is thicker (12.3 cm) because it supports the axial load of 1.02×10^8 N (22.9×10^6 lb) exerted by the windings of S-1. The 40,000-psi maximum bending stress on the wall results in a conservative safety factor of 2.5.

The axial load on the thick wall is carried by six intercoil columns that transmit the load to the outer and inner rings of the S-0 casing. The load is then balanced by an equal and opposite load exerted by S-1 via a similar intercoil column arrangement. The supports are 0.5 x 0.32 m (20 x 12.5 in.) in overall cross section and have a maximum compressive stress of 287 MPa (41,600 psi) and a safety factor of 2.5. The stress in the inner and outer rings of the casing of S-0 is very low because of the large area of the rings. The axial load can be balanced in the unit flange of the machine without affecting S-0. The top of each coil casing has penetrations for the service-stack loads.

Figure 3-14. Solenoid magnets, TDF-010.

[illegible]

CONDUCTOR AND INSULATION DATA															
COIL	SECTION	WINDING COIL SKEWED J INCHES INCHES	STAIN- LESS STEEL PIPE INCHES INCHES	J/C WINDING PIPE INCHES INCHES	A/D WINDING PIPE INCHES INCHES	A/D WINDING PIPE INCHES INCHES	WINDING PIPE INCHES INCHES	WINDING PIPE INCHES INCHES	WINDING PIPE INCHES INCHES	WINDING PIPE INCHES INCHES	WINDING PIPE INCHES INCHES	WINDING PIPE INCHES INCHES	WINDING PIPE INCHES INCHES	WINDING PIPE INCHES INCHES	WINDING PIPE INCHES INCHES
		WINDING PIPE INCHES INCHES	WINDING PIPE INCHES INCHES	WINDING PIPE INCHES INCHES	WINDING PIPE INCHES INCHES	WINDING PIPE INCHES INCHES	WINDING PIPE INCHES INCHES	WINDING PIPE INCHES INCHES	WINDING PIPE INCHES INCHES	WINDING PIPE INCHES INCHES	WINDING PIPE INCHES INCHES	WINDING PIPE INCHES INCHES	WINDING PIPE INCHES INCHES	WINDING PIPE INCHES INCHES	WINDING PIPE INCHES INCHES
S-0	I	1235 WINDING PIPE	17	12	1899	3.0	1345	2.0	1899	11	1320				
	II	1500 WINDING PIPE	19	18	1899	2.80	1437	1.80	1899	11	1480				
	III	2111 WINDING PIPE	23	15	1730	2.50	1510	1.10	1730	11	1550				
S-1	I	7065 WINDING PIPE	27	12	1459	3.0	1495	2.0	1459	11	1550				
	II	1479 WINDING PIPE	30	14	1520	2.80	1567	1.80	1520	11	1557				
	III	2192 WINDING PIPE	30	20	1608	2.70	1657	1.80	1608	11	1622				
S-2															
WAT		1800 WINDING PIPE	14	14	1525	2.1			222	11	1540	1800			

NOTES -
 1. INSULATION FOR S-1 TO BE SINTERED SHAPE.
 2. INSULATION FOR S-0 & S-1 TO BE POLYIMIDE FIBERGLASS.
 3. ALL CASE MATERIAL SHALL
 4. INSULATED TO BE POLYIMIDE FIBERGLASS.
 5. SET TOP OF COIL ON TOP SUPPORT STRUCTURE & VACUUM VESSELS
 6. ALL COILS SHALL BE 1/2 INCH
 7. ALL COILS SHALL BE 1/2 INCH

Figure 3-14. Continued.

Service Stack. A preliminary service-stack concept is presented in Fig. 3-15. This stack accommodates one set of power leads and accepts all the helium and nitrogen fluids needed to cool the coil and its radiation shield. It also has provision for instrumentation lines, liquid-level sensors, and emergency helium venting. Because the stack requires simultaneous penetration of the vacuum vessel, the radiation shield, and the helium vessel, the use of bellows and coiled tubing is mandatory to allow for differential thermal contraction. The location of the magnet support struts can also affect relative movements in the stack and will have to be optimized in the final design. The fairly large plenum chamber of the S-0 and S-1 solenoids collects the helium bubbles and allows them to migrate from the conductor to the service-stack opening. Since the conductors carry their own hoop forces, penetration of the helium vessel's outer shell will not present a structural problem. The detail design of the service stack will be a straightforward design task.

Engineering Analysis.

Magnetics. The LLNL EFFI program was used to establish basic magnet concepts that satisfy physics requirements at a uniform current density. We optimized the magnets by designing conductors, checking their stress loads and cryostability, and defining graded current density configurations that met the central field requirement. The EFFI program modeled these optimized magnets to verify resizing calculations and to determine the peak field (needed to determine conductor hoop stress and the type and amount of superconductor required).

The solenoids were optimized through an iterative process in which the winding pack is divided into an arbitrary yet reasonable number of grades (three or four). It was helpful to have a concept that had been analyzed for cryostability for various current densities because the size of the first grade could then be chosen by using an integral number of turns and layers.

Figure 3-15. Service stack, TDF-011.

We assume that the winding will be layer-wound to facilitate grading the current density. Knowing (or assuming) the field gradient will allow us to find the peak field for the next graded section, which, in turn, will determine the amount of niobium-titanium superconductor and maximum allowable current density, provided the maximum allowable stress is not exceeded.

Force and Stress Analysis. We performed stress analyses to size the conductors, windings, coil casings, and supports according to design criteria based on those used for MFTF-A, MFTF-B, and the Tandem Mirror Next Step (TMNS).

The STANSOL-II computer code¹ was used to evaluate radial and hoop stresses in the conductors. The conductor pack is modeled as a thick-walled cylinder whose length is equal to the width of the conductor. The effective radial modulus of the conductor and insulation is adjusted to 0.3×10^6 psi to account for gaps in the winding and for radiation-caused swelling of the insulating material. This value is based on data generated in the TMNS study.

Additional design criteria include:

- Conductor: a yield-stress safety factor of 1.5 and the Von Mises equation used for combined stresses.
- Substructure/case: a yield-stress safety factor of 1.5, ultimate strength safety factor of 3.0, and a primary bending allowable of 1.5 times the allowable tension.

Numerous STANSOL runs were made in different magnet locations and with varying substructure thicknesses. We found that the conductor stresses varied little from those of a self-supporting conductor and, because a self-supporting conductor gives a simpler and more compact magnet, we decided to size both S-0 and S-1 on the basis of the conductors carrying their own hoop stresses with no substructure support.

We used a conservative stress-analysis approach to size the thick side wall of S-1 and the intercoil support structure. The axial load of 22.9×10^6 lb was calculated using the EFFI program. The sideplate of the S-1 coil was analyzed as an annular plate under uniform pressure as a result of the axial force, with only the top and bottom portion of the plate providing support. A maximum bending stress of 40,000 psi was found for the 12.3-cm-thick plate. The 304LN stainless steel plate material had a minimum yield strength of 100,000 psi, which resulted in a safety factor of 2.5.

Conductor Cryostability. To support conductor grading iterations, we performed a cryostability analysis to determine the maximum cryostable current density as a function of field strength. To facilitate the magnetic field calculations of Section 3.4.2, the stability analysis was done in terms of the overall current density, packing factor, and conductor unit cell size. In these terms, the stability criterion becomes:

$$q > J^2 \rho_D(B) \frac{A \lambda \phi}{P (1+\phi)} \quad (3-1)$$

where

- q = design heat flux (W/cm^2),
- J = current density over conductor unit cell (A/cm^2),
- $\rho_D(B)$ = stabilizer magnetoresistivity ($ohm-cm$),
- A = area of the conductor unit cell (cm^2),
- P = wetted perimeter of the conductor (cm),
- λ = packing factor (conductor area/unit cell area),
- ϕ = copper-to-superconductor ratio in the conductor.

The conductor design determines the unit cell area and packing factor. The turn-to-turn and layer-to-layer insulation are assumed to cover 50% of the conductor surface, which then determines the wetted perimeter. The copper-to-superconductor ratio was assumed to be approximately 10 to 15 to satisfy quench hot-spot constraints. (See "Quench Analysis" later in this section.)

The magnetoresistivity of the copper stabilizer is computed from:

$$\frac{\rho_D(B) - \rho_D(0)}{\rho_D(0)} = 4.37 \left(\frac{B}{\rho_D(0)} \right)^{1.115} \quad (3-2)$$

where

- $\rho_D(B)$ = resistivity at field B ($ohm-cm$),
- $\rho_D(0)$ = degraded resistivity at zero field ($ohm-cm$),
- B = field strength (T).

Equation (3-3) is a power-law relation, similar to that of Kohler,² that accounts for neutron-radiation-induced damage to the copper lattice that increases resistivity.³ The value of $\rho_D(0)$ is determined by assuming a 20% increase in the initial value of resistivity at 4.5 K and zero field:

$$\rho_D(0) = 1.20 \times \frac{1.73 \times 10^{-6} \Omega\text{-cm}}{\text{RRR}}, \quad (3-3)$$

where

$1.73 \times 10^{-6} \Omega\text{-cm}$ = resistivity of OFHC copper at room temperature,
 RRR = residual resistivity ratio (ρ 300 K/ ρ 4.5 K).

The material selected for the central cell solenoid stabilizer is half-hard copper with an RRR of approximately 90.

The design heat flux q is limited by the conductor's ability to transfer heat to LHeI by pool boiling. For an unconditional stability criterion, the design flux is the minimum film-boiling or recovery flux. This was determined through a series of tests measuring heat transfer to LHeI from conductors in bundles simulating actual winding packs. General Dynamics has established correlations relating minimum film-boiling flux to the average gap dimension between conductors.⁴ Using these data, we set the design flux of the conductor wetted surface at 0.25 W/cm^2 .

Equation (3-1) was solved initially for several typical conductors and plotted as J vs the field, B . These plots were used for the first sizing of the coils. After a trial in which the graded conductors were sized, Eq. (3-1) was again solved to give curves of J vs B to verify the conductor sizes chosen. Figures 3-16 and 3-17 give these latter plots for the S-0 and S-1 coils. Also indicated on the curves are the actual operating points for the three grades of conductor. The fact that the points fall well below the allowable curves emphasizes that the conductors are generally stress-limited.

Radiation Effects. The magnet components most affected by radiation are the stabilizer, the insulator, and the superconductor. The following radiation limits were developed for these components:

- Stabilizer: 1.1×10^{-4} displaced atoms (dpa) for copper stabilizer at 8 T for 2 FPY.

Figure 3-16. Maximum cryostable current density for S-O solenoid.

Figure 3-17. Maximum cryostable current density for S-1 solenoid.

- Insulator: 5×10^9 rads for polyimide-glass insulator over 5.4-FPY life.
- Superconductor: 10% decrease in critical current density over 5.4-FPY life. This corresponds to a neutron fluence of 4×10^{18} n/cm², at energies greater than 0.1 MeV.

For magnet cryostability analysis, the electrical resistivity of the copper stabilizer was allowed to increase 20% because of irradiation. This increase for half-hard OFHC copper at 8 T amounts to approximately 13×10^{-9} $\Omega \cdot \text{cm}$.

J. M. Williams et al.⁵ have experimentally determined the increase in electrical resistivity ($\Delta\rho_{irr}$) of OFHC copper at 4 K for a fast fission neutron spectrum as follows:

$$\Delta\rho_{irr} = 400 (1 - e^{-162 D}) \times 10^{-9} \Omega \cdot \text{cm} ,$$

where D = radiation damage in displaced atoms.

For 1.1×10^{-4} dpa, this yields $\Delta\rho_{irr} = 7.06 \times 10^{-9}$ $\Omega \cdot \text{cm}$. In a subsequent room-temperature anneal, about 85% of this radiation-induced resistivity anneals out and 15% (about 10^{-9} $\Omega \cdot \text{cm}$) remains as residual resistivity in the stabilizer. Thus, an allowance of 13×10^{-9} $\Omega \cdot \text{cm}$ in the electrical resistivity of the copper stabilizer is adequate to accommodate six annealing cycles.

The polyimide-glass insulation retains better than 80% of its compressive strength up to a dose of 5×10^9 rads and is about an order of magnitude better than epoxy-glass insulation; its cost is about four to six times higher than that of epoxy-glass. Epoxy-glass insulation can still be used in relatively low-dose areas of the magnet.

The radiation limit for niobium-titanium superconductor is about the same as that for polyimide-glass insulator if the approximate relationship is 1 rad = 10^9 n/cm². Therefore, both constraints are satisfied.

Quench Analysis. Quench analyses were performed to determine the advantages of having multiple sets of current leads rather than a single set. The analyses were done with the S-2 coil, which has since been reconfigured as S-1 with three current densities instead of four. However, these data should be representative enough to compare the effect the number of leads has on the final temperature following a quench situation.

Figure 3-18 represents the as-modeled coil configuration for the four-lead-set case and gives the final temperatures reached in various regions of the magnet following the dump of the magnet.

The data in Table 3-10 were used in the single-lead-set case and were input into the computer program, SUPERQ, which relates voltage, temperature, current, and time in the event of a quench situation. SUPERQ is an iterative time-step program that includes the enthalpy of both copper and superconductor, but does not include helium cooling effects. It represents a realistic case for the situation where a quench is initiated by a low helium level.

In the single-lead-set case, for an overall current density of 1532 A/cm^2 in the conductor pack, the maximum temperature will reach 85 K using a discharge voltage of 500 V. Similar models were input into SUPERQ for the four-lead-set case to arrive at the final temperature of 67 K in the region of highest current density.

By comparing these final temperatures, we see that a single set of leads will be entirely adequate. The benefits of having a slightly lower final temperature does not outweigh the additional system complexity and risk of failure of the four-lead-set case.

3.2.4. Choke Coils

This section presents the selected conceptual design for the choke coils, gives the relevant engineering analysis, and discusses design considerations and requirements.

The design of the choke coils presents a technical challenge because of high field requirements, stringent space limitations, and the severe radiation environment in which the coil is located. A number of alternatives were considered and examined before a design was selected; the most promising of these are presented at the end of this section. These alternative designs merit further detailed analysis to determine which should be considered in the detailed design phase of TDF.

Design Considerations and Requirements. The selection of design parameters for the choke coil must be made in the context of overall machine design, as it is for most components. Systems plasma physics dictates a peak mirror field of 15 T. The coil system that produces this field should be small

Figure 3-18. Coil configuration and final temperatures following quench for four sets of current leads.

Table 3-10. Input data for single-lead-set quench analysis.

Operating current (A)	11272	Delay (s)	10
Maximum discharge voltage (V)	500	Self field (T)	7.9
Overall current density (A/cm ²)	1532	Background field (T)	0.0
Magnetic energy (MJ)	570.15	Field angle (deg)	0.0
Critical temperature (K)	5.0	Cu/SC ratio	11.2
Normal zone length (cm)	100	Packing fraction	0.815
Normal zone trigger voltage (V)	0.001	KRR	80

enough to permit lateral access for beam injection between the central cell and choke coil. The length of the choke coil should be small to minimize overall machine costs. The choke coil should also be radiation-resistant enough to operate for at least 1 FPY. Several approaches to achieve these goals have been examined. The resistive-coil approach was selected because it satisfies overall machine requirements and results in a simpler design.

Choke coil design requirements are summarized in Table 3-5. The coil utilizes AMZIRC copper for the conductor and is cooled with an axial flow of deionized water. Each coil requires about 26 MW. Spinel was chosen as the coil insulator to increase operational time in the radiation environment.

Engineering Trade-Off Studies. Our engineering trade-off studies focused primarily on the choice of conductor and insulation and on the choice of coolant and cooling configuration for the baseline design. Several alternatives that have potential for further development are presented in Section 3.2.4, "Alternative Design Approaches."

Choice of Conductor and Insulation. The conductor material for the resistive magnet must have high yield strength to withstand hoop stresses and high electrical conductivity for low power dissipation. The best electrical conductors, in order, are silver, copper, gold, and aluminum. Precipitation-strengthened copper alloys have high electrical conductivity, high yield strength, and a high recrystallization temperature. We considered and compared several alloys of copper and one alloy of silver; Fig. 3-19 compares these alloys in terms of mechanical yield strength and electrical conductivity.

The compositions of the alloys are:

- Be-Cu (beryllium 0.4-0.75%, cobalt 2.4-2.7%, balance copper),
- Cu-Ni-Ti (5% nickel, 2.5% titanium, balance copper),
- Glidcop AL-20 (0.4% Al_2O_3 , balance copper),
- Narloy Z (2.75-3.25% silver, 0.3-0.5% zirconium, balance copper),
- Consil 900 (cadmium 10%, Ag 90%),
- ZAC-1 (0.1% zirconium, 0.3% chromium, balance copper),
- MZC (0.03-0.06% magnesium, 0.08 - 0.15% zirconium, 0.4-0.8% chromium, balance copper),
- AMZIRC (0.13-0.2% zirconium, balance copper).

Figure 3-19. Conductor yield strength vs electrical conductivity.

Consil 900 is a desirable conductor because of its low activation, but it does not have sufficient yield strength. Therefore, we selected AMZIRC for the conductor material because it has both sufficient electrical conductivity and yield strength.

Spinel (Al_2MgO_4) has low swelling, no microcracking, and strengthens in a neutron radiation environment. A stress analysis shows that 3% volume swelling in spinel can be accommodated in the magnet structure without causing stress problems. This amount of swelling is expected in spinel after 2 FPY of magnet operation. Under the same conditions, alumina (Al_2O_3) would swell 16% by volume, develop microcracks, and deteriorate considerably in mechanical strength. On the basis of these observations, we selected spinel as the insulation material for the resistive magnet.

Choice of Coolant. Because the thermodynamic and physical properties of water make it an excellent heat-transfer medium, we chose it as the baseline coolant for the resistive coil. In an externally cooled magnet, the water creates problems because of the ability of copper to dissolve in water and to be oxidized by the oxygen within the water. (See Ref. 6 for complete details of problems caused by the use of water in externally cooled copper magnets.)

We investigated two alternate coolants that would eliminate the problems caused. We compared liquid nitrogen and gaseous helium to the water-cooled baseline design, with the figure of merit being the total power (I^2R + pump and/or compressor work) required by the coil. The water-cooled baseline design required 25.9 MW of power, based on an average conductor temperature of 100°C and an average 20% increase in the resistivity of copper as a result of transmutations caused by neutron radiation.

Liquid-Nitrogen-Cooled Option. Assuming the magnet configuration remains the same as the baseline, the I^2R power consumption will be reduced with LN_2 coolant by the fraction of copper resistivity that remains at 80 K. For pure OFHC copper, the resistivity decreases linearly with temperature from room temperature to LN_2 temperatures at a rate of about $6.9 \times 10^{-9} \Omega \cdot \text{cm/K}$, so that ρ_{300}/ρ_{80} is approximately 8. This ratio is smaller in a neutron environment since the increase in resistivity caused by transmutations is independent of temperature. The increase in resistivity caused by neutron damage is equivalent to a 20% increase at room temperature

and to a 160% increase at LN_2 temperatures. For damaged copper, then, ρ_{300}/ρ_{80} is approximately

$$\frac{1.2}{2.6} \times \left(\frac{\rho_{300}}{\rho_{80}} \right)_{\text{undamaged}} = 3.7$$

Thus, for an LN_2 -cooled coil, the I^2R power is reduced by a factor of 8 initially, but only by a factor of 3.7 at the end of 1 FPY.

On the other hand, refrigerator power is important. Refrigerators have efficiencies of less than 0.5, so, at best, an LN_2 refrigerator requires 7.6 W of input per watt of cooling.

The total power consumed by an LN_2 -cooled magnet is $7.6 I^2R + I^2R$, or over 8 times the joule heat dissipation. The benefit of the reduced operating temperature is, for radiation-damaged copper, outweighed by the total power requirement, which is more than twice that of a water-cooled magnet. For this reason we rejected the LN_2 option.

Gaseous Helium-Cooled Option. Its main advantage as a coolant is that it is chemically inert. The hope was that the gas could be pumped through the coil at a velocity (and thus heat-transfer coefficient) high enough to keep the conductor cool without excessive pump power. A heat-transfer analysis shows that, to keep pumping power down in the range of 20 MW, the conductor temperature must exceed 125°C . At 125°C , the joule heating increases to 35 MW, 10 MW higher than the baseline of 25 MW. The total power consumption is around 55 MW, again more than double the water-cooled baseline requirement. Because the gaseous helium cooling concept is not as efficient as water cooling, it was also rejected.

Cooling Configurations. Figure 3-20 summarizes the advantages and disadvantages of the various coolants. We chose an externally cooled coil as the baseline design because it is more compact and economical than an internally cooled coil. Also, it has a current density of 4800 A/cm^2 vs 2800 A/cm^2 for the internally cooled coil. Because the current must be lower in an internally cooled coil, the overall length would be 65 cm as opposed to 48 cm for the externally cooled coil. Another advantage of an externally cooled coil is the lower power requirement of 25.9 MW vs 31.0 MW

CONFIGURATION

COMMENT

Externally H_2O cooled
(Baseline)

Provides the highest indicated allowable current density; however, long-term electrical integrity is questionable.

Externally GHe cooled

Use of GHe cooling should improve electrical integrity but requires up to 2 times greater power.

Externally LN_2 cooled

Use of LN_2 cooling should improve electrical integrity but requires nearly 3.2 times greater power consumption

Internally H_2O cooled
(Alternative)

Good long-term electrical integrity, power consumption not as low as externally H_2O cooled, but allowable current density is lower.

Internally H_2O cooled
LAMF conductor

Good long-term electrical integrity; power consumption may be up to 2 times greater than externally cooled due to high leakage current; however, allowable current density too low.

Figure 3-20. Comparison of coolants and cooling configurations.

compared to internally the cooled coil. Axial cooling, too, is more straightforward than separate cooling of each of the 27 layers of winding.

Although we originally chose the externally cooled coil because of the listed advantages, this choice should be reconsidered as reliability data on the polyhelix configuration becomes available. The internally cooled option is analyzed in more detail in Sections 3.2.4 (Alternative Design Approaches) and 3.2.6.

Selected Design Approach. The choke coil consists of a polyhelix wound with a rectangular zirconium-copper conductor. The magnet is ceramic-insulated and axially cooled with flow of deionized water external to the conductor. Key engineering analysis and major design features follow.

Design Description.

Coil Configuration. The coil design incorporates features that promote long operational life in the radiation environment. The magnet carries 9.83×10^6 A-turns and, in concert with the central cell magnets, produces the 15-T mirror field. Table 3-5 gives the overall coil requirements, and Fig. 3-21 shows the arrangement of conductors and insulation.

Figure 3-9 gives more design details. The winding has an inner radius of 0.122 m and is 0.48 x 0.30 m in cross section. The coil has 35 layers, each with 14 turns. The insulation consists of thick spinel spacers placed between turns and between layers.

The spacers separate the conductors and form the cooling channel through which deionized water flows axially to remove 26 MW of heat deposition. Of this power, 20% is heat from I^2R losses due to radiation-induced resistivity after 1 year of operation at full power. Less than 10% of this power requirement is caused by neutron heat deposition.

The coil is placed in a steel casing that incorporates inlet and outlet manifolds through which water is introduced and removed from the coil. The manifolds enclose and support the ceramic-insulated leads. The casing is streamlined to avoid hot spots caused by plasma interception and to support the axial load of 257 tons exerted by the radial component of the magnetic field.

CONFIGURATION

- AXIALLY COOLED
- CONDUCTOR - ZIRCONIUM COPPER or
MAGNESIUM-ZIRCONIUM-CHROMIUM COPPER
- SPINEL CERAMIC INSULATION
- 304L SS CASE

KEY FEATURES

- CENTRAL FIELD - 12.0T (15.0T w/BACKGROUND)
- CURRENT DENSITY - 4800 A/cm²
- POWER CONSUMPTION 25.9 MW
- WATER FLOW RATE 2,450 GPM
- WEIGHT 6500 POUNDS

Figure 3-21. Baseline concept for the TDF choke coil.

Conductor and Insulation. The conductor is 0.647 x 3.207 cm in cross section and carries 14,106 A with a current density of 4800 A/cm². Because it is made of AMZIRC, the conductor is able to support the design circumferential stress of 18,300 psi with a safety margin of 100%. Its initial conductivity is 93% of that of OFHC copper. Heat from the additional I²R losses caused by radiation-induced resistivity is easily removed by axial cooling because of the large wetted surface of the conductors. The radiation-induced resistivity in AMZIRC copper is less than that of OFHC copper because of the initial content of alloying materials in the conductor. Therefore, a slight increase in conductor cross section can compensate for the additional I²R losses.

The insulation is made of spinel spacers that maintain a 0.21-cm separation between conductors and between layers. The spinel is highly resistant to radiation and exhibits less than 3% swelling in operation. The swelling of the insulation is accommodated by a properly design coil assembly. We assume that the mechanical and electrical properties of spinel are superior to those of alumina and that radiation does not necessarily impose a limitation on the insulator's lifetime. Thin layers of soft material are interposed between the insulator and the conductor to eliminate point loads and prevent fracture of the insulator.

Conductor Cooling. The conductors are cooled with an external flow of deionized water at a required flow of 12,500 liters/min. Introduced into the coil at 32.5 °C, the water flows at 2.2 m/s between conductors. The maximum temperatures of the water and conductor are 60° and 120° C, respectively. A detailed analysis of the cooling scheme is given in Section 3.2.4 ("Selected Design Scheme").

Coil Casing and Support Structure. The general arrangement of the coil casing is shown in Figs. 3-20 and 3-21. Made of 1.25-cm-thick 316L stainless steel, the casing forms both a winding bobbin for the conductors and a pressure vessel to contain the 175-psi cooling water maximum pressure. By means of manifolds at each end of the casing, the cooling water is forced to flow axially through the conductors.

The "spoked-wheel" support at the ends of the coil reacts against the axial magnetic forces generated on the coil pack. While the water pressure generates low stress levels, giving a safety factor of 5, the large axial force of 2.29×10^6 N (514,000 lb) requires closer consideration. The support was devised to allow the cooling water to freely enter and leave the coil pack from the manifolds and to resist the large axial force. The 12 main rectangular spokes (plus intermediate ones) are welded to the inner and outer case, and their loads are transmitted to the end of the manifold for pickup by a separate bucking ring anchored to the radiation shield.

The current leads run through the feed and exit water pipes, with a junction box at each end to make the transition to the conductors. We expect that the coil assembly and its water piping will have to be removed as a unit to replace the coil. The casing will be welded closed after winding the coil since it is not considered feasible to work on the conductor after the coil has been used. The small size of the entire coil makes this attractive. At this stage of the design, conservative hand calculations were performed to size the members. For the final design effort, more detailed analyses can be made to optimize the member sizes.

Engineering Analysis.

Magnetics. Once the required central field is obtained, the next important consideration for the choke coil is to minimize power consumption. Although peak fields are of little concern when choosing a resistive conductor, the design of the M-1 choke coil was still a formidable task. Initially, there were two small resistive coils with current densities exceeding 9000 A/cm^2 . Later these coils evolved into a slightly larger single coil with a current density of 4800 A/cm^2 . Regardless of the values of current density, central field, inner base radius, conductor resistivity, and packing factor, the most efficient geometry for a coil of uniform current density can be determined by two dimensionless geometry factors, α and β . Figure 3-22, taken from Ref. 8, shows a plot of α vs β .

The most efficient geometry is at $\alpha = 3.08$ and $\beta = 1.86$. Moving off this point, the values of α and β can vary widely for a given efficiency line. This results in magnets of varying volume. Further optimization is possible to obtain the least volume (and lowest cost) for the given field and

Figure 3-22. Operating efficiency is a function of magnet geometry.

power. Based on the work of Ref. 8, General Dynamics created an aid to perform this operation, which is presented in Fig. 3-23. The figure shows the locus of the tangency points of constant volume line with the efficiency curves; the line represents the most efficient magnet shape for any given volume. Conversely, if the volume is not known, the line can be followed toward the most efficient point ($\alpha = 3.08$, $\beta = 1.86$) until a set of α and β values is met that gives the required field. This will be the most efficient coil geometry possible. The above discussion is limited to coils of uniform current density.

Work on the MARS study⁹ shows that the highest current densities can be achieved by externally cooled conductors cooled by axially flowing water. This approach was used for the choke-coil baseline design. At a current density of 4800 A/cm^2 , the conductor can be adequately cooled. The central field requirement of 12.0 T can be met, and α (3.46) and β (1.97) are quite close to the maximum efficiency point.

Forces and Stresses. Stress analysis of the conductors is performed by modeling the coil with the STANSOL-II computer code. The stresses are well within allowable values for the AMZIRC conductor.

The buckling analysis of the inner cylinder of the coil casing is based on the American Society of Mechanical Engineers (ASME) Boiler Code, Section III. For a 1.25-cm-thick inner wall of 316L stainless steel, a safety factor greater than 5 for buckling is obtained, and the maximum stress is well below yield. The outer casing is placed in hoop tension by the relatively low water pressure of 175 psi maximum, and the stress level is well below yield.

The analysis of the axial support is based on an axial force of $2.29 \times 10^6 \text{ N}$ (514,000 lb) obtained from the EFFI computer program, and conservative hand calculations show the stresses are well below yield. A finite-element analysis of the final design should be made to optimize this part of the structure.

A check of the stress level in the proposed free-standing conductor shows a peak stress of just under 18,300 psi, well within the range of present technology for conductor materials.

Figure 3-23. Maximum efficiency for any given magnet volume.

Thermodynamics. We analyze the choke coils to determine coolant flow rates and to verify that there is enough heat-transfer capability to transfer the dissipated energy into the water.

First, we compute the total coolant load by adding the joule heating in the conductor to the heat deposited by neutron radiation:

$$Q_L = \frac{J^2 \rho V}{\lambda} + Q_N, \quad (3-4)$$

where

Q_L = cooling load (W),

J = current density in the coil (W),

ρ = average resistive of copper conductors ($\Omega \cdot \text{cm}$),

V = winding volume (cm^3),

λ = packing factor,

Q_N = integrated neutron heat deposition (W).

Once the coolant load is determined, we find the required water flow rate by the following two energy balances:

$$Q_L = \dot{m} c_p \Delta T_w = h A \Delta T_c, \quad (3-5)$$

where

$\dot{m}$ = water flow rate (gm/s)

c_p = specific heat of water (J/gm $^{\circ}\text{C}$),

ΔT_w = temperature rise of water from inlet to outlet ($^{\circ}\text{C}$),

h = heat-transfer coefficient from conductor to water ($\text{W}/\text{cm}^2 \text{ } ^{\circ}\text{C}$),

A = heat-transfer surface area (cm^2),

ΔT_c = temperature differential between the conductor and water ($^{\circ}\text{C}$).

Two additional constraints must be satisfied: to avoid the possibility of boiling in a nonpressurized system, the conductor peak temperature is limited

to 100 °C maximum, and the heat-transfer coefficient is related to the water velocity within the coil by the Colburn equation for flow inside a duct:

$$h = 0.23 (D^{-0.2}) k (\nu^{-0.8}) (V^{0.8}),$$

where

- D = flow passage hydraulic diameters (cm),
- k = thermal conductivity of water (W/cm/°C),
- ν = kinematic viscosity of water (cm²/s),
- V = water velocity (cm/s).

The final assumption: the inlet water temperature is 32.5 °C, a value expected to be typical of the temperature of water exiting a cooling tower.

The flow passage between layers is modeled as a gap between two parallel planes. The hydraulic diameter of the passage is twice the gap dimension. For this coil, the hydraulic diameter is 0.42 cm. The spacers are assumed to block 60% of the passage on the average, so the flow area is computed to be 778 cm², and the wetted area of the conductor is 3.85 cm² per centimeter length of conductor.

The neutron effects are most severe at the inner layer of the coil and drop off exponentially with radial distance. A volume-averaged value of resistivity degradation (20% increase in initial resistivity) was used to compute the average coil resistivity of $2.8 \times 10^{-6} \Omega \cdot \text{cm}$. The volumetric neutron heat deposition of 2.6 W/cm³ was used to compute the total neutron heat deposition of 0.6 MW. With these values, at 4800 A/cm² and a packing factor of 0.70, the heat flux on the conductor wetted surface is 72.3 W/cm², corresponding to a total heat dissipation rate of 22.7 MW. To limit the water temperature to 60 °C at the exit, the required mass flow rate is 209 kg/s, or 12,500 liters/min. This flow rate is equivalent to a water velocity of 2.2 m/s within the coil.

According to the Colburn equation, the expected average heat-transfer coefficient at 2.2 m/s, with the 0.21-cm-wide gap between layers, is about 1.0 W/cm²°C. When multiplied by the total wetted surface area within the coil and equated to the required energy-removal rate, the maximum conductor temperature is computed at 120 °C; this computation is based on an average

water temperature of 46.3 °C. Although the maximum conductor temperature is 20 °C higher than the goal of 100 °C, we foresee no serious threat to magnet performance. Because the conductor is designed to be self-supporting, spacer spacing could be relaxed to increase the wetted surface of the conductor and allow greater flow at the same velocity. Both effects combine to reduce conductor temperature to the desired operating point.

Failure Mechanisms. According to Ref. 6, the possible failure mechanisms in axially water-cooled magnets with organic insulators have been identified. Advanced design concepts provide potential solutions to these problems:

1. Radiolytic corrosion of copper.
2. Water absorption in compressed insulation.
3. Water attack on copper insulator bond.
4. Water oxidation of mechanical joints.
5. Growth of copper-oxide whiskers in water.

The corrosion rate of copper in externally cooled coils is at a minimum in deionized water that has a resistivity of $1 \times 10^6 \Omega \cdot \text{cm}$. The corrosion rate is 7.7 mils/yr for 8 to $10 \times 10^6 \Omega \cdot \text{cm}$ resistivity water and 2.8 mils/yr for $0.005 \times 10^6 \Omega \cdot \text{cm}$ resistivity water, compared to only 0.5 mil/yr for $1 \times 10^6 \Omega \cdot \text{cm}$ resistivity water. From the standpoint of corrosion, $1 \times 10^6 \Omega \cdot \text{cm}$ is optimum for copper conductors. In the presence of radiation, radiolysis of water occurs. Radiation breaks H_2O molecules down to $\text{H}^\cdot$ and $\text{OH}^\cdot$ radicals that interact to produce other radicals as well as H_2 , O_2 , and H_2O_2 . The oxygen concentration in the water is estimated at 8000 parts per billion (ppb). At this oxygen concentration and a water temperature of 60 °C, water resistivity cannot be as high as the $1 \times 10^6 \Omega \cdot \text{cm}$ "optimum." Radiolytic corrosion, which is oxidation of copper by H_2O_2 , may erode 0.65 mm from copper in 2 FPY. Radiolytic corrosion can be suppressed by adding reducing agents, such as H_2 or hydrazine, and can be partially suppressed by an ion exchanger.

The choke coil is designed to use spinel insulation that is permeable to water. This coil can be designed to eliminate mechanical joints where possible, or the mechanical joints can be gold-plated. The coolant passages can also be gold-plated to reduce radiolytic corrosion and the growth of

copper oxide whiskers. Further development will be needed to verify whether these steps can eliminate failure mechanisms in water-cooled magnets in a radiation environment.

Radiation Effects. In the inner layers of the resistive magnet, the conductor, sees a very intense 14-Mev neutron environment. These neutrons affect both the mechanical and electrical properties of the conductor. The most significant changes in mechanical properties result from radiation-induced void swelling, radiation creep, and radiation embrittlement. Those changes in electrical properties are caused by both the transmutation of atoms and to the introduction of defects in the conductor lattice.⁷ The change in electrical resistivity that results when defects are introduced in the lattice (sometimes referred to as displaced-atom damage) saturates above any temperature when the recombination rate of defects equals the generation rate of defects. In OFHC copper, the saturation values of displaced-atom-induced resistivity is $\approx 80 \times 10^{-9} \Omega \cdot \text{cm}$ at $\approx 30^\circ \text{C}$. The resistivity increase caused by transmutations does not saturate but increases linearly with neutron fluence. The primary transmutation products in copper are nickel, zinc, and cobalt. These products are assumed to be in solution in copper; thus, transmutations increase electrical resistivity and result in alloying of the copper conductor.

In small concentrations, nickel, zinc, and cobalt contribute $1.3 \times 10^{-6} \Omega \cdot \text{cm}$, $0.3 \times 10^{-6} \Omega \cdot \text{cm}$, and $6.5 \times 10^{-6} \Omega \cdot \text{cm}$ per atomic percent, respectively. For the choke-coil inner layer, the peak resistivity increase resulting from transmutations alone for a 2-FPY operation, is expected to be about 55% of its unirradiated value. As noted, the resistivity increase due to defects (displaced atoms) would be about 5% of the unirradiated value. This is based on the assumptions that the introduction of transmutation products, or the production of hydrogen and helium caused by (n,p) and (n,α) reactions, would not interfere with the basic recombination of defects process that leads to a saturation of defect density and thus to a saturation value for defect-induced resistivity. If, indeed, the impurities interact with the defects, the saturation value would change and the defect-induced resistivity would assume a higher saturation value. The average change in the conductor's electrical resistivity is expected to be less than 20% over a 2-FPY operation.

Alternative Design Approaches. At the outset of our conceptual design effort, we examined several choke coil approaches before selecting the baseline concept. Trade-off studies show that certain options have definite advantages inherent to the type of coil used. The externally cooled baseline option has lower power requirements than the internally cooled, and the coil can be accommodated in a smaller space. On the other hand, internally cooled coils are more reliable but have larger power and space requirements. Superconducting coils have very small power requirements but are much more expensive. The alternatives discussed in detail below may have sufficient potential for future TDF design studies.

Internally Cooled Resistive Coil. General Dynamics is developing high-field, internally cooled resistive coils applicable to mirror fusion reactors. The goal is to increase coil lifetime beyond the limits set by present technology. Thus, it is a prime candidate for TDF. In fact, recent developments indicate that this alternative is preferable to the selected baseline design because of increased coil reliability. The projected lifetime is substantially longer than that of an externally cooled coil because the turn-to-turn voltage is sustained by dry insulation rather than by water or by insulation in contact with water. The approach also avoids complications caused by copper-water chemistry, thereby reducing the chance of turn-to-turn shorts during operation. More details on coil-failure mechanisms are discussed in Ref. 6.

Figure 3-24 compares an internally cooled conductor for TDF to the baseline externally cooled conductor coil. The internally cooled conductor is limited to a lower current density and therefore requires a higher cross-sectional area to establish the same central field. The power requirement for an internally cooled coil is also higher (roughly 0.6 vs 0.7), primarily because of the lower packing factor and larger build of the internally cooled coil. The larger build (3.76 times the volume) and more complex conductor will increase cost, but the expected increase in service life could make this alternative attractive.

Major design features of the internally cooled concept include separate cooling and powering for each layer of the polyhelix. Although not necessary for adequate cooling, this approach does allow current grading (higher currents for the shorter, inner layers), which could lead to further coil

	4800 A/cm ² EXTERNAL COOLING	2800 A/cm ² INTERNAL COOLING
A ₁ (CM)	12.2	15
A ₂ (CM)	42.2	69
2B (CM)	48	65
TENSION (PSI)	18203	13055
POWER (MW)	25.9	31.0
PACKING FACTOR	0.7	0.6

Figure 3-24. Comparison of internally and externally cooled resistive coils.

optimization. Getting 27 separate leads in and out of the coil is not a trivial task considering the huge side load (514,000 lb) on the winding and the anchoring of each conductor to react the hoop forces.

The outstanding feature of this concept is the fact that the conductor is not encased in a metal sheath with the usual magnesium-oxide powder insulation, but by separate pieces of spinel. This feature raises the packing factor over that of a conventionally sheathed conductor (thus lowering the power required) and reduces the limitations on minimum bending radius--an important consideration for a coil whose inside radius is 15 cm. Because the hoop stresses are low (13 ksi), the conductor can be made from a zirconium-copper alloy that has 93% IACS resistance and a high yield stress of 46 ksi for 50% cold worked and aged. *Half-hard OFHC copper was ruled out* because of its low strength. Figure 3-25 gives additional design features.

The spinel insulation does not have to be thick since the separate power supply for each layer results in a low layer-to-layer voltage. One winding concept is to overwind a turn without the insulation in place, to relax the coil enough to insert the spinel strips, and then to start the next turn.

When the basic uncertainties of either the baseline or the alternative high-field coil are considered, the inevitable conclusion is that some developmental work should precede a final design effort. High-field resistive coils of this size have not been built before, and the high radiation environment compounds the problem. We feel that a low-cost development program can be scoped to yield the answers necessary for a reliable design.

For a detailed engineering analysis of internally cooled resistive coils see Section 3.2.6.

Background Superconducting Coil. The resistive coil of the baseline configuration receives a background field of about 3 T from the central cell coils. Without this background, the resistive coil would require more power. This need led us to consider an additional background superconducting coil to further reduce the power requirement. Although space constraints imposed by the need for an unobstructed neutral beam pathway limit the geometry of such a background coil, it should be considered during the final design because the power saving could be dramatic.

Figure 3-26 shows the calculated ohmic power required by the M1 choke coil for varying background fields. The baseline case with a 3-T background field requires 25.9 MW of power. If the background field can be raised from 3 to 6.2 T, the ohmic power requirement will drop to 12.5 MW. Of course, the reduction in power will have to be balanced against the cost of an additional superconducting magnet.

Figure 3-27a shows the approximate location of the alternative S-2 coil. Unfortunately, other constraints prohibited placing S-2 directly over the M-1 choke coil, its most efficient position. When General Dynamics made a preliminary optimization of the coil, it became apparent that, with the relatively narrow build, the peak field at the inner radius would exceed the 8 T allowable for a niobium-titanium conductor. The weight of the winding alone for this coil is approximately 36 tons, and a cost trade-off against the ohmic power saving of 13.4 MW (25.9 MW - 12.5 MW) should be considered in the final design.

Superconducting Choke Coil. General Dynamics has considered carrying this one step further and eliminating the choke coil. This would require a superconducting coil whose inner radius is large enough to accommodate the shielding required to protect the coil from radiation damage. Such a coil has been tentatively sized, assuming a plasma/halo radius of 20 cm and a shielding thickness of 90 cm. Allowing 27 cm from the inner radius to the outside of the vacuum vessel gives a winding inner radius of 137 cm. The concept coil is shown in Fig. 3-27a. It would require LHe-II cooling and the development of a 16-T rated superconductor. Total weight would be approximately 834 tons.

With such a huge coil eliminating the M-1 choke coil, the next consideration is to eliminate the S-1 coil as well and to move the new coil inboard. This configuration is also shown in Fig. 3-27b. Note that the effects of these alternatives on the physics have not been analyzed and that they are presented as options strictly from the magnet point of view.

3.2.5. Anchor Cell

Design Requirements. The anchor set at each end of the machine consists of a transition coil (T-1) and the yin-yang anchor pair (M-2 and M-3). The anchor-cell magnet set must produce a magnetic field on-axis, providing MHD

NOTES:

1. ALL DIMENSIONS IN CENTIMETERS
2. MATERIAL - AMZIFCO, 1/2 HARD
3. MATERIAL - SINTERED SPINEL
4. OPERATING CURRENT - 15,500 AMPS
5. CENTRAL FIELD PRODUCED - 12.0T
6. BACKGROUND FIELD 3.0T TOTAL FIELD 15.0T
7. MAX. CONDUCTOR TEMP. < 200°C
8. WATER INLET TEMP. 31.2°C
9. WATER OUTLET TEMP. < 60°C
10. WATER FLOW RATE < 12 M/S
11. PACKING FACTOR: 0.605
12. EACH LAYER INDIVIDUALLY MANIFOLDED
- 12 OHMIC POWER: 31.0 MW

TDF INTERNALLY-COOLED
CONDUCTOR CONCEPT
SCALE: AS SHOWN
DRAWING: TDF-014

Figure 3-25. TDF internally cooled conductor concept, TDF-014.

Figure 3-26. Power required for choke coil to produce 15 T for varying background fields.

Figure 3-27. Alternate solenoid options.

stability for the plasma. Its configuration must also be compatible with neutral-beam access requirements and with a requirement to clear the plasma halo. Finally, the magnet windings must be supported for magnetic forces, dead weight, and seismic loads for the design life of the magnet.

Coil Configuration Requirements. MHD stability requirements dictate that the anchor cell winding configuration and coil currents produce a 3-T peak mirror field on-axis and a 3:1 mirror ratio.

To clear the plasma halo, the configuration of the anchor cell magnet set must provide a clear bore for the flux surface that is located at a radius of 17 cm in the central cell.

There must be space through which plasma heating can be introduced at appropriate points on the machine axis by these methods:

- Anchor pumping beam directed up the mouth of coil M-3, at an angle of 15 deg to the machine axis, to strike the axis over the area between $z = 9.2$ and 10 m.
- Anchor sloshing beam directed at 90 deg to the machine axis, through the mouth of coil M-2, at a point $z = 8.95$ m.
- Transition pumping beam, at an angle of 25 deg to the machine axis, aimed to strike the axis over the area between $z = 4.7$ and 6.0 m.
- Rf launchers directed at $z = 9.5$ and 10.5 m.

In addition to these quantitative requirements there is, for beam access, a challenging compromise between the space availability between magnet structures and having enough beam footprint to achieve the desired beam power striking the plasma.

Design Life. TDF has a design life of 10 years, and the anchor cell coils have been designed for 1000 startup and shutdown cycles--a reasonable cyclic requirement for this design life and the expected operating scenario. The anchor cell coils have been designed for a seismic load of 1 g laterally and vertically; this figure is based on an assumed ground acceleration of 0.25 g and on a dynamic amplification of 4, found to be realistically conservative for such massive structures.

Design Criteria. These criteria have been used in the conceptual study:

- The winding should be cryostable.
- The maximum discharge voltage during a quench should be 500 V.

Magnet Structural Design. The allowable stress for magnet structural sizing calculations is 207 MPa. This figure is based on an allowable alternating stress of 414 MPa, derived from crack growth and brittle fracture considerations, with an arbitrary conservatism factor of 2 to reflect that sizing is by hand calculations and that detailed analysis will probably produce higher stress.

Vacuum Vessel Structure. Wall thickness is chosen in accordance with the ASME Boiler and Pressure Vessel Code. The design pressure is 1 atm, and the design temperature is 93 °C.

Engineering Trade-Off Studies. Before proceeding with a point design, we completed several general design studies so that we could evaluate alternative concepts.

Choice of Cooling. From a preliminary study of the relative cost of liquid-helium-cooled superconducting magnets vs water-cooled resistive coils we concluded that the resistive option would be cheaper than the superconducting option. The direct capital cost in 1982 dollars of two coil sets, one at each end of the machine, was estimated at \$17.7 million for the superconducting option and at \$2.7 million for the resistive option. The 10-year life-cycle costs were \$17.7 million (with the operating cost for the superconducting coils assumed to be negligible in this study) and \$14.3 million for the superconducting and resistive options, respectively.

A key assumption that strongly influenced the conclusion was that the superconducting coils would need significant shielding in the bore while the resistive coils, which could be insulated with polyimide, would not. The need for general shielding in the bore of the anchor-cell magnet set meant that larger magnets were needed to provide the same clear bore and, consequently, that there was an increased MAT requirement to achieve the same field on-axis, both of which resulted in the greater capital cost. An evaluation of anchor cell shielding requirements contradicted the original shielding assumptions. Because of the relatively benign nature of the plasma within the anchor cell (regarding the neutron irradiation), neither option would require general shielding of the bore. The use of glass-epoxy insulation was found to be feasible, with perhaps some shadow shielding required to protect against streaming through the choke coil from the central cell.

Motivated by the results of this neutronic study, we reworked the cost comparison, deleting the need for general shielding of the bore of the superconducting magnets. This reevaluation also incorporated relatively minor comments resulting from a General Dynamics and LLNL review of the original study. The reevaluation showed that the estimated capital cost of the superconducting option (\$1.2 million) is less than that of the resistive option (\$2.8 million) and that the life-cycle costs are much less. Although coil configuration changes affecting the actual cost have occurred since this study, the relative economy of superconducting coils for this application is still valid.

Choice of Conductor. The Grade-II yin-yang conductor for the MFTF-B Project was selected. No serious thought was given to alternatives since this conductor has been successfully manufactured, successfully wound into quadrupole coils, and successfully operated at design conditions. Also, the TDF anchor cell performance requirements are very similar to those for MFTF-B (a current density of 3430 A/cm^2 at 4.7 T on TDF, compared to 2837 A/cm^2 at 5.28 T on MFTF-B). As discussed in Section 3.2.3 ("Engineering Analysis"), calculations confirm conductor cryostability for the TDF field and current.

The size difference between the TDF quadrupole coils and those successfully fabricated and operated on MFTF-B was of potential concern because it meant that the conductor would have to be wound into a tighter radius of curvature on TDF than it was on MFTF-B. With the radius of curvature being only half as large on TDF, the bending strain in the conductor would be twice as great, which could damage the conductor. Also, it is more difficult to produce a tight winding when winding to a smaller radius of curvature. We have evaluated these potential concerns, however, and found them to be invalid. Although the bending strain during winding is larger, it is only 2.5%, which is acceptable. Further, during practice winding of the MFTF-B "A" coil, which has a minimum radius of curvature approximately equal to TDF, we encountered no practical difficulty, i.e., no solder joints broke, and the pack tightness was acceptable.

Vacuum Topology. The entire machine will have a plasma vacuum vessel that could also provide the thermal guard vacuum for the anchor cell coils. However, the test samples are inside the plasma vacuum vessel, which would require that the superconducting magnets be warmed up and cooled down every time the plasma vacuum vessel is opened for the installation of test modules.

Because module installation will occur frequently, and the time required to cycle the superconducting magnets between cryogenic and room temperature will be long, the impact on operation would be most undesirable. Thus, we will place the anchor cell coils within a vacuum vessel independent of the plasma vacuum vessel so they can be kept cold when the plasma vessel is penetrated.

Consistent with the provision of a guard vacuum independent of the plasma vacuum vessel, two alternatives are possible. The three anchor cell coils (T-1, M-2, and M-3) can each have their own guard vacuum vessel, or they can be placed within a common vessel. The advantage of individual vessels is that a single coil could be replaced or repaired without disturbing the other two coils in the set. A single vessel, on the other hand, simplifies support problems and reduces the space envelope; it also allows for the coil set to be assembled into the vacuum vessel and then for the subassembly to be loaded into the machine as a unit, simplifying installation. Considering all these factors, the single-vessel approach was favored.

Winding Support. As a minimum, the helium case around the winding pack must be sized so that it is strong enough to act as the coil former during winding and to resist the winding magnetic running loads in plate bending. Sized in this manner, the case is not strong enough to resist integrated running loads whose major effects are lobe-opening forces, with equilibration of the major lobe-opening forces being the more difficult design problem. Equilibration of the major lobe-opening forces can be accomplished in several ways.

Consider coil T-1--because there are only two options for it, it is easy to discuss. On MFTF-B, the "A" coil will be supported against the major lobe-opening forces by a support yoke (deep beams across the major lobes connected by tension members at their ends). As an alternative, the case can be strengthened sufficiently so that it is free-standing against the lobe-opening forces. These two options are illustrated in the upper sketch in Fig. 3-28. Because of its lower cost, the latter option was adopted for TDF. On TDF, the plasma halo fans out into the mouth of coil T-1 to such a degree that a support yoke is not practical. The deep beams have to be long enough for the tension members to clear the halo fan; this would result in such large bending moments in the deep beams that it is impractical to carry the load in this manner.

Figure 3-28. Coil winding support alternatives.

Three options are available for coils M-2 and M-3: the major lobe yokes or free-standing cases discussed above, as well as a third option not available for T-1. This is to connect coils M-2 and M-3 so that they tend to support one another. This option was adopted for MFTF-B and is illustrated in the lower portion of Fig. 3-28. Major lobe-support yokes were again rejected, as they were for coil T-1, because the plasma halo fan fills the mouths of coils M-2 and M-3 to such an extent that it is deemed impractical to support the major lobe-opening force in this way. In an early design study for coil M-3, 40% of the major lobe-opening force was carried by the yoke and 60% by the coil case itself. The structure weight (and therefore cost) was found to be 20% less than the structure weight that would have been required had the case been designed to be free-standing. In this study, however, the deep beam was made just long enough to clear the coil itself. Had the deep beam been long enough for the tension members to clear the halo, the bending moment in the beam would have nearly tripled. It was judged, too, that resizing of the beam for this moment would result in a more expensive design than the free-standing case, leaving the support scheme used on MFTF-B as the only alternative to free-standing coils. Although this design was not evaluated quantitatively, it was rejected in favor of free-standing coil cases for lobe spreading. The geometry of the MFTF-B yin-yang pair lends itself to structural connections between the coils. A modest extension of the case in the minor radius of the coils is sufficient to place the case in that region directly over the midpoint of the major radius of the other coil in the pair. For TDF, however, the axial separation of coils M-2 and M-3 in the yin-yang pair means that a much greater distance must be spanned to provide the structural connection between the minor lobes of each coil to the middle of the major lobe of the other coil in the pair. (On MFTF-B the centers of the major radius of curvature of the two coils actually overlap by 140 cm, while on TDF there is a 37-cm-wide axial spreading). A further consideration deals with design philosophy. A virtue of the free-standing approach is that coil forces and moments become statically determinate, providing greater assurance that they have been accurately computed and properly accounted for than in a design that requires accurate computation of the force-deflection characteristics of the coils for accurate computation of coil forces and stresses. A design in which the coil cases are free-standing is also more tolerant of the sort of design change presently being incorporated into MFTF-B, i.e., axial separation of the coils, which will have a first-order effect on the structural design.

Conclusions. On the basis of the alternatives, we chose a baseline design for the anchor cell consisting of liquid helium-cooled (4.5 K) superconducting coils, with the same kind of niobium-titanium, copper-stabilized, Type-II yin-yang conductor being employed in MFTF-B. The coils will be suspended in a common vacuum tank, distinct from the main plasma vacuum vessel, that will permit installation of the anchor cell coil set as a subassembly and provide the thermal guard vacuum needed to eliminate heating up and cooling down the superconducting coils every time the plasma vacuum vessel is penetrated to install test modules. The cold mass of the magnets will be supported from the common vacuum vessel, and external locations on the anchor cell will be provided for supporting the subassembly from the main machine structure. The individual coil cases are designed to be free-standing under the action of lobe-opening forces, with intercoil support structure needed only to transfer the resulting axial magnetic and inertial loads (dead weight and seismic load) from coil to coil.

Selected Design Approach. The baseline concept has just been discussed. Figure 3-29 illustrates the overall arrangement of TDF, showing the location and orientation of the anchor cells relative to other components. Notice that the two cells are rotated 90 deg about the machine axis relative to one another. For clarity, the anchor cells have been rotated so that their planes of symmetry lie in and normal to the plane of Fig. 3-29; in practice the orientation will be such that the planes of symmetry are at 45 deg relative to the vertical and horizontal. This orientation reduces the tendency to trap helium bubbles under horizontal surfaces of the helium case.

Design Description. The baseline concept consists of three superconducting coils--T-1, M-2, and M-3--enclosed within a single vacuum vessel. A detailed description of these components follows.

Conductor and Insulation. The conductor and insulation are depicted in Fig. 3-30. The conductor consists of a square monolithic core with niobium-titanium strands in copper, with a copper-to-superconductor ratio of 6:1. Additional stabilizer material is provided by a copper sheath surrounding the core and bonded to it by solder joints. Turn-to-turn and layer-to-layer insulation is provided by G-10CR sheets perforated to ensure adequate ventilation of helium bubbles from the winding.

ORNL-DWG 82-3240A FED

Figure 3-29. TDF arrangement.

ORNL-DWG 82-3183 FED

ALL DIMENSIONS IN INCHES

- MONOLITHIC CORE WITH WRAPPED COPPER STABILIZER
- NbTi SUPERCONDUCTOR CONTAINS $46.3 \pm 1.5\%$ TITANIUM
- Cu/Sc CORE = 6.00:1
- Cu/Sc OVERALL = 18.7:1
- RRR CORE = 150
- RRR STABILIZER = 220
- 50/50 Pb Sn SOLDER
- COPPER AREA 1.168 sq cm

Figure 3-30. Anchor cell magnet conductor.

The conductor shown in Fig. 3-30 is wound into the winding pack illustrated in Fig. 3-31. For all three anchor cell coils, the winding is nominally 0.2 x 0.4 m with 420 turns arranged in 28 layers, with 15 turns in each layer. A layer of 1.27-cm-thick G-10CR surrounds the winding and provides ground-wall insulation. The overall dimensions of the winding pack, including ground-wall insulation, are 0.229 x 0.419 m.

Winding Configuration. The winding configuration is described in Table 3-11. Each anchor cell coil is a quadrupole with major and minor radii of 1.25 and 0.35 m, respectively. For coil T-1, there is an 80-cm straight leg between the minor and major lobes. The centers of major radius of curvature for the three coils are located at 6.6 m (coil T-1), 10 m (coil M-2), and 10.37 m (coil M-3), measured from the center of the central cell.

Support Structure. The support structure may be separated into four categories distinguished by the function performed.

1. Helium case. The winding cavity helium case is a rectangular, leak-tight box welded around the winding pack, which serves as a coil former, onto which the winding is wound; the structure, which also includes a helium vessel to contain the coil coolant, continuously supports the winding pack against the in- and out-of-plane magnetic running loads (magnetic load integrated over the winding cross section).
2. Helium-case support structure. The helium-case support structure is a heavy channel section that extends the helium-case cross section parallel to the short dimension of the helium case. Its purposes are to provide a composite box section that is strong enough to equilibrate the bending moments, shears, and axial tension produced by integration of the magnetic running load along the coil and, in particular, to provide enough bending section to equilibrate the resulting critical stress, the bending moment produced by the major lobe-opening forces.
3. Intercoil support structure. The intercoil supports tie the coils together into a single unit and must be strong enough to transmit the intercoil forces, i.e., dead weight, seismic load, and axial magnetic load (the only magnetic load of a non-self-equilibrating character).

COIL	I, A	j, A/cm ²	MAT
T1	5714	3000	2.4
M2	6548	3438	2.75
M3	6548	3438	2.75

ALL DIMENSIONS IN cm

Figure 3-31. Anchor cell winding pack--typical.

Table 3-11. Anchor cell coil-winding parameters.

	Coil		
	T1	M2	M3
Major radius, m
Minor radius, m
Straight, m	0.8	0	0
Axial location, m*	6.7	10	10.37
Winding pack, m × m
Mean turn length, m	13.25	10.05	10.05
MAT	2.40	2.75	2.75
j, A/cm ²	2994	3430	3430
I, A	5714	6548	6548

*Location of center of major radius of curvature relative to center of central cell.

4. Cold-mass support structure. The cold-mass supports suspend the coils inside the anchor cell vacuum vessel and must be capable of equilibrating the resulting loads acting on the coil set.

Figure 3-32 shows a typical coil case cross section to illustrate the composite box character of the case. Case dimensions for each coil are provided in Table 3-12. Figure 3-33 is an isometric of the complete anchor-cell set, showing the coil set at the left end of the machine.

The sides of the helium case against which the in- and out-of-plane magnetic running loads bear have thicknesses t_1 and t_2 in Fig. 3-32. These thicknesses vary from coil to coil, reflecting the fact that the magnetic running load varies from coil to coil. The two remaining sides of the rectangular box that constitutes the helium case provide the former onto which the coil is wound; their thickness is chosen rather arbitrarily.

The helium-case support structure illustrated in Fig. 3-32 is an extension of the two short sides of the helium case, with a cross plate forming the top of the composite box. The h and t_3 dimensions of the structure are chosen so that the resulting beam properties ensure adequate strength at critical locations along the coil center line. As shown in the figure, the helium-case support structure is oriented to produce the most effect on the cross sectional moment of inertia of the case in the direction of bending produced by the major lobe-opening forces.

The case cross section is graded in each coil, with the basic section prevailing over most of the major lobe. The case cross section dimension h is then increased linearly to a maximum value at critical coil sections--the junction between the major lobe and the straight section for coil T-1, and the middle of the minor lobe for coils M-2 and M-3. The grading was done to reduce cost. The maximum section does not have to be run all around the coil periphery; the resulting stresses are generally lower near the center of the major lobe, so the reduced section is adequate there. The basic section extends to ± 50 deg of the center of the major lobe for coil T-1 and to ± 60 deg for coils M-2 and M-3.

Additional helium-case support structure is provided in the form of gussets in the mouth of coil T-1 joining the straight legs between lobes together across the mouth of the coil (see Fig. 3-33). These gussets equilibrate the major lobe-opening forces in direct tension rather than permitting the maximum moment due to major lobe-opening forces to increase

ALL DIMENSIONS IN cm

Figure 3-32. Typical coil case cross section.

Table 3-12. Coil case cross section dimensions.

	Coil T1 Basic section	Coil T1 Max. section	Coils M2 & M3 Basic section	Coils M2 & M3 Max. section
t ₁ , cm	7.51	7.51	7.89	7.89
t ₂ , cm	4.70	4.70	3.53	3.53
t ₃ , cm	5.08	7.62	5.08	10.16
h, cm	25.4	38.1	25.4	53.34
W, cm	49.10	49.10	47.93	47.93
H, cm	65.99	81.23	66.37	99.39

Figure 3-33. Anchor cell coil set.

linearly along the 80-cm straight leg. Analysis has shown that these gussets will not be in the way of the plasma halo. The plasma halo, while fanning out in the mouth of coil T-1, does so outboard of the straight legs.

The intercoil supports are depicted in Fig. 3-33. They consist of four gussets connecting coils T-1 and M-2 at the centers of their major lobes and of two beam supports connecting the minor lobes of coil M-2 to the centers of the major lobes of coil M-3. The gussets are plates, each 25-cm deep and 5-cm thick; the beam supports are hollow box beams, 30-cm square with a 2.5-cm wall thickness.

The cold-mass support concept is shown in Fig. 3-34. Struts from the coil cases to the reinforcing yokes on the vacuum vessel transmit the resulting loads on the coil set to the yokes.

Vacuum Vessel. The vacuum-vessel concept is depicted in the Fig. 3-35 isometric and in the section views of Figs. 3-36 and 3-37. As discussed in Section 3.2.5 Vacuum Topology, a single vessel has been designed to enclose each anchor coil set. The major features of the vacuum vessel, discussed in more detail below, are:

- Outer vacuum surface.
- Reinforcing yokes.
- Inner vacuum surface or bore.
- Rf penetrations.

The shape of the vacuum vessel has, through evolutions, become increasingly complex. Although this complexity may not be readily grasped from Fig. 3-35, it is better understood when the current vessel is contrasted with an earlier configuration. The earlier configuration consisted of an outer cylindrical vessel with hemispherical heads completely surrounding the coil set and of an inner bore to permit the plasma to pass through the anchor cell. The diameter chosen for the cylinder was to be large enough to completely contain the plasma halo as it fans out in the mouths of coils T-1 and M-2. The portion of the vessel that encloses the yin-yang is still pod-like in Fig. 3-35, but the diameter has been reduced to more nearly conform to the coils. The result is that the bore of the vacuum vessel actually intersects the outer shell of the vessel, producing the slot (top of the vessel in Fig. 3-35) that permits the halo to fan out beyond the outer shell through the mouths of coils T-1 and M-2. In the present configuration, the portion of the vessel around coil T1 has been shaped to conform to the shape of the coil.

DANL-DWG 82-3287 FED

Figure 3-34. Cold mass support concept.

ORNL-DWG: 82-3207A FED

Figure 3-35. Anchor cell vacuum vessel.

Figure 3-36. Section of anchor cell through the mouths of coils T-1 and M-2.

Figure 3-37. Section of anchor cell through the mouth of coil M-3.

The changes made to the early pod-like configuration were primarily for two purposes: (1) to provide a larger annular gap between the vacuum vessel and the inside diameter of the shielding in order to ensure adequate cryopumping of the anchor cell and (2) to improve access for the transition pumping beam.

A sketch of the inner shell or bore of the vacuum vessel appears in Fig. 3-38. The bore shape was chosen to accommodate the plasma halo fan in the mouths of coils T-1/M-2 and in the mouth of coil M-3.

A liquid nitrogen cold shield following the vacuum vessel wall is located on the coil side of the inner and outer vacuum vessel surfaces. In those areas where the vacuum vessel surface lies close to the coils, (e.g., most of the inner surface and where the outer surface wraps the legs of coil T-1), a 10-cm space is reserved for the vacuum vessel. This space is considered sufficient to accommodate the vacuum vessel wall (including water-cooling channels), the liquid-nitrogen cold shield, multilayer insulation, and tolerance.

Access requirements for the neutral beams are listed in Section 3.2.5 ("Coil Configuration Requirements"). The anchor cell pumping beam is aimed up the mouth of coil M-3 at an angle of 30 deg to the machine axis. Because of the angle and location of injection, a beam dump must be located inside the yin-yang pair for the anchor pumping beam. The anchor pumping beam dump is illustrated in Fig. 3-39 and is discussed in detail in the "Neutral Beam" chapter. The anchor sloshing beam is directed through the slot in the mouth of coil M-2. Unlike the anchor pumping beam, the sloshing beam requires no dump inside the coil set because it can pass out through the slot on the other side. The contour of the vacuum vessel around coil T-1 provides ample access for the transition pumping beam (see Fig. 3-37).

There are, in connection with the anchor pumping beam, local exceptions to the minimum vacuum vessel standoff distance mentioned above. To accommodate the anchor pumping beam dump and not interfere with the plasma halo, it is necessary to indent the vacuum vessel about 2.5 cm locally. The flat sides of the inner surface of the vessel in the mouth of coil M-3 also have cylindrical indentations (see view A-A in Fig. 3-39) to enlarge the anchor pumping beam footprint on the plasma. In these regions, it may be necessary to use a thin copper plate as a cold shield that, although it has no active LN_2 cooling over its surface, is attached along its edges to the actively cooled portions of the cold shield. This thin plate or "chill" would rely on conduction in

Figure 3-38. Inner shell of anchor cell vacuum vessel.

DRNL-DWG 823283 FED

Figure 3-39. Anchor pumping beam dump.

the plane of the chill to its actively cooled edges to provide a cold shield in these very local regions where space does not permit use of the LN_2 shield used elsewhere.

The trajectory of the transition pumping beam is shown in Fig. 3-37; note that it physically interferes with the vacuum vessel and the case of coil T-1. An acceptable solution to this problem is to notch the vessel and coil case locally to provide space for the beam. The spot where the trajectory interferes with the coil case (i.e., at the center of the major lobe) is a location of relatively low stress, and local notching is clearly feasible.

The entire inner surface of the vacuum vessel must be water-cooled. There is a uniform heat load of 1.5 W/cm^2 over the entire surface of the inner shell, with locally higher loads in close proximity to the neutral beams. The heat load is 70 W/cm^2 where the anchor pumping beam passes through the mouth of coil M-3 and 20 W/cm^2 where the anchor sloshing beam passes through the mouth of coil M-2.

Local cooling will also be required where the transition pumping beam passes close to the outer shell of the vacuum vessel. The heat load at this location is 10 W/cm^2 . For this application, it is considered feasible to provide water-cooled surfaces designed to last for the life of the machine.

A shielding detail is shown in Fig. 3-40. Although general shielding of the anchor cell magnets is not required, shadow shields are necessary to shield local areas from neutron streaming from the central cell through the bore of plug coil M-1. The shield is attached to the wall of the vacuum vessel and must be water-cooled. Similar shielding is required in the same area of coil M-3 (coil M-2 is adequately shielded by T-1 in the region of neutron streaming from the central cell); Fig. 3-36 illustrates an unresolved problem in this regard. As shown in Fig. 3-36, the plasma halo clips the inside corner of the shielding on coil M-3, violating the requirement for a 45-cm shadow-shield thickness in this area. This will require further study.

The shielding near the centers of the major lobes, depicted in Fig. 3-40, is necessary on coils T-1 and M-3 to ensure that the following neutronic shielding criteria are met.

- Maximum integrated dose of 5×10^9 rads.
- Maximum damage in copper of 1.1×10^{-4} dpa (producing a 7 n Ω -cm increase in stabilizer resistivity).
- Maximum neutronic heating of 0.06 mW/cm^3 .

Figure 3-40. Anchor cell shielding detail.

The outer shell of the vacuum vessel is reinforced by two support yokes (Fig. 3-35): a circular yoke of 1-beam cross section at $z = 10.15$ m (approximately the axial location of the center of gravity of yin-yang pair M-2/M-3) and a rectangular yoke of hollow rectangular section at $z = 6$ m (approximately the axial location of the c.g. of transition coil T-1). The yoke locations are dictated by beam access and halo clearance requirements. The reinforcing yokes provide support structure with bending strength sufficient for

- Supporting the cold mass inside the vessel.
- Lifting and handling a completed anchor-cell module consisting of the vacuum vessel and three magnets.
- Supporting a complete anchor-cell module from the main plasma chamber.

Also shown in Figs. 3-36 and 3-37 are the four 10-cm-diam penetrations for rf. Two are aimed at each of the points ($z = 9.5$ and $z = 10.5$ m) on the plasma axis. Sections through the anchor cell at $z = 9.5$ m and $z = 10.5$ m are given in Fig. 3-41, which shows these penetrations. The physical interference between the penetrations and the coil cases at $z = 10.5$ m will require minor local notching of the coil cases.

One feature not shown in Fig. 3-41 is the stiffening required for the flat-plate portions of the inner surface of the vessel, which are shaped to accommodate the fanning of the plasma in the mouths of the coils. To support the applied pressure load, these portions of the inner wall will require stiffening ribs. In those areas where the flat plates are immediately adjacent to the coil cases and space limitations preclude the use of stiffening ribs, standoffs made of a thermal insulating material to reduce heat leak would be used to provide support from the coil itself. Note that the pressure differential across these plates acts in a direction to push them against the coils.

Engineering Analysis.

Magnetic Field Distribution. Magnetic field calculations have been performed with the EFFI computer program. The geometry of the conductor used for this purpose was slightly different than that described in Table 3-11 in that the nominal winding cross section of 0.2×0.4 m was used, rather than

Figure 3-41. Anchor cell sections at (a) $z = 9.5 \text{ m}$ and (b) $z = 10.5 \text{ m}$.

the 0.204 x 0.393 m stated in Table 3-11, with the correct center line configuration and with current density adjusted as necessary to ensure that the total MAT were correct. This was to permit magnet field and force calculations to proceed in parallel with conductor choice and winding layout. This difference is very small and has a negligible effect on the results.

Figure 3-42 shows a plot of the magnet field on-axis for the anchor cell region. The desired field on-axis characteristics, i.e., a 3:1 mirror ratio and a 3-T peak mirror field are actually exceeded by the baseline magnet system; the mirror ratio is approximately 3.7:1, and the peak mirror field is actually 3.45 T. Therefore, it is possible to reduce coil cost somewhat while still meeting the minimum field requirements. A potentially more significant feature in the field distribution shown in Fig. 3-42 is the local field dip at the inboard yin-yang coil (near $z = 8.5$ m). This dip does not improve performance, but is the result of an unnecessary feature of the configuration. It would be preferable if the field on-axis were to increase monotonically from the transition minimum near $z = 6.7$ m to the first anchor peak near 8.7 m. During the evolution of the baseline magnet systems, an axial separation was intentionally introduced between coils T-1 and M-2 to allow space for coil structure. This separation was later proved to be larger than necessary to meet structural requirements; it also causes the local dip in field on-axis. The presence of the dip in field has been reviewed by TDF plasma physicists and found acceptable for performance.

When coil T-1 is moved 20 cm closer to coil M-2--the amount of separation introduced unnecessarily--the dip can be eliminated. However, to avoid lengthy reanalysis of fields, forces, and coil structural requirements, the baseline was not revised to eliminate the dip as it would be in future TDF studies.

Field plots have also been generated at critical locations in the cross sections of the anchor cell coils to determine the peak field in the anchor cell magnets. The results of this evaluation are provided in Table 3-13.

Electromagnetic Analysis. Figure 3-43 presents a plot of operating current vs field for the conductor described in Section 3.2.3 ("Selected Design Approach"). The values of operating current were obtained from the experimentally determined critical current for this conductor by imposing an operating margin of 0.21, where operating margin is defined to be equal to 1 minus operating current/critical current.

Figure 3-42. Anchor cell magnetic field on-axis.

Table 3-13. Peak fields in anchor cell coils.

Coil	Peak field (T)	Location
T-1	4.3	Center of minor lobe
N-2	4.7	Center of major lobe
M-3	4.7	Center of minor lobe

Figure 3-43. Operating current vs peak field.

The margin of 0.21 was chosen because it exists in the MFTF-B application of this conductor. As seen in Fig. 3-43, the condition for coil T-1 is conservatively to the left of the curve. For coils M-2 and M-3, the operating point is slightly to the right (margin of 0.19, rather than 0.21), but it is acceptable for this conceptual design study.

A stability calculation performed for this conductor reflected an increase in resistivity caused by irradiation. The stability margin was found to be about 7%, i.e., the critical heat flux for this conductor is 0.19 W/cm^2 and the heat flux required to dissipate the joule heating in a normal zone is 0.177 W/cm^2 , neglecting heat flow away from the normal zone. No detailed dynamic stability calculations were performed. The 7% stability margin is inconsistent with the stability margin in previous applications of this conductor and is deemed to be unsatisfactory. For example, the final stability margin for this conductor in the MFTF-B "A" cell is 33%. In future evolutions of TDF, design modifications (e.g., reduced current density) will have to be incorporated to ensure cryostability.

In an adiabatic quench analysis performed for the conductor, the dump resistance selected to limit the discharge voltage to 500 V resulted in a maximum hot-spot temperature rise of approximately 100 K. It is judged acceptable, however, on the basis that the resulting strain will only be 0.045%, or considerably less than yield.

Coil Forces and Stresses. Electromagnetic forces on the coils were calculated using the EFFI computer code. The modeling of the conductor pack used in this calculation was the same as that used to calculate fields (Section 3.2.3, "Engineering Analysis").

A summary of coil electromagnetic running loads is presented in Tables 3-14 and 3-15 for a one-quarter model of the coil set; running loads for the other three quadrants may be deduced by symmetry. A sketch of the quarter model is given in Fig. 3-44. Each coil lobe was divided into nine segments of equal arc length, or a 10 deg angle for each segment, with forces given at the midpoint of each segment ($0 = 5, 15, \dots, 85$ deg relative to the beginning point of each lobe). In-plane force is positive when directed radially outward, and out-of-plane force is positive when it tends to cause lobe spreading.

Table 3-14. Out-of-plane electromagnetic running loads on anchor set, MN/m.

θ , deg.	Coil M3 Major arc	Coil M3 Minor arc	Coil M2 Minor arc	Coil M2 Major arc	Coil T1 Major arc	Coil T1 Minor arc
5	2.4701	0.5920	1.0407	3.2085	2.9327	0.0561
15	2.4770	0.6254	1.0252	2.7701	2.8208	0.4568
25	2.4918	0.6918	0.9948	2.6420	2.6526	0.8724
35	2.5168	0.7726	0.9506	2.6527	2.4889	1.2886
45	2.5568	0.8557	0.8950	2.7501	2.3582	1.6865
55	2.6218	0.9322	0.8315	2.9115	2.2593	2.0441
65	2.7333	0.9955	0.7657	3.1147	2.1774	2.3386
75	2.9453	1.0405	0.7068	3.3155	2.0949	2.5488
85	3.4323	1.0638	0.6709	3.4451	1.9928	2.6583

Table 3-15. In-plane electromagnetic running loads on anchor set, MN/m.

θ , deg.	Coil M3 Major arc	Coil M3 Minor arc	Coil M2 Minor arc	Coil M2 Major arc	Coil T1 Major arc	Coil T1 Minor arc
5	0.9204	3.3528	4.1003	0.5495	1.2441	2.9112
15	0.9171	3.6515	4.0817	0.7856	1.1480	3.1404
25	0.9099	3.8649	4.0437	0.9000	1.0155	3.3073
35	0.8976	4.0226	3.9846	0.9808	0.8977	3.4338
45	0.8783	4.1399	3.8992	1.0587	0.8078	3.5313
55	0.8474	4.2270	3.7841	1.1525	0.7366	3.6056
65	0.7956	4.2884	3.6278	1.2722	0.6666	3.6599
75	0.6990	4.3277	3.4159	1.4054	0.5699	3.6956
85	0.4738	4.3470	3.1177	1.5016	0.3700	3.7134

Figure 3-44. Notation for coil electromagnetic forces.

A summary of the resulting coil loads is given in Table 3-16. These loads on individual coils are caused by the coil dead weight, by the l-g lateral and vertical seismic load, and by the resultant electromagnetic load. Because of the symmetry of the magnet configurations, integration of the running loads over the coil produces resultant load only in the axial direction.

Comparison with MFTF-B establishes the ability of individual turns in the winding to transmit the electromagnetic running loads that accumulate from turn to turn and layer to layer. The electromagnetic running loads on individual turns are of about the same magnitude on both TDF and MFTF-B (i.e., the product of conductor current times field is about the same); since the winding cavity dimensions (height and width) on TDF are about half as large as those on MFTF-B, the accumulated loads (and therefore the stresses due to this source of loading) are about half as large.

The sense of the in- and out-of-plane magnetic running loads causes the winding pack to bear against the two sides of the helium case with thicknesses t_1 and t_2 , as shown in Fig. 3-32. The sidewalls are evaluated as being simply supported, with uniformly applied load leading to the following design equation for the required thickness, in meters:

$$t = (0.75fL/207)^{1/2} ,$$

where f is the running load in MN/m and L is the sidewall span in meters. The thicknesses t_1 and t_2 for this equation are determined with values for f_1 and f_2 (the maximum local running loads bearing on sidewalls t_1 and t_2) selected from Tables 3-14 and 3-15. The calculations are summarized in Table 3-17.

To shorten the effort involved in this study, the cases for coils M-2 and M-3 have been made identical (see Table 3-12), with the thicknesses t_1 and t_2 corresponding to the coil M-2 dimensions in Table 3-17. Therefore, the structural limit of 207 MPa will be slightly exceeded in wall t_1 of coil M-3. Recall, however, that the stress limit has an arbitrary factor of 2 to account for such simplifications.

As discussed in Section 3.2.3 ("Winding Support"), the coil case is designed to equilibrate lobe-opening forces in case bending. To accomplish this we sized by hand analysis, using beam theory. Force resultants (axial tension and bending moments about the two principal axes in the plane of the

Table 3-16. Force summary--anchor set.

Force component	Coil T1			Coil MC			Coil M3		
	Dead weight	Seismic	Magnetic	Dead weight	Seismic	Magnetic	Dead weight	Seismic	Magnetic
Axial (Z)	0	+21.05	-23.61	0	+17.74	-49.49	0	+17.74	-148.0
Horizontal (X)	0	+21.05	0	0	+17.74	0	0	+17.74	0
Vertical (Y)	-21.05	+21.05	0	-17.74	+17.74	0	-17.74	+17.74	0

All forces in 10^3 kg.

Table 3-17 Helium case sizing for magnetic running loads.

Coil	f1 (MN/m)	L1 (m)	t1 (cm)	f2 (MN/m)	L2 (m)	t2 (cm)
T-1	3.7134	0.4186	7.508	2.6583	0.2292	4.701
M-2	4.1003	0.4186	7.889	1.5016	0.2292	3.533
M-3	4.3470	0.4186	8.123	1.0638	0.2292	2.974

cross section) were computed at 10-deg increments along the coil. The combined normal stress was then calculated at the most highly stressed point in the cross section, using the equation

$$\sigma = M_x/Z_x + M_y/Z_y + P/A.$$

Here M_x , Z_x (M_y , Z_y) are the resultant moment and the section modulus with respect to an axis through the centroid of the coil case cross section, parallel to the long and short dimensions, respectively, of the winding pack, and A is the area of the coil case cross section. Table 3-18 is a summary of combined stresses at critical locations; it gives coil parameters for coils T-1 and M-2 at two critical locations. Detailed stress analysis was not done for coil M-3; comparison of the loads for coil M-2 to those for M-3 reveals that making the coil cases identical for the two coils is conservative, but not unduly so.

The coil weights are provided in Table 3-16. Stiffening rings built into the vacuum vessel support the coils. The calculated axial locations of the coils' centers of gravity are reported in Table 3-19, which also includes the axial locations of the cold-mass support points. Using the information provided in Tables 3-16 and 3-19, we computed support loads for static equilibrium of the anchor set, calculated resultant forces and moments for the intercoil supports, and, through analysis, verified that the stresses within the intercoil supports are less than allowable.

As discussed previously, the coil cases are designed to equilibrate the large lobe-opening forces in bending. Because this manner of winding support is inherently more flexible than the alternate support schemes considered, we were concerned that deflections might be too large, i.e., might cause unacceptable distortion of the magnetic field. Therefore, to obtain a rough order-of-magnitude estimate of the coil deflections, we performed a rudimentary deflection analysis on coil M-2, which has the largest lobe spreading forces. This calculation resulted in 0.64-cm spreading of the major lobes, which is judged acceptable.

Vacuum Vessel Size. One requirement of the vacuum vessel is that the plasma bore be sized and shaped to clear the plasma halo (the flux surface, a 17-cm radius cylinder in the central cell). Figures 3-36 and 3-37 show the

Table 3-18. Summary of coil stresses.

Location	Coil T1		Coil M2	
	50° from center of major lobe; location of max. stress in basic section	Junction of major lobe with straight section; location of largest stress resultants	60° from center of major lobe; location of max. stress in basic section	Center of minor lobe; location of largest stress resultants
M_x , MN-m	1.5441	4.0059	2.2321	6.6081
Z_x , m ³	0.01712	0.02584	0.01641	0.04095
M_y , MN-m	1.4603	0.9338	0.6745	0
Z_y , m ³	0.01326	0.01549	0.01423	----
P , MN	1.6711	1.2503	1.6373	7.2276
A , m ²	0.1217	0.1397	0.1159	0.1572
σ , MPa	214.4	224.1	197.2	207.5

Table 3-19. Center of gravity and support locations.

Component	Axial Location,* m
Coil T1	6.99
Coil M2	9.67
Coil M3	10.71
Rectangular yoke	6.
Circular yoke	10.15

*Measured relative to the center of the center cell

projection of the plasma halo on the two planes of symmetry of the anchor cell. Figure 3-36 illustrates the need for the slots in the sides of the vacuum vessel to accommodate the halo as it fans out in the mouths of coils T-1 and M-2. Figures 3-36 and 3-37 also show that the halo clears the coils in these two projections.

The same two figures illustrate potential problem areas where it is unclear if there is physical interference between the halo and the vacuum vessel or even between the halo and the coils themselves. Considerable effort was expended to ensure that interferences do not exist.

For example, consider the halo fan in the mouth of coil M-2. There is clearly no problem from $z = 8.5$ to 9.1 m; even in the x-z plane (Fig. 3-37), the halo fan is thinner than the width of the slot and diminishes in thickness with distance y from the x-z plane. In the region $z > 9.1$ m, however, as shown in Fig. 3-37, the width of the plasma halo in the x-z plane exceeds the width of the slot, and it is possible that the walls of the slot may interfere with the halo. The point $z = 9.75$ m (the B section in Figs. 3-37 and 3-38) presents a potential problem because the width of the halo in the x-z plane not only exceeds the width of the slot in the vacuum vessel at that point, but also exceeds the coil-case-to-coil-case clear space in the mouth of coil M-2.

Figure 3-45 shows a section through the plasma near $z = 9.75$ m; as can be seen, the halo is well away from the inside surface of the vacuum vessel. By drawing similar sections through a number of axial positions, we have demonstrated conclusively that the vacuum vessel described in this report satisfies the requirement to pass the plasma halo.

The wall thickness for the outer surface of the vacuum vessel was chosen in accordance with Section VIII of the ASME Boiler and Pressure Vessel Code. Sizing in accordance with paragraph UG-28 of the Code resulted in a required wall thickness of 0.478 cm for the hemispherical portions of the outer shell and of 0.956 cm for the cylindrical portion. Time did not permit detailed design of the flat portions of the outer shell, which will be stiffened flat plates. For the purpose of estimating weight and cost, we assumed that these portions have an equivalent uniform thickness of 2.54 cm.

Preliminary sizing of the support yokes was accomplished by designing each yoke to carry the full dead weight of the coil set as diametrically opposed concentrated forces. Although this method of sizing is probably conservative, the yoke section dimensions that resulted are deemed to be reasonable.

Figure 3-45. Section through anchor coil at $z = 9.75$ m.

3.2.6. Internally Water-Cooled Resistive Coil

A high-performance magnet that is internally cooled by water is an attractive alternative to a magnet that is externally cooled. Externally cooled magnets have problems with wet polymer insulation shrinking or swelling, embrittling, and extruding into the coolant passages. Also, copper whiskers can grow from one layer to another and create a short.⁶ Internally cooled coils have their own set of problems. Heat generation is proportional to the product of J^2 and L , where J is current density and L is length of coolant path. There is a limit to the amount of cooling possible; therefore, there is a limit to the J^2L product. Because internally cooled coils have longer coolant paths, they have a lower achievable current density. Another trait of internally cooled coils is the reduced wetted surface area (and coolant flow area), requiring larger water flow rates to improve heat transfer. The net effect is to make pumping power more important than in an externally cooled coil. Higher flow rates are necessary, and the long coolant path must be broken down to little more than one coolant path per layer, at which point the coolant path is still longer than for externally cooled magnets. All this means that manifolding is more complex, copper operating temperatures are higher, and a pressurized system might be necessary.

The increased reliability of an internally cooled coil must be weighed against the penalties of more difficult thermodynamic cooling requirements. To quantify these requirements, a thermal analysis of an internally cooled coil concept was performed. The maximum current density vs hydraulic length and operating parameters was determined for the proposed alternative baseline TDF choke coil. By analyzing the results, one can select an optimum conductor and operating parameters for minimum operating power.

Theory. The internally cooled coil can be modeled as flow through a pipe with frictional losses and heat generation within the pipe wall. In the steady state heat is generated by electrical resistance, and neutron heat deposition is carried away by the water. The problem is described by the following energy balances:

$$Q = \dot{m} (C_p) (T_{out} - T_{in}) = h (P_w) L (T_{cond} - T_a) = Q_{nt} \frac{J^2 (AUC) \rho L}{\lambda} \quad , \quad (3-6)$$

where

Q = heat flow (W),

$\dot{m}$ = flow rate of water (kg/s),

C_p = heat capacity of water (W·s/kg·K),

T_{out} = water outlet temperature (K),

T_{in} = water inlet temperature (K),

h = film coefficient (W/m²K),

P_w = wetted perimeter (m),

L = length of coolant channel (m),

T_{cond} = conductor temperature (K),

T_a = average water temperature (K),

Q_n = neutron heating (W),

ρ = electrical resistivity (Ohm·m),

A_{uc} = area of the unit cell (m²),

J = current density (A/m²),

λ = packing factor.

The figure of merit of a resistive coil is the total power it dissipates. This power is the sum of the electrical work needed to overcome resistance and the pump work required to cool the coil:

$$W = \frac{J^2 \rho A_{uc} L}{\lambda} + W_p, \quad (3-7)$$

$$W_p = \frac{\pi \Delta p}{\rho_f}, \quad (3-8)$$

$$\Delta p = f \frac{L}{D} \rho_f \frac{v^2}{2}, \quad (3-9)$$

where

W_p = pump power (W),

Δp = pressure drop (N/m²),

ρ_f = density of water (kg/m³),

f = friction factor

D = hydraulic diameter, m

v = velocity of water, m/s.

The conductor pack for TDF for a current density of 2800 A/cm^2 is shown in Fig. 3-25. A method is being developed to determine the geometry of the optimum internally cooled conductor that will fit in a given conductor pack. The conductor is shown in Fig. 3-25. The values for areas, packing factor, and length of different layers are also listed.

Various assumptions must be made to set values for several of the above variables. The inlet water temperature is assumed to be coming from a cooling tower and estimated to be at 32.5°C (90°F).

The conductor temperature is assumed to vary linearly with length. Heat transfer is assumed to occur between this average conductor temperature and an average water temperature. The peak conductor temperature was kept below 200°C (392°F) to avoid recrystallization of the cold-worked copper and the subsequent loss of strength. Therefore, both a peak and an average conductor temperature (T_{cp}) were calculated. The peak conductor temperature is approximated by the following relationship:

$$T_{\text{cond}} - T_a = T_{cp} - T_{\text{out}} \quad (3-10)$$

Conductor resistivity is affected by temperature changes, transmutations, lattice deformations, and radial location. Total resistivity is calculated as follows:

$$\rho = \rho_{\text{temperature}} + \rho_{\text{transmutations}} + \rho_{\text{lattice}} \quad (3-11)$$

The temperature effect on resistivity is a function of the type of copper used and the amount of cold-working. The TDF internally cooled coil will use zirconium-copper, which has an IACS of 93%. Through cold-working, the conductivity of zirconium-copper will degrade about 3%, so a value of 90% is used in the equation that follows. (Pure, fully annealed copper has an IACS of 100%). Note that the average conductor temperature is used to compute the temperature-dependent component of resistivity.

$$\rho_{\text{temperature}} = 0.006818 T_{\text{cond}} + \frac{1.55}{0.90} (10^{-6} \Omega \cdot \text{cm}) \quad (3-12)$$

Transmutation effects for TDF were, except for changes caused by wall loading and bore radius, assumed to be similar to those computed by the University of Wisconsin for the Mirror Advanced Reactor Study (MARS). The transmutation effects drop off as the location increases radially because of progressively thicker wall copper and water that acts as shielding. Figure 3-46 shows the change in resistivity per total resistivity as it changes with the radial location of the conductor.

The following are values for radial positioning corresponding to the inner and outer layers, the middle layer, and a layer between the inner and middle layers, in ρ transmutations ($10^6 \Omega \cdot \text{cm}$).

- Inner layer: 1.1487.
- One-fourth: 0.4022.
- Middle: 0.1081.
- Outer: 0.0089.

The increase in resistivity caused by lattice displacements is $0.6 \times 10^{-6} \Omega \cdot \text{cm}$, which is the saturation value of the change in resistivity vs dpa in the severe neutron radiation environment of TDF. This value has no radial or temperature dependence.

Neutron heating also varies in going from the inner to the outer layer. Fig. 3-47 shows the neutron heating rate as it changes with the radial location of the conductor.

The following are values for the inner, one-fourth, middle, and outer layers in W/cm^3 :

- Inner: 10,
- One-fourth: 3,
- Middle: 2,
- Outer: 1.

The velocity of water was constrained to 12 m/s, based on an assessment of maximum practical velocities and cavitation limits. Corrosion/erosion of a copper pipe flowing water at 12 m/s continuously for one year is limited to a depth of no more than 70×10^{-6} m. Further experimental work may allow this constraint to be relaxed.

Although detailed configurations for the power buses are not available yet, the resistive heating calculation must include the bus length and cross section. The buses were taken into account by adding an effective length to the length of conductor. An arbitrary value of two meters was chosen.

Figure 3-46. Resistance change as a function of radial location of conductor.

Figure 3-47. Heating rate vs radial location of conductor.

To account for possible increased thermal resistance caused by the formation of a copper-oxide scale, a fouling coefficient was included in the computation of the film coefficient. The added resistance was assumed to be the result of conduction across a copper-oxide surface film. There is very little data on the thermal conductivity of copper-oxide or on the thickness of oxide that may form. The thermal conductivity is assumed to be around 6 to 9 W/m²·K. Knowing the electroplating and other coating means can only achieve a thickness of one-half mil, we assumed that a worst-case thickness would be 0.5 mils. An equivalent film coefficient h_{cr} was calculated.

$$h_{cr} = \frac{6 \frac{W}{m^2 \cdot K}}{0.0005 \text{ in} \cdot \frac{1 \text{ ft}}{12 \text{ in}} \cdot \frac{1 \text{ m}}{3.281 \text{ ft}}} \quad (3-13)$$

$$= 470 \times 10^3 \frac{W}{m^2 \cdot K} .$$

To understand how important this term is, we can compare it to the film coefficient for the coolant water, which is approximately $30 \times 10^3 \text{ W/m}^2 \cdot K$ at the TDF service conditions. So, the water film coefficient is 1/16 of the fouling coefficient. The overall heat-transfer coefficient U is as follows:

$$U = \frac{1}{\frac{1}{h_{water}} + \frac{1}{h_{cr}}} . \quad (3-14)$$

Typical values of U are $30 \times 10^3 \text{ W/m}^2 \cdot K$.

Results and Discussion. Power includes joule heating and pump work. The inner layer operates at a power of less than 1 MW, and the outer layer operates at a power of 2 MW. The outer layer takes more power than the inner layer because of its greater length, which more than makes up for its lower resistivity. Total power for all layers is 31 MW. The operating window is constrained from the right by the 12-m/s maximum velocity and from the left by the 200 °C peak conductor temperature. The reduction in the operating window is shown by the bands on each of the lines that reflect margins required to account for uncertainties in film coefficient, resistivity, current density, and mass flow rate. A 10% uncertainty in film coefficient is

allowed for by constraining the peak conductor temperature to only 180 °C. A 10% uncertainty in resistivity and 2% uncertainty in current density result in the inner and outer layer power curves shifting up 14%. A 5% uncertainty in flow requires constraining the velocity to 95% of 12 m/s.

Given the velocity and conductor temperature constraints and the bands of uncertainty in resistivity, current density, velocity, and film coefficient, the TDF internally cooled coil is able to operate at a current density of 2800 A/cm² (see Fig. 3-48). The fact that the inner layer has a wider operating window than the outer layer indicates that it could be optimized. Examining Eq. (3-10) of this section we can see that the $J_2 L$ product can be higher and still allow an adequate operating window, i.e., reasonable water outlet and conductor temperatures. Physically, this means that the inner layer can either share its coolant with the second layer (increase L) or it can be operated at higher current densities (increase J). Even though raising the current density will increase the power needed for the first layer, less total power will be needed because the magnet can be made smaller. An added benefit is the lower capital cost of a smaller magnet. On the other hand, if the coolant path of the inner layer is joined with the coolant path of the second and possibly even the third layer, less piping and pumps are needed.

If the curves for the inner and outer layers are extrapolated out to lower and higher velocities, the total power can be seen to first decrease and then increase with increasing velocity, with minimum power occurring over the TDF operating window of velocities between 8 to 12 m/s. The reason the curve has a minimum point is that pump work becomes a more important part of the total work at higher velocities. At lower velocities, the conductor operates hotter and, therefore, has a higher resistive heating.

The operating conditions at the "four corners" of the window are shown in Table 3-20.

Two values of interest are total resistivity " ρ " and the transmutation portion of resistivity " ρ trans". The change in total resistivity from 3.4 to 2.3 (or 3.6 to 2.5) results from the change in transmutation.

Another parameter of interest is the pressure drop, which ranges from 12 psi at the inner layer (for maximum conductor temperature) to 390 psi at the outer layer (for maximum coolant velocity). As discussed previously, the inner layer would be optimized by increasing either current density or length, so the value of 12 psi would be changed. A pressurized system is required to allow for pressure drops of 400 psi.

Figure 3-48. The internally-cooled choke coil concept at 2800 A/cm^2 operated within design constraints.

Table 3-20. Operating parameters at the inner and outer coil layers.

Parameters	Inner layer maximum velocity	Inner layer conductor temperature maximum	Outer layer maximum velocity	Outer layer conductor temperature maximum
L (m)	10	10	36	36
W (MW)	0.7	0.8	1.8	1.9
W_p (kW)	4	0.1	13.7	2.6
Q (MW)	0.9	0.9	1.8	2.0
QN (MW)	0.2	0.2	0.06	0.06
V (m/s)	12	3	12	6
U ($W/h \cdot cm^2 \cdot K$)	3.8	2.2	4.4	3.5
ϕ_p (Pa/psi)	758,500/110	82,700/12	2,689,000/390	896,000/130
ρ ($\mu \cdot \Omega \cdot cm$)	3.4	3.6	2.3	2.5
ρ_{trans} ($\mu \cdot \Omega \cdot cm$)	1.1	1.1	0.0089	0.0089
T_{out} ($^{\circ}C/^{\circ}F$)	75/167	180/356	120/248	200/392
T_c ($^{\circ}C/^{\circ}F$)	70/158	135/275	85/185	125/257
T_{cp} ($^{\circ}C/^{\circ}F$)	90/194	210/410	125/257	210/410

Lastly, the temperatures of the water outlet and the average and peak conductor are of interest. The hottest water outlet temperature is $200^{\circ}C$ at the outer layer for the peak conductor temperature of $210^{\circ}C$. Actually, the peak conductor temperature would only be allowed to reach $200^{\circ}C$. So, the water outlet temperature would be less than that. To avoid boiling and vapor formation at the magnet outlet, a water temperature of $200^{\circ}C$ requires an outlet pressure of 230 pounds/square inch absolute (psia). Combined with the 390-psia drop in the system, TDF will require a peak operating pressure of 620 psia.

3.3. NEUTRAL BEAMS

3.3.1. Overview

The neutral-beam injection systems (NBIS) perform several key functions necessary for the successful and efficient operation of TDF. The diversity of the systems is indicated by the fact that each beam subsystem has a markedly different task. One set of beams (central cell NBIS) is needed to fuel, heat, and maintain the central cell plasma at a level that ensures the desired neutron flux at the test modules. Several beams are needed to "pump out" unwanted, trapped, low-energy ions both in the transition regions and anchor cells (transition-region and anchor-cell pump beams). These unwanted ions would otherwise modify the axial potential distribution in a way that would allow increased axial particle and energy loss. Ion pumping is accomplished by choosing a neutral-beam injection angle and energy such that the incident neutral particle, after charge-exchange with the unwanted ion, can overcome potential and magnetic mirror barriers and leave the system. A fourth set of beams is used to heat and maintain the proper trapped ion population in the anchor cells (anchor sloshing beams).

Because of the different tasks assigned to the central and end cell beams, the problems associated with their design are considerably different. The central cell NBIS design is dominated by the high power requirements of 70 MW absorbed in the plasma. This means that primary attention had to be directed toward efficient beam generation and transport, both to increase the Q-value of the reactor and to reduce requirements on power-absorbing surfaces. In the design reported here, an injector efficiency (trapped power/source power) of 41% was achieved. The neutral beams are grouped in eight beamlines, each containing four sources. Four beamlines, two of which inject tritium and two deuterium, are arranged in a semicircle at each end of the central cell. This geometry allows good interfacing with and the good servicing capability of the choke coils and the shine-through beam dumps.

In contrast, the end cell beam system design, particularly that of the pump beams, is dominated by the topology of the various end cell components, and the power requirements are rather modest. For example, the injection angle of the transition pump beam cannot be made arbitrarily small because of interference with the anchor cell coils. All end cell beam sources can only be positioned at sizable distances from the interaction point. This, together

with the narrow gap allowable in the vacuum chamber surrounding the transition and anchor cell magnets, means that the beams have to be well focused and collimated to prevent excessive power deposition on the magnet vacuum wall surfaces.

While some beamline components are not expected to need servicing during the lifetime of the reactor, others, like the ion sources, as well as the ion and shine-through dumps, will need more frequent repairs or exchanges. Since most of the components will either be activated directly by neutron irradiation or contain dissolved tritium like the dump surfaces, much of the servicing will have to be performed remotely. To facilitate remote handling and servicing, the beamlines are modular. For example, a complete beamline can be removed for servicing or just a single source exchanged, since it will have the shortest lifetime of all the reactor components.

Another design rationale is to minimize development cost. Thus, we use current technology, such as present-day parameters for ion-source current density and beam divergence and maximum power dissipation for particle dump surfaces. Standard configurations like the magnetic separation of remnant ions and neutrals are also used even though such schemes might be more space-consuming than unproven concepts.

Some aspects must rely on, or could benefit from, advancements in neutral-beam technology. Consistent with constant wave (steady-state) operation of the reactor, it will be necessary to develop continuous beam ion sources. The primary source problem, component lifetime, should be solved within the next two years, either with improvement of the Oak Ridge hollow cathode or with development of the TRW rf sources. Extraction grid cooling and mechanical stability must also be evaluated and improved to ensure proper curvature for beam focusing under continuous beam conditions. On the other hand, radiation hardening of the sources is not absolutely necessary, since carefully designed shielding can be installed between the plasma and the source insulators.

There are several areas of the system where further technological development is desirable but unnecessary. As indicated above, improved beam divergence would facilitate beam injection in the end cells and increase the beam fraction deposited in the central cell plasma. It would also allow plasma-source distance to be increased, facilitating the magnetic shielding problem. Changing the species mix in the pump beams to decrease the

number of molecular ions would improve beam pumping efficiency. Finally, it would be desirable to improve the particle beam dumps, both to increase the allowable power density and to find materials or combinations of materials to decrease the diffusion of tritium into the cooling water.

3.3.2. Central Cell Neutral-Beam Requirements

The basic parameters are the desired neutron wall loading and the total available test module surface area. These requirements, i.e. neutron flux and plasma radius and length, together with hot-ion confinement time and ion temperature, determine the neutral-beam voltage and the current that needs to be trapped to maintain the desired fusion reaction rates. Plasma density electron temperatures, as well as plasma and beam density profiles, determine the fraction of the incident beam trapped by the plasma; that is, they determine the basic neutral-beamline parameters of a 64.9-MW beam incident on the plasma. The neutral-beam injection angle represents a compromise between the ion lifetime and a geometrical factor determined by the magnetic-filled coil configuration.

Table 3-21 shows the central cell NBIS requirements for a 1.4-MW/cm neutron load at $R = 0.25$ m and a plasma of 8 m length, confined to a radius of 0.1 m by a 4.5-T magnetic field. The required beam voltage of 80 kV is well within the range of present-day technology and ensures a reasonably large charge-exchange cross section for the primary positive ions in the neutralizer cell. The large trapped beam currents mean that a sizable number of sources have to be arranged around the device without interference with other components.

A redundancy of at least 20% is required because ion source lifetime is estimated, at most, to be of the order of the duration of an average blanket module test.

The environment in which the system must operate gives another set of requirements. The residual neutron flux will determine the insulator lifetime of the ion source. Even more important is the heat deposited by the neutrons in the approximately 200-m² cryopanelis of all beamlines because, for each watt that must be removed from the cryogen, 600 to 1000 W must be expended in the cryoplant.

Table 3-21. Central cell NBIS requirements and design capabilities.

Item	Requirements	Design capabilities
Incident power (MW)	64.9	76
Beam voltage (kV)	80	80
Trapped current (A)	853	1212
Trapping fraction	0.79	0.79
Injection angle (deg)	≥ 65	65
Species	50% D; 50% T	50% D; 50% T
Species mix (%)	80/15/5	80/15/5
Redundancy (%)	>20 (One spare source per three operating sources or one spare beamline per three operating beamlines)	33
Injection points	1/2 power at each end of 8-m-long plasma column	
Lifetime of source insulators due to neutron load (FPY) 10-cm-wide	0.5	1 (polyimide) 1 (Macor with shield)
Cryopanel neutron heat load (mW/cm ²)	<10	1
Maximum B-field at source (G)	≤ 2	1
Maximum B-field at neutralizer (G)	≤ 10	10

The specified limits for remnant magnetic fields at the ion source and neutralizer ensure that the ion beam, and therefore the neutral-beam divergence, remains within specified limits.

3.3.3. Central Cell Beams

Approach. The central cell NBIS requirements are well defined and can be met by using mostly known technologies. Implementing them is not straightforward, however, partly because of cost and because component interface and serviceability have to be considered, but primarily because several components must be evaluated intensively since their intended use goes beyond known parameter ranges for fusion reactor concepts. The reasons can be traced to three new operational conditions: (1) high total incident beam power, (2) steady-state operation, and (3) the neutron flux and magnetic field environment.

Beam Duct. The requirement for a very high power injection and limited access area results in a beam-current density that is very high because of a partial overlap of adjacent beams. This, together with the necessarily long ducts, leads to a substantial beam loss in the ducts that must be made up with increased source current. Particles lost from the beam increase the pressure of the ambient background gas further, increasing beam loss; in principle, complete blockage of the beam can occur. Because the plasma is a strong neutron source, the beam duct also constitutes a source of streaming neutrons; thus, an improper duct design can result in excessive neutron loading of critical components. The beamlines also compete directly with the adjacent field coils for critical space.

Beam Dumps. The high beam power density requires that special consideration be given to both the neutral-beam and ion-beam dumps, which will have to operate at a power dissipation rate close to the state of the art. Careful design is required to achieve reasonable lifetimes in the presence of sputtering and blistering. In addition, gas emanating from the dumps needs to be pumped efficiently to prevent beam losses resulting from increased background pressure both in the beam duct and in the beamline area. Finally, the shine-through neutral-beam dumps need to allow easy servicing while not interfering with field coils and blanket modules.

Ion Source. Ion sources have reached a state of development that allows them to deliver the required current densities for several seconds, so more development is needed for steady-state operation. In addition, the IDF plasma is an intense neutron source. Because the beam ducts provide an excellent path for neutron streaming, neutron damage to key components, especially to the source insulators, has to be evaluated carefully.

Cryopanel. The tritium and deuterium gas emanating from the neutralizer and ion dumps has to be pumped steadily by cryopanel. Therefore, an operational mode must be developed to allow routing outgassing. Another concern is neutron heat deposition in the cryopanel. To avoid this, the panels must be located so as not to intercept streaming neutrons.

Magnetic Shields. To limit divergence growth of the neutral beam, the stray magnetic field at the ion source and neutralizer must be reduced to the values indicated in Table 3-21. Two types of shields are possible, inactive (magnetic material) and active (superconductor). Both types were evaluated in terms of weight, effectiveness, and engineering feasibility.

Central cell NBIS components that fulfill the requirements in Table 3-21 can be arranged several ways. To show that the design reported here has the required parameters and is optimized, we discuss the rationale used to choose one particular design.

An axial and azimuthal cross section of the NBIS configuration is shown schematically in Fig. 3-49. The 8-m-long plasma is surrounded by solid material of considerable thickness, consisting of neutron shields, test blankets, and magnetic-field-producing coils. Ducts are inserted through this structure to allow neutral beams to pass from the source into the plasma. The beam axis is inclined at 65 deg with respect to the plasma axis. Because the plasma radius is small compared to the source dimensions, it is obvious from Fig. 3-49a that, in the plane perpendicular to the plasma column, the neutral beam should be focused geometrically onto the plasma column and should have a minimum source divergence in that direction.

In contrast, the optimum focusing conditions in the plane containing the plasma axis (Fig. 3-49b) is not self-evident, and any arrangement seems feasible from the view of the beam-plasma footprint. On the other hand, the specific IDF environment of high neutron flux from the plasma column imposes substantial requirements on the neutron shielding:

Figure 3-49. Neutron-streaming considerations.

- The neutron flux on the ion source insulators should be low enough to ensure a reasonable insulator lifetime.
- The power deposited by neutrons in the cryopanel surrounding the beamline should remain at a tolerable level.

The most effective way to limit the neutron loading of the source components is to restrict the duct width close to the plasma surface in Z , i.e., parallel to the plasma axis, thereby minimizing the plasma seen by the neutron-sensitive components. This minimum width, on the other hand, is determined by the size of the neutral beam, which, in turn, is given by the beam divergence and the focusing in that plane. It follows, therefore, that the neutral beam should be focused onto the plasma and should have a minimum beam divergence in the Z -direction, as well as in the θ -direction.

Although a narrow beam duct seems optimal from the view of neutronics considerations, other aspects influence duct design. The most prominent of these is the possibility of beam blockage. The gas density inside the duct is given by the balance of pumping speed, which is determined by the duct geometry and external pressure, and by the gas entering the duct; this gas comes primarily from the walls and results from outgassing. This outgassing can be enhanced by wall heating from neutral-beam particles. The shine-through beam dumps are another gas source, where that fraction of the neutral beam not absorbed in the plasma is captured and converted into thermal particles. The neutral-beam particles ionized by the gas within the duct are removed along the magnetic field lines onto the duct wall. This beam loss must be made up by increasing the source current because the current incident on the plasma should remain the same. This, of course, will increase capital and operating costs.

In addition to fractional beam loss, which will always be present to some degree, an improper duct design can lead to beam blockage. The particles lost from the beam by ionization will recombine at the duct surface, increase the gas density, and lead to a higher beam loss that, in turn, can cause gas density to increase exponentially in time until complete beam blockage occurs. This problem is of particular concern for TDF because of the requirement for constant wave operation and because of the large beam currents and current densities involved.

To prevent beam blockage and ensure minimum beam loss in the duct, conductance to the cryopanel pumps located above the duct should be

maximized. This can be accomplished in two ways: (1) In the plane containing the Z-axis, the source end of the duct should be widened while keeping the plasma end narrow to limit the neutron flux and (2) In the θ -plane the duct can be wide from the view of neutronics because the angle that determines the neutron flux onto the source components is given by the plasma radius, not by the duct width. It is also evident that a wide duct in θ allows the side-by-side grouping of several individual beams.

The result is that the beams should be focused onto the plasma and should have minimum divergence both parallel and perpendicular to the plasma axis. The beam duct should also be V-shaped in both planes, with a wide opening at the source end, as indicated in Fig. 3-50. The upper width of the duct should be large enough for good pumping speed, but narrow enough so that the cryopanels can, for neutronics reasons, be positioned outside the line of sight from the plasma.

Figure 3-50a shows the beam ducts between the superconducting S-1 coil and the normal conducting choke coil. Sufficient space has to be provided for the coolant manifold of the choke coil and the vacuum cryostat of the S-1 coil. The vacuum cryostat must be provided with enough neutron shielding to keep the resistivity increase of the copper stabilizer within prescribed limits. To provide sufficient space while preserving the plasma column length at 8 m, the three-coil neutral cell concept was adopted.

As detailed in Section 3.3.1, the total beam current required to be incident on the plasma could best be achieved by placing at each central-cell end four beamlines containing four sources each. This arrangement also meets requirements for at least 20% redundancy. As shown in Fig. 3-51, these beamlines were not placed symmetrically around the plasma, but were grouped in one-half plane for the following reasons:

- The average path length between the beam dumps and the beam duct opening is increased, decreasing the probability of gas entering the ducts from the dumps and increasing the effectiveness of halo pumping.
- Since one-half plane is free of beamline vacuum vessels, enough space is available for access to both the beam dump and choke coil. This way both can be modular, as indicated for the beam dump in Fig. 3-50a. The accesses for the beam dump and choke coil modules are azimuthally offset from each other.

Figure 3-50. Central cell beam-duct geometry.

Figure 3-51. Central cell neutral-beam and beam-dump configuration.

The grouping of components within one beamline is drawn schematically in Fig. 3-52. The concepts used are standard. A reflection magnet separates the mixture of neutral-beam particles and ions emanating from the neutralizer. This means of separation, compared to the straight line electrostatic separator with energy recovery, was chosen because it represents a well-developed concept. Also, the baffle, together with the reflection magnet, conveniently separates the vessel into high-, intermediate-, and low-pressure sections. The magnet and additional shield material serve as a neutron absorber, providing additional neutron protection for the rear cryopanel and the ion source components. Continuous cryopumping is effected by cyclically outgassing the cryopanel sections (see Section 3.3.6).

The ion sources are expected to have a shorter lifetime than other TDF system components; therefore, gate valves between the neutralizer and sources allow easy source replacement. In contrast, no gate valves should be needed in the beam duct to separate the main system from the beamline components. Gate valves of the required size are cumbersome, bulky, and costly. Also, the lifetimes of beamline components, other than the ion sources, can be expected to be long enough that servicing and maintenance can be scheduled to coincide with TDF servicing or other repair work. The whole beamline assembly can be removed from the vacuum envelope, facilitating beamline servicing.

Component Design

Ion Source. One of the key parameters of the TDF device is the total neutral-beam power of 64.9 MW incident on the central cell plasma. A total current of 853 A is specified to be trapped in the plasma, with beam energy set at 80 kV.

Assuming that the neutralizer efficiency, beam losses, deposition efficiency, and plasma deposition efficiency are known, the total ion beam source current required can be calculated from the required trapped neutral-beam current.

$$I_{s,tot} = \frac{I_{p,tot}}{(1 - \epsilon)_T \sum_{k=1}^3 k^n p_k^n n_{Nk} f_k} \quad (3-15)$$

Figure 3-52. Arrangement of beamline components.

The following notation is used:

$I_{p,tot}$ = total current trapped in central cell plasma,
 $I_{s,tot}$ = total source current,
 k = species index [$k = 1$ for $(D,T)^+$, $k = 2$ for $(D,T)_2^+$, $k = 3$ for $(D,T)_3^+$],
 f_k = species fraction of the beam,
 η_{Nk} = neutralizer efficiency for k -th species,
 η_{Pk} = efficiency of deposition into plasma for k -th species,
 η_T = beam-transport efficiency (from end of neutralizer to plasma)
 i.e., $\eta_T = 1 - \text{reionization loss} - \text{collimation loss} - \text{duct loss}$,
 ϵ = losses unaccounted for.

with

$\eta_{N1} = 0.62,$ $\eta_{p1} = 0.75,$
 $\eta_{N2} = 0.82,$ $\eta_{p2} = 0.80,$
 $\eta_{N3} = 0.87,$ $\eta_{p3} = 0.80,$
 $\eta_T = 0.875$ (2.5% loss at collimators, 2.5% reionization loss in
 beamline, 7.5% loss in drift-duct),
 $f_1 = 0.80,$
 $f_2 = 0.15,$
 $f_3 = 0.05,$
 $\epsilon = 0.10,$
 $I_{p,tot} = 853 \text{ A},$

then the total ion source current required is $I_{s,tot} = 1618 \text{ A}$.

TDF ion sources have to operate in continuous mode. Since the long pulse ion source at Oak Ridge National Laboratory (ORNL)^{10,11} is presently closest to this requirement, it is the chosen baseline source. This source produces an ion beam of up to 200 keV, 100 A with 10- to 30-s pulse lengths. It has been determined¹² that, by reducing the number of grids from four to three and with minor design, such a source could be used in the cw mode while extracting up to 100-A beams at 80 keV. For TDF use, the current for an individual source will be set conservatively at $I_s = 70 \text{ A}$. This means that

24 sources are needed to satisfy the $I_{s,tot} = 1618 \text{ A}$ requirement. Because of the expected short source lifetime, a source redundancy of at least 25% is required to avoid blanket test interruptions by source failure. To provide redundancy and allow a convenient source grouping, the total number of sources was set at 32. For engineering, fabrication, and maintenance, it is desirable to group several sources together and to combine all of their associated beamline components within one common vacuum envelope. These sources then share the same cryopanel, drift duct, reflecting magnet, shields, and other components or subsystems, thereby substantially easing construction and maintenance.

As shown in Fig. 3-53, four sources are sharing one beamline. Four of these beamlines are positioned at each end of central cell. This allows redundancy to be arranged in two ways: (1) one source in all beamlines is spare; (2) in three beamlines all four sources operate, while one complete beamline is unused.

As discussed in Section 3.3.3, the beams of each beamline have to be focused onto the same point of the plasma for optimum power deposition into the plasma and for neutronics reasons. Because this results in a beam overlap, specific considerations must be made for beam-duct blockage (Section 3.3.4) and for shine-through dumps on the other side of the plasma (Section 3.3.5). The issue here is whether the divergence of the beams is low enough and whether the source grids can be properly shaped to ensure the required focusing.

That the accelerator grid structure of the source greatly affects the optical quality of the ion beam grid design is well known. In one design, the grids are constructed with elongated apertures (slits). The advantage of this design is that the source can be easily focused by bending the grids independently in two directions: (1) parallel and (2) perpendicular to the grids. Such grid structures can usually be finely tuned during source testing. The difficulty, however, is that individual beamlets are somewhat defocused in the Y-direction, although the source as a whole is well focused. The defocusing, weak focusing, or divergence as it is sometimes called results from plasma meniscus at the extraction grids. This meniscus is rather shallow and has slow depth variation parallel to the slit, causing increased beamlet divergence in that direction, as illustrated in Fig. 3-54. The beam

Figure 3-53. Beamline layout.

Figure 3-54. Extraction grid with slits. Entries constitute present technological limits.

divergence in the X-direction is given by the thermal ion energy distribution of the source plasma. The second grid design uses circular apertures. Not only is the plasma meniscus deeper at the extraction grid, enabling better focusing, but the resulting beamlet divergence is isotropic (see Fig. 3-55).

The circular aperture grids, however, have disadvantages. The grid structure is somewhat rigid and therefore harder to adjust. These grids have lower optical transparency than grids with slits; consequently, their overall area has to be increased to accommodate the same current. In addition, water cooling channels are more difficult to construct.

For spatial and neutronics reasons (Section 3.3.7), the beam duct has to be narrow in the direction of the plasma axis at the plasma end. This duct has poor optical transmission for a divergent beam, since a large fraction will be intercepted by the duct wall. In addition, the plasma radius is small. This means that a source with the smallest possible divergence in both X- and Y-direction is required. Therefore, a source with circular aperture grids was selected as a design standard. The divergence limit was conservatively set at 0.5 deg, rather than 0.4 deg, the technological limit.

Assuming $f_x = 950$ cm source distance from plasma and $v = 0.5^\circ$ source divergence, the beam/plasma footprint will be 8.3×8.3 cm (Fig. 3-56). The parameters of the TDF standard ion source are assembled in Table 3-22.

The ion source has the shortest lifetime of all beamline components. It is thus necessary to redesign the ORNL source to facilitate easy access and replacement without impairing the vacuum integrity of NBIS. For this purpose, an isolation valve will be installed at the interface of each source with the back wall of the NBIS vacuum vessel. Such an arrangement will enable independent servicing of each source.

When new or overhauled sources are installed on a NBIS unit, fine adjustments will have to be made to direct the beam precisely at the plasma column. The entire source will not have to be moved, as is often done; rather, the orientation of the accelerator grid will be adjusted. The Oak Ridge source has such an arrangement, where the acceleration grid structure is suspended in a gimbal that is controlled remotely by stepping motors (not shown). During the adjustment cycle, the beam is fired from the source in short pulses and its profile/position is monitored on shine-through beam dumps using an infra red camera. This method may be also used to check the quality

Figure 3-55. Extraction grid with circular apertures. Entries constitute present technological limits.

Figure 3-56. Beam focusing.

Table 3-22. TDF standard ion source.

Parameter	Symbol	Value
Extracted current (A^+)	I_s^+	70
Particle energy (keV)	E_0	80
Species mix, $D^+/D_2^+/D_3^+$ (%)	--	80/15/5
Power delivered (MW)	P_s	5.6
Number of grids	--	3
Grid size (cm)	$2x_0 \times 2y_0$	40 x 16
Apperture diameter (cm)	a_a	0.2
Beam divergence (deg)	θ	0.5
Focal length (cm)	f_x, f_y	950
Pressure (Torr)	P_s	5×10^{-3}
Grid transparency (%)	t_g	45
Current density (Acm^{-2})	j_s	0.25

of source focusing. While focusing is usually nonadjustable in situ, some adjustment is possible by shimming the grid structure at assembly time.

Ion sources contain a number of electrical insulation components, usually made of ceramics, Kapton, Mylar, Polyimide, or epoxy and vacuum seals using elastomer O-rings. All of these components will be exposed, to some degree, to radiation effects that might degrade them and render them ineffective. Thus they must be made of materials that can survive the radiation environment. Special shielding around the interface flange and within the source itself will be used to reduce the requirements to reasonable levels.

Neutralizer. The ion beam emerging from the acceleration grid structure of the ion source is transformed into a neutral beam by charge-exchange collisions of ions with neutral gas atoms. To keep this gas target density high and the gas load onto the beamline cryopanel low, the gas is contained in a low-conductance rectangular duct tightly surrounding the ion beam. The length of this neutralizer (Fig. 3-57) is chosen to provide an optimum target thickness π .

A typical ion source has a gas efficiency of 50%. The unused gas streams along with the ion beam through the grid structure into the neutralizer, where it becomes part of the gas target. Depending on neutralizer duct conductance and the required target density, additional gas might have to be introduced into the neutralizer. Since the neutralizer is made as narrow as possible and since the ion beam has a finite divergence, a certain fraction of the beam will be intercepted by the walls. Water cooling, therefore, is required.

The neutralizer length is optimized for the major constituent of the beam U^+ (80 keV) and T^+ (80 keV). The neutralizer efficiency for this species can be obtained from:

$$b_{N1} = \frac{D^0(L)}{D^+(0)} = \frac{\sigma_{10}}{\sigma_{10} + \sigma_{01}} \left[1 - \exp \left(- (\sigma_{10} + \sigma_{01}) \int_0^L n(z) dz \right) \right], \quad (3-16)$$

where $D^+(0)$ is the count of ions entering the duct, $D^0(L)$ is the count of neutrals leaving the duct, $\sigma_{10} \approx 2.0 \times 10^{-16} \text{ cm}^2$ and $\sigma_{01} \approx 1.4 \times 10^{-16} \text{ cm}^2$ are the neutralization and ionization cross sections, respectively, and $n(z)$ is

Figure 3-57. Baseline neutralizer tube.

the gas density distribution along the duct. Duct length is determined by requiring a high percentage (here 95%) of equilibrium neutralization under a low excess gas condition. For a duct of nearly uniform cross section, Eq. (3-16) becomes

$$0.95 = 1 - \exp \left(- (\sigma_{10} + \sigma_{01}) \frac{n_{\text{entrance}} - n_{\text{exit}}}{2} L \right). \quad (3-17)$$

The target density is defined as

$$n = \frac{n_{\text{entrance}} - n_{\text{exit}}}{2} L. \quad (3-18)$$

Thus, Eq. (3-17) yields

$$n = - \frac{\ln(0.05)}{\sigma_{10} + \sigma_{01}} = 8.81 \times 10^{15} \text{ cm}^{-3}. \quad (3-19)$$

From the cryopanel pumping speed and the volume and conductances outside the neutralizer, one can estimate the pressure at the neutralizer exit to be $P_{\text{exit}} = 10^{-4}$ Torr. The entrance pressure, P_{entrance} , can be obtained from the following considerations. The gas pressure in the source is $P_s \approx 5 \times 10^{-3}$ Torr so gas will flow into the neutralizer. However, the acceleration grid system constitutes an impedance to the flow. Assume a grid aperture diameter $d_a = 2$ mm. The sequence of apertures on each grid will appear to gas molecules as a tube whose total length is equal to the total grid thickness plus total grid spacing, here about 1.26 cm. Thus the conductance of a single aperture is

$$C_a = 3.81 \sqrt{\frac{T}{M}} \frac{d_a^3}{l_a + 1.1 D} = 0.237 \text{ l/s}, \quad (3-20)$$

where T was taken to be 400 K. But, a grid of dimensions $2x_0 = 14$, $2y_0 = 40$ and transparency $T_G = 0.45$ has a total of

$$16 \times \frac{0.45 y_0 x_0}{\pi d_a^2} = 8021 \text{ apertures}, \quad (3-21)$$

which yields a total grid conductance of $C_G = 1900 \text{ l/s}$.

Denoting a flow rate by Q , the C_G relates to pressure as

$$C_G = \frac{Q}{P_s - P_{\text{entrance}}} \quad (3-22)$$

On the other hand, the duct conductance is given as

$$C_N = \frac{Q}{P_{\text{entrance}} - P_{\text{exit}}} \quad (3-23)$$

So,

$$P_{\text{entrance}} = \frac{C_G P_s + C_N P_{\text{exit}}}{C_N + C_G}$$

If we assume a pressure in Torr and conductance in $\text{cm}^3 \text{s}^{-1}$, a substitution to (Eq. 3-18) yields

$$\frac{2\pi}{L} = \left(\frac{C_G P_s + C_N P_{\text{exit}}}{C_N + C_G} - P_{\text{exit}} \right) \times 3.53 \times 10^{16}, \quad (3-24)$$

since, for a rectangular duct,

$$C_N = 9.71 \times 10^3 \sqrt{\frac{T}{M}} \frac{a^2 b^2}{(a+b)L}, \quad (3-25)$$

where a and b are dimensions of the duct cross section. Equation (3-25) can be converted into a quadratic equation for L :

$$C_G (P_s - P_{\text{exit}}) L^2 - \beta C_G L - \alpha \beta = 0, \quad (3-26)$$

where $\alpha = C_N L$ and $\beta = 2\pi/3.56 \times 10^{16} = 623$. Using $a = 40$ cm, $b = 16$ cm results, the $L \approx 150$ cm.

The final design features a duct that is slightly tapered in the direction of the beam, as shown in Fig. 3-58, with conductance $C_N = 4734$ l/s and optical transmission factor $T_N = 0.996307$. This yields about 19-kW power dissipation there.

Figure 3-58. Neutralizer geometry.

Gas streaming from neutralizer can now be obtained as

$$Q = \frac{C_G C_N}{C_G + C_N} (P_S - P_{\text{exit}}) = \sim 10 \text{ T} - \text{g/s} . \quad (3-27)$$

The final neutralizer parameters are shown in Table 3-23.

Reflection Magnet. The ions contained in the neutral beam that exits the neutralizer carry a sizable fraction of the total power delivered by the ion source. In the case of the TDF central cell injector, about 2 MW of the 5.6 MW delivered by a single ion source are carried by the unneutralized ions. It is necessary, therefore, to dissipate this power in a well-defined manner onto a power absorbing surface. In addition, care has to be taken to pump the gas emanating from these ion dumps efficiently because gas in the path of the neutral beam can lead to reionization and subsequent beam loss.

As shown in Fig. 3-59, the reflection magnet chosen to separate the ions and neutrals is similar in configuration to the Doublet III (Ref. 13) beamline magnet. This arrangement has two advantages: (1) the design is based on a proven concept, and (2) beam loss caused by background gas is minimized. The magnet, together with the baffle, separates the beamline vessel into three compartments. The high-pressure section, in which the gas emanating from the neutralizer and ion dumps is dominant, contains only a short neutral-beam path. The transition section between the reflector magnet and the beam duct is kept at low pressure.

The three major beamline components--source, reflection magnet, and ion dump, together with some characteristic particle orbits--are shown in Fig. 3-59. The minimum separation ΔW of source and ion dump is given by the Larmor radius ρ_1 of the lightest, least energetic ion and the incident beam width W_S as

$$\Delta W = 2\rho_1 + W_S . \quad (3-28)$$

To collect the most energetic ion of Larmor radius ρ_2 on the ion dump, the magnet width W_m has to be at least

$$W_m = 2\rho_2 + W_S . \quad (3-29)$$

Table 3-23. Neutralizer parameters.

Parameter	Value
Entrance size, $a_1 \times b_1$ (cm x cm)	15 x 44
Exit size, $a_2 \times b_2$ (cm x cm)	16 x 36
Length, L (cm)	150
Conductance, C_N (liters/s)	4734
Entrance pressure, P_{entrance} (Torr)	1.5×10^{-3}
Exit pressure, P_{exit} (Torr)	10^{-4}
Gas throughput, Q (Torr-liters/s)	10
Target thickness, π (cm ⁻²)	7.68×10^{15}
Optical transmission factor, T_N	0.996607
Beam power dissipated (kW)	19

Figure 3-59. Ion-dump arrangement.

This shows that the field should be high to keep the magnet size and weight low. On the other hand, the field cannot be made arbitrarily large since ΔW has to remain finite. For source width $W_s = 32$ cm and a source-to-neutralizer distance of $\Delta W = 20$ cm, it follows from Eq. (3-28) that ρ_1 should be 26 cm. Because the lightest, least energetic ion is D^+ at 27 kV or T^+ at 27 kV, a magnet field strength of 0.128 T for the deuterium beams and 0.157 T for the tritium beams is required. The needed magnet width W_m depends on the heaviest, most energetic ion that is still to be directed onto the ion dump (Eq. 3-29). Table 3-24 shows that the relative abundance of the molecular ions and, therefore, the energy carried by them is relatively low. Limiting the ion dump collection to $(D,T)_1^+$ (80 kV) rather than $(D,T)_3^+$ (80 kV) can save magnet length and therefore weight by about the square root of 3.

Figure 3-60 shows the magnet dimensions adopted. Most of the $(D,T)_1^+$ (80 kV) ions and a fraction of the $(D,T)_2^+$ (80 kV) ions will be directed toward the inside of the magnet gap, where they are collected on a low power density dump consisting of water-cooled copper sheets (devious ion shield). With 70 cm, the magnet depth is smaller than the Larmor radius of the $(D,T)_3^+$ (10 kV) ions. The shield, therefore, has to be extended beyond the magnet gap. It should be noted that a step-function field distribution was assumed. The actual magnet is 10 cm wider to accommodate the fringing field. Four beams are located adjacent to each other in one beamline. This makes it possible to construct the reflection magnet as one unit, shown in Fig. 3-60. With the gap dimensions shown, the magnet assembly weighs 9 tons.

The power consumed in the magnet power beamline can be estimated from

$$P = \rho j g L B_0 / \eta \mu \Delta \cdot (\nu_0) \quad (3-30)$$

Here, ρ is the resistivity of copper ($1.7 \times 10^{-6} \Omega \text{ cm}$), j is the usable current density in the conductor (assumed to be 250 A/cm^2), g is the gap width, L is the conductor length, and η is the conductor filling factor (assumed to be 0.6). The magnet parameters are assembled in Table 3-25.

Drift-Duct. Neutral beams are injected into plasma devices through drift-ducts that are usually long and narrow for geometrical and neutron shielding considerations (Fig. 3-61). The major problem encountered with

Table 3-24. Ion species, their relative abundance and Larmor radius in $B = 0.128$ T for D and $B = 0.157$ T for T.

Species D or T	Atomic current (A)	Larmor radius (cm)
$(D,T)_1^+$ (27 kV)	1.13	26
$(D,T)_1^+$ (53 kV)	1.03	37
$(D,T)_1^+$ (80 kV)	20.90	45
$(D,T)_2^+$ (53 kV)	0.14	52
$(D,T)_2^+$ (80 kV)	1.56	64
$(D,T)_3^+$ (80 kV)	0.66	78

Table 3-25. Reflection-magnet parameter for one beamline (four beams).

Species	Field (T)	Weight (tons)	Power (kW)
D	0.128	9	35
T	0.157	9	43

Figure 3-60. Magnet dimensions.

Figure 3-6.. Drift-duct geometry.

these ducts is that some of the beam particles undergo ionizing collisions with thermal gas within the duct and are reflected by stray magnetic fields onto the duct wall. This phenomenon was responsible for significant power loss from the neutral beam in devices such as PLT,¹⁴ PDX,¹⁵ and others.¹⁶ Early analysis of drift-duct physics was done by Riviera and Sheffield¹⁶; later it was modified by Hemsworth.¹⁷

The importance of the development of a good neutral-beam transport model for drift-ducts can be seen from the following considerations. The ions generated via collision from beam particles and transported to the wall will recombine at the surface. This increases the gas pressure in the duct which, in turn, increases the rate of beam particle loss due to ionization. If a stable equilibrium is not reached, the situation can lead to a complete beam blockage.

Drift-duct designs can be examined with Monte Carlo techniques. However, these methods are usually tailored to a particular design and are, therefore, not versatile; only simple cases can be treated effectively. Finally, Monte Carlo analysis is not very cost-effective. Therefore, we focused on an analytical method. Notice below that a reasonably accurate analytical method can, in fact, be constructed. This model was initially created from very general considerations and later refined for TDF.

A number of effects that contribute to beam blockage have been identified. They are of varying importance, depending on fusion device configuration:

Classical Effect. In magnetic confinement devices, the neutral beam is ionized by collisions with background gas, and the ions are removed to the wall by the magnetic field. Thus, it follows that the beam propagating along the Z-axis is attenuated according to

$$P = P_0 e^{-\int_0^L \sigma n(z) dz}, \quad (3-31)$$

where P_0 is beam power at $z = 0$, P is beam power at $z = L$, σ is the reionization cross section, and $n(z)$ is the background (thermal) gas density distribution. The difficulty encountered in developing a beam transport model

is that the density distribution is unknown, unless experimentally obtained. One usually takes $n(z)$ to be constant and assigns it some experimentally obtained value. While this procedure makes it possible for one to estimate beam loss for new designs by extrapolating from existing designs, results obtained by this method are crude at best because $n(z)$ is never constant, usually not even linear and, in fact, strongly a function of duct shape.

The classical effect is the most important one in the TDF configuration and, therefore, forms the basis of the model to be developed.

Culham Effect. In the first neutral-beam injection experiment at Culham in 1973, it was found that the beam loss far exceeded original estimates.¹⁴ The unexpectedly high drift-duct pressure was attributed to the interaction of collisionally ionized particles with the drift-duct wall. It is believed that the bombarding particles "knock off" additional gas molecules from the surface. This gas contributes to local pressure and is responsible for increased losses. Knock-off yields (released particles/incident particles) are generally unknown and their contribution is, therefore, difficult to estimate. However, for continuously operating beamlines, it is clear that the yield becomes unity after some time since no adsorbed gases are left at the surface.

Princeton Large Tokamak Effect. During the testing phase of PLT neutral beamlines, a slow rise (300 ms) in drift-duct pressure was responsible for excessive neutral-beam loss. This increase in pressure was attributed to a gas source within the duct itself: diffusion of gas previously absorbed inside the duct wall material. It is well known that, unless special metallurgical methods are used during the manufacturing phase, most metals will contain appreciable amounts of gases.¹⁸ As the duct wall is heated by particle bombardment, these gases diffuse to the surface and are desorbed from it. A complete diffusion model for this case was devised by Stewart, et al.¹⁹ Because it also requires some inputs that must be obtained experimentally, it is of rather questionable value for any new design, except perhaps as a basis for extrapolation.

Because of the gradual surface clearing and wall outgassing of the continuously operating TDF beamlines, both the Culham and PLT effects will become of minor importance after some operating time. For this analysis,

"knock off" and desorption will be represented by a single constant, r_{wa} (x, y, z), that is the wall outgassing rate in molecules per second at the point (x, y, z).

Beam-Divergence Effect. In most practical beamline configurations it is impossible to design beam collimators (scrapers) so that no point on the drift-duct wall "can see" the source (Section 3.3.5, "Magnetic Shielding Requirements for Low Divergence"). An optical model for the source assumes that each point of the source is an emitter of beamlets directed toward some common point (focus). Each of these beamlets is divergent and has a Gaussian power distribution profile. Clearly, any point of duct wall in direct line of sight with the source is illuminated by it. The far end of the duct represents another aperture to the beam. It is necessary to choose the dimensions of the far end duct opening so that the highest possible optical (ignoring reionization) transmission is achieved. Even in such an optimal case, the fraction of beam lost because of optical inefficiency may constitute a significant gas load on the drift-duct, causing a local increase in pressure there and, consequently, increased beam blockage caused by reionization (enhanced classical effect).

In the following model, the beam-divergence effect is taken into account as follows. Consider a beam passing through a set of two rectangular apertures, each representing one of the drift-duct end openings. Let f_r be the power fraction of the beam that passes through a rectangular aperture of size $2x'$ by $2y'$ (Ref. 20). From preliminary studies, duct openings were sized at approximately 100 x 120 cm and 50 x 70 cm, located at $z = 625$ and 850 cm, respectively. The fraction of the beam lost within the duct is calculated approximately as

$$f(\text{lost}) = f(\text{entrance end}) - f(\text{exit end}) \quad . \quad (3-32)$$

Using previously defined source parameters, namely $\theta_x = \theta_y = 0.5$ deg, $f_x = f_y = 950$ cm and $x_0 = 8$ cm, $y_0 = 20$ cm results in $f(\text{lost}) = 2.5\%$, which is significant. In fact, for a 240-A beam, it represents a 6-A loss. This number is reduced somewhat if a collimator is placed just before the duct entrance, but that is a minor point.

Gas-Plume Effect. From experiments involving neutralizers in various beamlines, we know that thermal gas is ejected from the neutralizer end with approximately Lambertian spatial distribution. Because of the relatively large mean free path for thermal gas within the beamline, the path of ejected gases can be traced using ray optics. It is easy to see that some of this gas impinges on the drift-duct walls. This case is analogous to the beam-divergence effect above, except here the neutralizer is taken as a cosine law "source."

For the duct dimensions above, the fraction of the beam lost is 1.23%. Since the thermal particles ejected by the neutralizer are at about a one-to-one ratio to beam particles, the 1.23% is equivalent to about 0.9 of the particle current. Ignoring this is hard to justify, but one can show that placing a collimator just before the drift-duct entrance can greatly reduce the flow of thermal particles onto the duct wall. In fact, if a 16- x 30-cm collimator is placed 150 cm before the duct, $f(\text{loss})$ becomes virtually zero. Gas-plume effect will, therefore, be ignored in the analysis.

Plasma Generation. As noted earlier, ions and electrons are generated inside the duct as a result of reionization. In the presence of a magnetic field, they are deflected from the beam so that their average velocity relative to the beam is non-zero. Since the lifetime of these particles within the duct (before they recombine at the wall) can be significant, they can interact with the beam. Although it is clear that the net plasma density within the duct is much less than that of the background gas, the charge-exchange $[(1,0) \rightarrow (0,1)]$ collision has a cross section that is one or two orders of magnitude larger than the reionization process. Depending on the size of the beam(s) and the strength and direction of the magnetic field, this may result in appreciable net beam attenuation.

The amount of plasma in the duct can be estimated from particle balance considerations. The parallel velocity of reionized particles is approximately

$$V_{||} = \sqrt{\frac{KT}{M}} \cos \psi, \quad (3-33)$$

where KT is particle energy, M is mass, and ψ (which equals 45 deg) is the angle between the beam and magnetic field. Because, in a magnetic field of several tesla, the Larmor radius of 80-keV ions is just a few centimeters

(small in comparison to the size of the duct), the ions will essentially follow the magnetic lines of force to the wall. Denoting $a = 45$ cm, the average distance from the collision site to the wall, the average ion lifetime in the duct is

$$\bar{\tau} = \frac{\bar{a}}{v_{II} \sin \Psi} = \frac{2\bar{a}}{\sqrt{\frac{KT}{M}} \sin 2\Psi} \quad (3-34)$$

Assuming about a 5% loss of the original approximately 245-A beam within the duct and charge-exchange cross section, $\sigma = 3 \times 10^{-14} \text{ cm}^2$ results in about a 0.1% additional attenuation because of plasma. This is much too low and can be ignored.

Gas Load from Neutral-Beam Dumps. Because of the geometrical configuration of the neutral beamlines at the end of the central cell, the shine-through beam dumps are located close to beamline drift-ducts. A sizable fraction of the neutral beam incident on the plasma is not absorbed and emanates from the shine-through dumps as gas. This gas can enter the beam duct and must be taken into account in the overall gas balance of the ducts.

The expected particle shine-through current for one injector is about 44 A. At the dump, this will turn into 22 A of molecular current. If 5 to 20% of this gas is drawn into the duct, 1 to 4.5 A additional thermal gas has to be pumped out. This effect is substantial and must be included in the analysis.

From the overall injector geometry, it has been determined that four neutral beams will share one duct. The location and orientation of the duct with respect to the reactor coils is shown in Fig. 3-62. As described in Section 3.2, the far end (plasma end) of the duct should be made as narrow as possible to minimize neutron streaming. A small plasma end aperture will also limit the amount of dump gas intake into the duct. On the other hand, the optical transmission of the duct will be reduced and the effect of beam loss by the apertures will be enhanced. A trade-off study is therefore required.

Similar considerations have to be given to other geometrical features of the duct. For example, a minimum amount of shielding is needed between the duct wall and the edge of coil S-1. Because the location of the duct and coil

Figure 3-62. Drift-duct location.

is fixed, shield thickness can be increased (desirable to limit neutron damage to the superconductor) only at the expense of reduced duct conductance (and higher beam loss) and vice versa.

Those geometrical features of the duct that will be used in analysis are defined in Fig. 3-63.

A number of assumptions will simplify analysis. Some of them are not absolutely necessary because the model could be extended to even more general cases, but minor increases in accuracy are not always worth the trouble. The assumptions are:

- The density gradient is always parallel to the duct central (z) axis, i.e., there is no density variation in the plane perpendicular to the central axis.
- The gas inside the duct is at constant temperature everywhere (hence, $T_{\text{gas}} = T_{\text{duct wall}}$).
- An infinite pumping speed exists at the flared end of the duct.
- There is no pump at the narrow (far) end of the duct ($z = L$).
- The wall degassing rate is uniform and constant in time (steady-state).
- The attenuation due to plasma is negligible.
- The gas load due to the neutralizer gas-plume effect is negligible.
- A fraction of the gas generated at the beam dumps will be drawn into the duct.
- A strong magnetic field is present within the drift duct (in case of TDF 1B) = 20 to 50 kg.

Theory. Once all the beam loss mechanisms and gas sources have been identified, flow equations can be used to determine the density distribution inside the duct. The particle balance is depicted in Fig. 3-64.

Gas generated in a cross sectional duct segment of thickness Δz located at an axial distance z is

$$K_g(z) = \text{beam loss} + \text{outgassing} + \text{other sources} \quad (3-35)$$

Figure 3-63. Duct geometry.

Figure 3-64. Physical model.

Conservation principles give the gas flow through that area as

$$R_f(z) = \int_z^L k_g(\zeta) \alpha_\zeta \cdot \quad (3-36)$$

The corresponding gas density can be written as

$$n(z) = \frac{R(z)}{C_\Delta(z)} + n(z - \Delta z) \quad , \quad (3-37)$$

where $C_\Delta(z)$ is the conductance of a duct element of length Δz with a cross section corresponding to the point z . Since the beam loss is related to the gas density, one can write the axial density gradient as

$$\frac{dn}{dz} = \frac{R_{B_0}}{2} \frac{e^{-\int_0^{z'} Q(\zeta) d\zeta} - 1(1 - fK_s) e^{-\int_0^L Q(\zeta) d\zeta} + \int_{z'}^L r_w(\zeta) \alpha_\zeta}{9.71 \times 10^3 \sqrt{\frac{T}{M} \frac{8\xi^2 \eta^2}{\xi + \eta}}} \quad (3-38)$$

where $Q(\zeta) = \sigma\eta(\zeta) + \lambda(\zeta)$, $r_w(\zeta) = 2(\zeta + \eta)r_{wa}$, and $\lambda(\zeta)$ is a view factor defined as the fraction the beam lost at cross section z because of direct source illumination. The other symbols are identified in Fig. 3-54.

Analytic solutions for Eq. (3-38) are difficult to find. If the equation is rewritten in different form, however, it becomes suitable for application of numerical methods. One may proceed as follows: subdivide the duct of length L into l segments, assume initially uniform density (background density), and obtain the density profile for the entire duct by repeated application of Eq. (3-33), working step by step from $Z = 0$ to $Z = L$. This newly obtained profile is now used as a starting point for recalculating the density profile, and the process is repeated. The iteration converges rather rapidly for most configurations; 20 to 30 steps are usually enough to stabilize the output to six significant digits.

Once the density profile $n(z)$ has been obtained, the total transmission factor T_D for the duct is easily computed as

$$T_D = \frac{\text{Power in}}{\text{Power out}} = e^{-\int_0^L \alpha n(z) dz} \quad (3-39)$$

The coefficients f and K_S used in Eq. (3-28) remain to be determined. The shine-through quotient, defined as

$$K_S = 1 - \frac{\text{Power absorbed in plasma}}{\text{Power incident on plasma}} \quad (3-40)$$

is usually known ahead of time from plasma heating calculations. On the other hand, the fraction (f) of dump-generated gas drawn into an adjacent drift-duct has to be obtained from some particle-transport model for the dump-duct region. Ideally, one would assume that gas particles are emitted from the dump with a cosine distribution and then calculate the relevant view factors. From the functional dependence of T_D on f , this complicated computation can be considered superfluous (Fig. 3-65).

Since T_D depends only weakly on f , it will suffice to obtain f by a simpler, but rather crude method of comparing conductances. Consider the configuration in Fig. 3-66, adopted from Drawing No. 1076.

For computation, it can be translated into the following model: the dump is located between two ducts connected end to end, each having a pump on the far end (the plasma is assumed to act as an infinite pump), as shown in Fig. 3-67.

The size and shape of the "plasma duct" is inferred from the following considerations: the smaller aperture is the same size as the plasma halo (assumed to act as an infinite pump), and the larger aperture is equivalent to the distance between two adjacent beam dumps. Assuming $\theta_1 = 6$ deg, $\theta_2 = 18$ deg, $a_0 = 60$ cm, $d_0 = 45$ cm, $L = 300$ cm, $R = 70$ cm, $r = 25$ cm, and duct width (both ducts) $2b \approx 60$ cm, we obtain conductances for the drift duct and plasma duct, respectively: $C_D = 25,000$ liters/s and $C_P = 50,000$ liters/s. We expect, however, that C_D has been over estimated

Figure 3-65. Dependence of T_D on f .

Figure 3-66. Duct-dump relationship.

Figure 3-67. Model illustration.

by about a factor of 3 because the duct intake is not in direct view of the adjacent dump. Using adjusted value $C_D = 8000$ liters/s,

$$f = \frac{C_D}{C_D + C_p} \approx 14\% \text{ (max)} \quad . \quad (3-41)$$

These calculations can be used to determine the beam transmission factor T_D for any given duct configuration. For the initial duct design, we can discuss the functional dependence of T_D on various parameters, particularly duct conductance, which is shown schematically in Fig. 3-68. Notice that appreciable beam loss can occur for small conductances and that complete beam blockage results below some minimum conductance. After a regime in which transmission increases rapidly with conductance, the curve levels off for $T_C \geq 0.95$. This means that a further increase in duct conductance has little effect on transmission.

As discussed in Section 3.3.3, the drift-duct cross section and, therefore, the conductance, cannot be made arbitrarily large. (1) The neutron flux on some beamline components should be minimized to limit heat transfer to cryopanel and to increase source insulator lifetime. (2) A limited space for ducts is available between the magnet coils and their neutron shields. This space and the curve describing the transmission dependence on conductance show a trade between a narrow, low-conductance duct that limits neutron flux but causes appreciable beam loss and a wide duct. The duct design should result in a conductance represented by this design region so that small errors in design, assessment of gas inputs, etc. do not appreciably alter the design transmission coefficient. The parameters of the beam duct finally accepted for TDF are shown in Table 3-26.

The duct walls under particle bombardment are water-cooled to keep them at constant temperature. The wall material will be further treated with vacuum metallurgy methods to lower the content of absorbed gases and other impurities. Order-of-magnitude improvements over untreated material are readily achievable.¹⁸ Quoted results for 304 stainless steel are approximately 1 ppm of H_2 and 20 ppm, for carbon, sulphur, and phosphorus. Using these two assumptions, it is possible to estimate maximum steady-state

Figure 3-68. Dependence of transmission on conductance.

Table 3-26. Drift-duct parameters.

<u>Initial parameters</u>	
Neutral-beam current entering duct, I_0 (A)	56.35
Power of neutral beam entering duct, P_0 (kW)	3436
Number of beams	4
Angular separation of beams, T (deg)	3.31
Source dimensions (cm)	
$2x_0$	16
$2y_0$	40
Source divergence, θ (deg)	0.5
Source focal length, f_s (cm)	950
Front collimator dimensions (cm)	
a_c	8.5
b_c	15
Location of duct entrance, Z_d (cm)	630
Duct wall outgassing rate, r_{wa} ($s^{-1}cm^{-2}$)	1×10^{13}
Reionization cross section at 80 keV d, σ (cm^2)	1×10^{14}
Magnetic field in thermal duct, $ B $ (kG)	~ 20
Temperature of thermal gas, T (deg)	400
Shine-through factor, K_s	0.8
<u>Design parameters (final design)</u>	
Neutral-beam current leaving duct, I_1 (A)	53.2
Power of neutral beam leaving duct, P_1 (kW)	3251
Duct transmission factor, T_D (four beams)	≥ 0.927
Power dissipated in duct, P_{LD} (kW)	740
Duct dimensions (cm)	
a_0	60
b_0	35
a_1	35
b_1	19
L	225
Duct dimensions (deg)	
α	6.34
β_0	4.07

wall degassing. The design of the duct wall is shown in Fig. 3-69. Because such an arrangement will limit wall outgassing, it may be assumed to be constant throughout the entire duct.

Other considerations are sputtering and heat load. Bombardment of the duct wall by energetic particles will cause wall face erosion. The thickness of the cooled shield should be adjusted so that it remains a lifetime component. The total heat generated within the duct is close to 800 kW. This heat load is not uniformly distributed, but varies along the duct and depends on the direction of the magnetic field. Cooling lines have to be carefully laid out to prevent local overheating.

List of Parameters. Table 3-26 shows requirements and final design parameters. They are related to Figs. 3-63 and 3-70.

Power Absorbing Surfaces. Of the 5.6 MW of ion beam power delivered by an ion source, 2.3 MW will be absorbed as neutral-beam particles in the plasma. The rest of the ion- and neutral-beam power will be intercepted and must be dissipated on various surfaces. While the power density on parts like the neutralizer and beam duct walls is not especially high and can be handled with conventional water-cooling, other surfaces like the edges of the beam-edge-defining collimator are subjected to high power density and require special attention. The problem is particularly severe for the ion dumps, which must handle about 2 MW per ion source, and for the shine-through neutral-beam dumps, which receive that part of the neutral beam not absorbed by the plasma. While the total power received per single beam is less than 1 MW, the power density is high because of the partial spatial overlap of the four beams contained in one beamline.

Three main issues must be addressed:

- Maximum power density that can be absorbed safely.
- Lifetime limited by sputtering and erosion.
- Tritium diffusion into the cooling water.

Beam-Dump Lifetime. Beam-dump lifetime is limited by erosion of the front surface produced by sputtering. Beam dumps intended to dissipate high-power heat flux usually have thin front walls to facilitate efficient heat transfer. Since the dump coolant is highly pressurized, only limited erosion of the front wall can be tolerated, usually 10%, but no more than 30%

Figure 3-69. Detail of duct wall.

Figure 3-70. Drift-duct final design.

of the initial wall thickness. Beam-dump lifetime can be estimated easily if the erosion rates are known.

If γ is a sputtering yield (i.e., the average number of particles removed from the first wall for every particle arriving) and j_{inc} is the incident current density, the rate of arriving particles per unit area is then

$$R_a = \frac{j_{inc}}{e} \left[s^{-1} cm^{-2} \right], \quad (3-42)$$

where e is the elementary charge. The wall material particle-removal rate is then given as

$$R_w = \frac{j_{inc} \gamma}{e} \left[s^{-1} cm^{-2} \right]. \quad (3-43)$$

The number of particles per unit volume of wall material is $\rho N/M$, where ρ is density, N is the Avagadros number, and M is atomic mass. Then the erosion rate can be expressed as

$$r_w = \frac{R_w}{\rho N/M} = \frac{j_{inc} \gamma M}{e N \rho} \left[s^{-1} cm \right]. \quad (3-44)$$

Assuming the front wall has an initial thickness d of which maximal fraction f may be eroded, the lifetime of the dump can be written

$$T_{life} = \frac{fd}{r_w} = \frac{fd \sigma N \rho}{\gamma j_{inc} M} [s]. \quad (3-45)$$

So far normal incidence is assumed.

The sputtering yield is a strong function of incident angle. Particles entering the dump material at large angles with respect to the surface normally deposit most of their energy and transfer most of their momentum to substrate atoms near the surface, thereby increasing sputtering yield. To account for this effect, an angle-dependent correction factor $A(\alpha)$ is introduced.

The expression for lifetime then becomes

$$T_{life, \alpha} = A(\alpha) T_{life, 90 \text{ deg}}. \quad (3-46)$$

As seen from Fig. 3-71, $A(\alpha) \approx (\cos \alpha)^{-1}$.

If E_{av} is the average energy of arriving particles and $P_{max}(\alpha)$ the maximum power density to be dissipated by the dump, then

$$j_{inc} = \frac{P_{max}(\alpha)}{E_{av}} \quad (3-47)$$

Thus

$$T_{life}(\text{hrs}) = \frac{A(\alpha) f d e N \rho E_{av}}{3600 T P_{max} M} \quad (3-48)$$

Note that $P_{max}(\alpha) = P_{max} \cos \alpha$. In view of the cosine dependence for $A(\alpha)$, it is clear that there is nothing to choose in terms of α to prolong T_{life} ; i.e. $T_{life} = \text{const}(\alpha)$.

It turns out that the most reasonable choice for front wall material is tungsten. Assuming $d = 2 \text{ mm}$, $\alpha = 60 \text{ deg}$, $f = 0.1$, $p = 2 \text{ kW/cm}^2$, $E_{av} = 80 \text{ keV}$, $M = 183$, $\rho = 16.6 \text{ g/cm}^3$, and $\gamma = 4 \times 10^{-4}$, the resulting dump lifetime is 2425 hours, i.e., 101 full power days. This is the equivalent of about one calendar year. Such a lifetime is quite reasonable.

Because tungsten is a high-Z material, the total sputtering rate into the plasma becomes of great concern. Assuming a total neutral-beam current onto the plasma per machine, $I_0 = 1280 \text{ A}$, and roughly a 25% shine-through factor, we have a total sputtering rate per machine and

$$S_{total} = \frac{I_0 K_s}{e} T = 9 \times 10^{17} [\text{s}^{-1}] \quad (3-49)$$

This is considered to be well below the dangerous threshold; hence, using tungsten will not increase Bremstrahlung radiation.

Figure 3-71. Variation of sputtering yield with angle of incidence for H⁺, D⁺ on Mo. The dashed line indicates a $\cos^{-1}$ dependence.

Power Density. The purpose of this section is to determine the feasibility of ion and shine-through dumps. The input is the set of source parameters (Section 3.3.1), and the output is the dump inclination angle required to limit power density at the surface to less than 2 kW/cm^2 . Because this alone is not sufficient for the ion pump, other methods like beam defocusing are considered.

The shine-through dump is easiest to analyze. In the model used, four beams are directed onto the plasma, and the shine-through beams overlap on a single, common dump, here just a flat plate (Fig. 3-72). This configuration is used for each neutral beamline. The power density distribution for each of the four beams is obtained by using a formula developed by Pittinger.²⁰ Beam absorption in the plasma varies as a function of plasma radius. Dependence on x , the beam local coordinate, is shown in Fig. 3-73.

Define a shine-through coefficient

$$K_S(x,y) = 1 - \text{absorption}(x,y) \quad . \quad (3-50)$$

Then the shine-through power distribution for each beam is given by

$$P_{ST}(x,y,z) = P_N(x,y,z) K_S(x,y) \quad . \quad (3-51)$$

where P_N is the power density of the incident neutral beam. Now there are four beams incident on a dump located at distance d_0 from the plasma and inclined at angle α with respect to the plasma axis (see Figs. 3-74 and 3-75).

For a beam with angular displacement δ ,

$$x = (\xi - d \tan \delta) \cos \delta \quad , \quad (3-52)$$

$$y = \eta \cos \alpha \quad ,$$

$$z = z_f + \frac{d}{\cos \psi} \quad ,$$

Figure 3-72. Beams intersecting plasma.

Figure 3-73. Beam absorption in plasma vs plasma radius ($\equiv$ local coordinate X).

Figure 3-74. Beam vs dump geometry in X-Z plane.

Figure 3-75. Beam vs dump geometry in Y-Z plane.

and

$$d = d_0 + y \sin (\alpha + \psi) .$$

Then, in the (ξ, η) coordinate system of the dump, the power density distribution is

$$P_{ND}(\xi, \eta) = \left(\frac{\cot^2 \delta + \cot^2 \alpha}{\cot^2 \delta + \cot^2 \alpha + 1} \right)^{1/2} \sum_{\text{beams}}^4 P_{ST}(x, y, z) . \quad (3-53)$$

For small δ 's, this can be simplified to

$$P_{ND}(u, B) = \cos \alpha \sum_{\text{beams}}^4 P_{ST}(x, y, z) . \quad (3-54)$$

A computer plot of this result for the TDF beamline is shown in Fig. 3-76. It relates to the dump quadrant shown in Fig. 3-77, which represents the case with the highest power density. The incidence angle is $\alpha = 75$ deg.

The power density everywhere is less than 2 kW/cm^2 and, except for a few hot spots, is less than 1 kW/cm^2 throughout most of the surface. Note that the incidence angle α that was previously assumed to be 60 deg must be increased to at least 75 deg, shortening dump lifetime from 110 to 52 full power days. To prolong dump life, it is necessary to increase either the front wall thickness or the allowable erosion.

The situation for the ion dump is somewhat different. The ion beam is first reflected by a magnet before it impinges onto the dump. Each of the four beams within a beamline has its own ion dump. For this analysis, it is sufficient to assume initially that the reflection does not affect any beam parameter except perhaps direction and lateral displacement. Hence, it is possible to assume that the dump is placed a distance z_{id} from the source and that the power density distribution is calculated using

$$P_{id}(x, y) = P_{i, \text{tot}}(x, y, z_{id}) \cos \alpha , \quad (3-55)$$

where $P_{i, \text{tot}}$ is the incident ion power-density distribution and α is the incidence angle. To bring P_{id} under the 2-kW/cm^2 limit everywhere

Figure 3-76. Power-density distribution on dump face (one quadrant).

Figure 3-77. Plotted quadrant location.

requires that α be rather large, about 85 deg. That, however, is not acceptable because of the drastically shortened dump lifetime. The problem can be resolved, however, by taking advantage of the reflection magnet. Magnetic defocusing of the ion beam would yield reduced power densities in the dump area, and such an approach is indeed suitable for the NBIS. The defocusing model presented uses strictly geometrical optics and ignores the fringing field of the magnet.

Consider a magnet with pole pieces slanted at angle Ω where the ion beam exits (Fig. 3-78).

Let w be the displacement from the center of a Gaussian beam where power density drops to $1/e$ of its peak value. Mark the center of the beam as a ray C or C', rays displaced from C by w , and by y as A, A' and B(B'), respectively. The sloped pole pieces are designed so that the reflected ray A' is unaffected by defocusing (Fig. 3-79).

The task is to calculate the power density at the pump located a distance z_{id} from the source (measured along ray C'). Since the separation of the dump and the magnet is z_{md} and that of the source and the magnet is z_m then, for small Ω ,

$$z_{id} = z_m + z_{md} + \pi r_L (D^+) , \quad (3-56)$$

where $r_L (D^+)$ is the Larmor radius of D^+ specie. Because of the large focal length of the source, the convergence of rays A, B, and C may be ignored. To locate a given ray on the dump it is necessary to find ϕ . First,

$$\tan \Omega = \frac{u_0}{2r_L + y - q} , \quad (3-57)$$

where

$$\begin{aligned} q &= r(1 + \cos \rho), \\ u &= r_L \sin \phi. \end{aligned} \quad (3-58)$$

Figure 3-78. Defocusing of ion beam.

Figure 3-79. Defocusing model.

Substituting these into the equation

$$r_L^2 = u_0^2 + r_L^2 \cos^2 \rho, \quad (3-59)$$

yields the quadratic equation

$$K^2 - 2KS^2 \left(1 + \frac{y}{r_L}\right) + S^2 \left(1 + \frac{y}{r_L}\right) + S^2 - 1 = 0, \quad (3-60)$$

where $K = \cos \phi_0$ and $S = \sin \Omega$. Solving for K results in

$$K = 1 + \frac{y}{r_L} \left[S^2 \pm \sqrt{S^4 - S^2 + \left(1 - S^2\right) \left(\frac{r_L}{r_L + y}\right)^2} \right]. \quad (3-61)$$

Let (ξ, η) be a coordinate system of the ion dump with origin on ray A' .

Then

$$P_{id}(\xi, \eta) = P_{i, \text{tot}}(x, w-y + z_{md} \sin \rho) \cos \alpha \quad (3-62)$$

$$= P_{i, \text{tot}} \left(x, w-y + z_{md} \sqrt{1 - K^2} \right) \cos \alpha,$$

where α is again beam incidence angle onto the surface. A plot of $P_{id}(\xi, \eta)$ for $\alpha = \text{TBD}$ is shown in Fig. 3-80. Note that everywhere $P_{id} \leq 2 \text{ kW/cm}^2$ as required.

Collimators. There are two sets of collimators in each beamline--front and rear (their locations with respect to the plasma, shown in Fig. 3-81).

Collimators clip the edges (wings) of the beam that would otherwise impinge on the beam duct, causing excessive heating and increasing the gas load. In essence, each collimator is an aperture with well-cooled edges that trim the beam to the desired profile. A major constraint in collimator design is the incident power density to which the edges may be exposed. This number is usually under 2 kW/cm^2 .

The rear collimator clips the beam before it enters the magnet yoke. It is estimated that the collimator edges will be exposed to 1000 W/cm^2 power density with an aperture size of $15 \times 35 \text{ cm}$ (Fig. 3-82).

Figure 3-81. Collimator locations.

Figure 3-82. Rear collimator.

The transmission factor is defined as the fraction of the incident power passing through the collimator. It can be calculated from Ref. 8,

$$T_{RC} = \frac{z_{rc}^2 \theta^2}{4 x_o y_o} \left(A \operatorname{erf}(A) + \frac{1}{\sqrt{\pi}} e^{-A^2} - B \operatorname{erf}(B) - \frac{1}{\sqrt{\pi}} e^{-B^2} \right)^2 \quad (3-63)$$

$$\times \left(C \operatorname{erf}(C) + \frac{1}{\sqrt{\pi}} e^{-C^2} - D \operatorname{erf}(D) - \frac{1}{\sqrt{\pi}} e^{-D^2} \right),$$

where

$$A = \frac{g x_o + a_r}{z\theta}, \quad D = \frac{h y_o - b_r}{z\theta}, \quad (3-64)$$

$$B = \frac{g x_o + a_r}{z\theta}, \quad g = \frac{z}{f_y},$$

$$C = \frac{h y_o + b_r}{z\theta}, \quad h = \frac{z}{f_y}.$$

The power lost at the rear collimator is given by

$$P_{Lrc} \approx P_s (T_N - T_r) = 5600 \times 5.197 \times 10^{-3} = 29.15 \text{ kW}.$$

The gas generated is equivalent to 3.21×10^{-5} Torr-liters/s.

The parameters that influence the design of the front collimator are different from those that influence the rear collimator. Ideally, the collimator would pass only that fraction of the beam that is directed onto the plasma and block everything else. However, because the aperture is located an appreciable distance from the plasma (as a result of beamline geometry) and source size is finite, it is apparent from Fig. 3-83 that this ideal situation cannot be achieved.

It turns out that attempts to reduce x_{pmax} (Fig. 3-83) result in a loss of beam reaching the plasma. A careful trade-off between these two effects

Figure 3-83. Front collimator.

was required, and the final design for the front collimator yielded an aperture size of 17 x 30 cm (Fig. 3-84).

The transmission factor (Eq. 3-64) associated with these dimensions is $T_{fc} = 0.977702$. Consequently, the power lost at the collimator is

$$P_{Lrc} = P_{N_{tot}} (T_{rc} - T_{fc}) = 41 \text{ kw} , \quad (3-65)$$

while the equivalent gas load is 4.26 x Torr-liters/s.

Another collimator is located in the beamline, namely the ion collimator that trims the reflected ion beam incident on the ion dump. Its sole purpose is to keep any of the beam from falling outside the dump area. The design of this collimator is not critical. If ion beam defocusing is ignored, its size is 17 x 29 cm; with defocusing, it will increase to about 17 x 40 cm.

Tritium Diffusion. In Section 3.3.1 we showed that four of the eight beamlines inject tritium. Energetic tritons will be implanted in both the ion and shine-through dumps. The diffusion of the implants into cooling water and their accumulation could become a significant radiological hazard. Tritium recovery from water is a rather costly process, but it may be required. In this section we attempt to determine the magnitude of the problem.

The following model was used in the diffusion analysis. The beam dump is represented by a flat wall of thickness t (Fig. 3-85), with a vacuum on one side and a pressurized coolant (H_2O) on the other. A beam (ion or neutral) of current density j_{total} impinges on the vacuum side of the dump at an incidence angle of α .

Although the situation for both the ion and shine-through dumps is postulated to be essentially the same for the purposes of this analysis, the ion dump has a larger area (it absorbs twice the power) than the shine-through dump. As a result, only the shine-through dump needs to be treated.

Diffusion is a temperature-dependent process. Starting from the power density distribution plot in Fig. 3-80, it is possible to determine the temperature profile in the wall.

$$T(\xi, \eta, \zeta) = T_c + \frac{\xi}{\lambda} P(\xi, \eta) , \quad (3-66)$$

where (ξ, η, ζ) is the coordinate system of the dump, λ is the thermal

Figure 3-84. Line-of-sight optics in X-plane. Y-plane optics analogical.

Figure 3-85. Diffusion model.

conductivity of the wall material, and T_c is the wall temperature at the coolant side.

The current density distribution is given by

$$j_{\text{total}}(\xi, \eta) = \frac{P(\xi, \eta)}{E_{\text{av}}} = 1.587 \times 10^{-5} P(\xi, \eta) [\text{A cm}^{-2}], \quad (3-67)$$

and the energy spectrum of the beam particles used is D^+ (61% at 80 keV), D_2^+ (25% at 40 keV) and D_3^+ (14% at 27 keV). Finally, it was assumed that the plate material is AMZIRC (98% copper) and that temperature T_c is $\sim 150^\circ\text{C}$.

These parameters were used as input for a diffusion computer code²¹; on the basis of these data, the tritium leakage rate into the cooling water was found to be approximately 4.8 mg/day per shine-through dump. Because there are 4 shine-through and 16 ion dumps for tritium beams and the ion dumps absorb twice the power, the total tritium leakage rate for the central cell beam injector dumps is 57.6 mg/day, or 21 g/FPY. This figure is significant enough to require continuous tritium recovery from the cooling water.

Vacuum System

Approach. To limit beam losses resulting from reionization and scattering, the background gas density along the beam path must be kept at the lowest pressure attainable. This situation is complicated by the presence of several gas sources within the vacuum envelope of the injector, namely beam dumps, collimators, the neutralizer, and the drift duct. Most conventional neutral-beam injectors feature vacuum pumps (cryopanel) in close proximity to the beamline axis. One advantage of this approach is high conductance between the gas sources and pumps. As seen from Fig. 3-86, cylindrical cryopanel (front and rear) completely envelop the beams, as well as all of the dumps. Cryopanel size is limited by the maximum size of the vacuum chamber; consequently pressure distribution inside the chamber is influenced by constraints on the external geometry.

The neutralizer represents by far the largest gas source. To intercept a large fraction of streaming thermal gas, a baffle is positioned some distance from the neutralizer end. Because of the gas buildup, it is advantageous to

Figure 3-86. Vacuum-vessel sectioning.

extend this baffle radially all the way to the cryopanel, which effectively seals off this high-pressure section (Region 1) from the rest of the vacuum chamber. However, apertures in the baffle allow beam passage; therefore, some downstream leakage of gases cannot be prevented. The distance between the neutralizer end and the baffle is a result of a trade-off between charge-exchange loss and gas streaming, both of which have to be minimized.

The reflection magnet, a large, bulky unit, constitutes a natural barrier within the beamline, subdividing the remaining vacuum chamber space into halves, Regions 2 and 3 (Fig. 3-86). The powerful ion beam is disposed of by directing it onto beam dumps in Region 1, causing yet higher pressure there. That leaves only small gas sources, such as collimators, ion shields, etc., in Regions 2 and 3, which is a good prerequisite for higher vacuum. A complete list of gas sources and their magnitudes is given in Table 3-27.

Cryopanel. Pumping the beamline from air on startup is accomplished by a secondary vacuum system (a forepump). To remove excess gas from the chamber and keep the pressure there at a suitable level, another means of pumping must be provided. Only two candidates are suitable for this task: getter pumps and cryopanel. The gettering system is an attractive option because it does not liberate gases, it is insensitive to minor thermal variations, and it is totally immune to neutron bombardment. However, the deployment of gettering surfaces requires approximately 1.5 times the NBIS space required by cryopanel; as a result, cryopanel were selected. A comprehensive comparison of getter pumps and cryopanel is given in Table 3-28.

As previously described, the central cell cryopanel are cylindrical and envelop most of the gas sources. Each cryopanel has the geometry shown in Fig. 3-87. Hydrogen gas pumping occurs on the surface of the liquid helium panels. These panels are surrounded by liquid-nitrogen-cooled shields and louvers for protection against thermal radiation and to cool incoming gas molecules. Each of the front and rear cryopanel is designed for continuous operation. Because of the need to occasionally liberate the gas frost from the liquid helium panels (degassing process), the cylinder has been subdivided into six angular sections, five pumping and one degassing at any given time (see Fig. 3-88). The degassing cycle proceeds as follows: Louvers on each segment to be degassed rotate on their supporting hinges until they close the communication path between the liquid helium panels and gas sources. Valve 1

TDF - NBIS, CRYOPANEL DETAIL
(NOT TO SCALE)

Figure 3-87. Cryopanel structure, showing TDF/NBIS and cryopanel detail (not to scale).

Figure 3-88. Cryopanel structure.

Table 3-27. Gas sources and magnitudes.

sources Regions ers/s)	Gas load		Gas (Torr-lit
	Target pressure (Torr-liters/s) (T)		
1	53	Neutralizers (40) Ion dumps (13)	$\leq 10^{-4}$
2	1.4	Ion shield (1) Reionization (4)	$\leq 5 \times 10^{-5}$
3	1	Reionization	$\leq 5 \times 10^{-6}$

Table 3-28. Getter and cryopump beamline comparison.

Type	Minimum Degas Time (Minutes)	Degas Fraction	Minimum Cycle Time (Minutes)	Power Needed Per Beamline		Operational Temperature		Configuration	Pumping Speed of Panel's Projected Area L/S-cm ²	Panel Life
				During Outgas (MW)	Steady State (MW)	Outgas	Pumping			
SAES ST707 Alloy Getter	35	0.2	210	0.02	0.0005	500°C	25°C	WPI250/2 wafer pump modules arranged in panel form	4.1	30 up to air exposures allowed ----- 1 yr. life when exposed to 1 TL/S O ₂ or 0.2 TL/S ² N ₂
Cryogenic LHe Panel (LLNL Type)	7	0.2	42	-	-	16°K	4.2°K	2 louvered LN ₂ cooled shroud surrounding LHe panels	6	Machine

opens, and liquid helium is diverted from the primary panels into secondary liquid helium panels (Fig. 3-87). The resulting warmup of the primary panels causes gas previously trapped there to be transferred onto secondary panels. Once the transfer is accomplished, valve 1 closes, valve 2 opens, and liquid helium is diverted back from the secondary to the primary panels. The louvers can now be opened again, and pumping resumes. As the secondary panel warms up, the gas is discharged into a manifold and proceeds to the forepump. One meter of the type of cryopanel under consideration has a pumping speed of 6×10^4 liters/s. So from the gas load and pressure identification in Table 3-27 for regions 1, 2, and 3, we obtain the following requirements on cryopanel size:

- Region 1: 8.3 m^2 (actively pumping).
- Region 2: 3.3 m^2 (actively pumping).
- Region 2: 8.7 m^2 (actively pumping).

Gas-Pressure Distribution. Background pressure in the three vacuum regions was determined from a set of linear equations relating to gas loads, conductances, pumping speeds and pressures at various points in the vacuum chamber. Each region was divided into naturally occurring sections, each of which was treated as a separate entity capable of communicating with neighboring sections (Fig. 3-86).

The model given below is based on gas flow balance. In steady-state the gas generated in any section equals a net gas outflow from that section. The inflow or outflow is governed by conductance and pressure difference with respect to neighboring sections or a cryopanel. Let $Q_1, \dots, Q_{11}$ be the magnitudes of actual gas sources (dumps, etc.) in Sections 1 through 11, respectively. Then

$$Q_n = \sum_{k \neq n}^{11} C_{nk} (P_n - P_k) + S_n P_n, \quad (3-68)$$

where C_{nk} denotes a conductance from the n -th section to the k -th section, P_k is the gas pressure in the k -th section and S_n is the effective pumping speed of the cryopanel(s) (if any) in the n -th section.

Simultaneous solution of this set yields the gas pressures in various sections. An overview of the situation is given in Table 3-29.

Table 3-29. Cryopanel area.

Region	Total gas load (Torr-liter/s)	Cryopanel area (m ²)	Resulting pressure (Torr)
1	53	8.3	6×10^{-5}
2	1.5	3.3	2.5×10^{-5}
3	1	8.3	5×10^{-6}

Cryogenic System. Energy removed from gas molecules, thermal radiation, and neutron heating constitutes a thermal load that must be continuously removed by the cryogens. Because large flow cross sections and generous fluid inventory are required for free convective flow, a forced-flow liquid helium and LN₂ system was selected. Heat is deposited in both the LN₂ and liquid helium.

Assuming an average neutron heating of $P_N < 5 \times 10^{-4}$ W/cm³ for aluminum, this yields less than 150 and 50 W heat input, respectively, for the LN₂ and liquid helium cryopanel, with a total structural volume of 3×10^5 and 1×10^5 cm³. Thermal radiation from the beamline vacuum enclosure can be greatly reduced by proper shielding; it is estimated at 350 W. The radiation from "hot" beamline components (neutralizers, dumps, collimator) cannot be shielded as effectively, and it represents an additional 1150 W. The bulk of the thermal radiation is deposited into LN₂ panels, with only about 8 W being deposited into the LHe system. There is virtually no gas condensation on the LN₂ panels, while it accounts for about 6 W heat input at the LHe panels. A detailed account of cryopanel parameters is given in Table 3-30.

Helium and nitrogen flow paths are similar, as can be seen from Figs. 3-89 and 3-90.

Magnetic Shield. Proper operation of the neutral beamlines requires that the area between the points of initial ion generation (i.e., the discharge) and the final point of separation of neutral-beam particles and residual ions (the reflection magnet) be practically free of external magnetic fields. In the arc chamber a magnetic field would influence primarily electron orbits, which can result in unwanted discharge characteristics and nonuniform plasma densities. It can also adversely affect the ion velocity distribution function at the extraction points. In the neutralizer, the straight-line ion trajectory must be maintained before neutralization. A deflection by perpendicular magnetic fields will result in beam defocusing and increased beam divergence. The beam exiting the neutralizer consists of an equilibrium mixture of ions and neutrals. A magnetic deflection of the ions in the space between the neutralizer and reflection magnet can, therefore, result in (1) beam loss as a result of the ionization of neutrals and (2) uncontrolled

Table 3-30. Cryopanel parameters for each NBIS unit.

Parameter	Region 1	Region 2	Region 3
Effective pumping area (m^2)	8.3	3.3	8.7
Net capture probability	0.2	0.2	0.2
Neutron heating LN_2/LHe (W)	41/40	16/15.5	43/42
Thermal radiation heating LN_2/LHe (W)	1289/3.3	5.6/1.3	145/3.4
Gas condensation heat impact LN_2/LHe (W)	0/2.4	0/1.0	0/2.4
Total heat input LN_2/LHe (W)	1330/45.7	72/17.8	188/48
LN_2 flow rate (liters/hr)	29.3	3.3	4.2
LHe flow rate (liters/hr)	62.8	24.5	65.9
Pressure drop through LN_2 circuit (psi at 77 K)	5	3	5
Pressure drop through LHe circuit (psi at 3.8 K)	0.4	0.2	0.4

Figure 3-89. Liquid-nitrogen flow system (each panel).

Figure 3-90. Liquid-helium flow system (each panel).

modification of the ion distribution at the ion dumps. For these reasons, the maximum fields allowed are 2 G in the arc chamber and 10 G at the entrance of the reflection magnet.

The main source of magnetic fields at the beamline is, of course, the stray field of the main system solenoids, but fields can also be produced by high-current electrical leads located close to the beams. The latter is of particular concern for arcs with high-current filaments, which do not have to be considered here since the design is based on either the hollow-cathode arc or an rf discharge.

The vacuum magnetic fields, which are due primarily to the central cell solenoids, are indicated in Fig. 3-91 for two prominent beamline points, the last grid of the source and the entrance of the reflection magnet. The fields are of the same magnitude as those considered for MFTF, but are essentially parallel to the beamline axis, compounding the shielding problem. To facilitate shielding, it would be highly desirable to place the beam sources farther from the magnets because the field decays as R^{-3} ; however, beam divergence limits plasma source distance to the 9.5-m design value.

Because TDF is steady state, not pulsed, shielding with ferromagnetic material seems to be indicated. A detailed analysis shows that other shields might be preferable. First, the option of shielding the entire beamline vessel would require a 40-cm-thick iron wall weighing 200 tons. Also, it would not be very effective because the field inside a cylindrical shield immersed in an axially directed external field decays as $\exp(-2.4Z/R)$. This means that a 1.8-kG field in a 1.5-m-radius shield would be reduced to only 70 G at a distance of 2 m.

The second option is to shield the individual source-neutralizer combinations in a way similar to the multilayered shields proposed in Ref. 22 or to the shields of the Doublet III beamlines. It was shown that such shields, in an environmental magnetic field of comparable magnitude, can indeed reduce the field to the required value.²³ The space between the neutralizer and the reflection magnet would have to be left unshielded to effectively pump the gas emanating from the neutralizer. As mentioned earlier, this field is undesirable because it can cause beam loss and an undesirable power distribution on the ion dumps. Using the stray field as an ion deflector as proposed in Ref. 23 cannot be done in the TDF case because

LOCATION	B_r (KG)	B_z (KG)	B (KG)	θ (O)
P ₁	.797	.086	.802	83
P ₂	1.798	.433	1.850	76

Figure 3-91. Magnetic field identification.

the field is longitudinal rather than transversal. Also, individual shields for each beam would weigh 3 tons, which, together with the refraction magnet, means that there would be 20 tons of magnetic material in a beamline.

A lightweight shield could be constructed to eliminate the harmful perpendicular field in the neutralizer to the reflection field region. However, the resulting fringe field in the source region would be intolerably high.

One potential shielding option has not been considered here because extensive computer calculations would be required to evaluate it.

The beamline vacuum vessel is to be surrounded by neutron shielding material. For neutronics reasons, this material can be replaced by soft iron of sufficient thickness. This iron cylinder would reduce the field inside to a level that could easily be reduced further by small, individual, magnetic shields.

Because the use of ferromagnetic materials as shields does not seem a reasonable solution primarily because of weight problems, a superconducting magnetic shield was adopted as a baseline design for the central cell neutral beamlines. Notice, apart from the difference in weight, that the superconducting shields can be more effective since, unlike iron shields, they do not pull field lines from a large area toward the shield, thereby increasing flux that has to be eliminated. Rather, they "push" the vacuum field lines away from the region to be shielded.

Although superconducting shields have been used in a trial-and-error fashion for a long time, only recently has work been done to optimize the them.²⁴ Such shields have been built for a variety of applications. They have produced regions containing fields of only 10^{-10} T, used to calibrate sensitive magnetometers. Long, narrow shields have been constructed at CERN and SLAC for field-free propagation of particles in high-energy experiments. Finally, a superconducting shield for a Baseball II beamline, measuring 36 in. in length and 16 in. in diameter, excluded an external field of 1.5 kG.²⁴ Superconducting shields are ideal if a cryogenic environment has already been provided, like the cryopanel surrounding the beamlines of the TDF central cell.

Type I superconductors are ideal magnetic shield materials because they totally exclude fields to within a surface depth of 10^{-5} cm. Their drawback

is that the critical field is low, 1.5 kG for niobium, beyond which complete field penetration occurs.

In contrast, Type II superconductors, whose critical field can be as much as a factor 30 higher, are not totally diamagnetic. An external magnetic field induces a current in a surface layer whose thickness increases with increasing field so that a critical current J_c is not exceeded at any point. The thickness of this layer and, therefore, the field penetration depth is given by

$$d = \frac{10}{4\pi} \frac{H}{J_c} \text{ (cm) } , \quad (3-69)$$

where the external field H is given in gauss and the critical current is given in amps per square centimeter. For a typical Type II superconductor, $J_c = 10^5 \text{ A/cm}^2$, which means that, for example for $H = 2 \text{ kG}$, $d = 0.016 \text{ cm}$ or about 6 thousandths of an inch.

The critical current J_c of Type II superconductor is temperature-dependent. Thus, if local heating occurs (for example because of rapid flux change), J_c is reduced, allowing additional flux penetration that can, in turn, cause additional heating. If equilibrium is not reached, flux jumping occurs. This stability problem occurs particularly in large cross-section superconductors, like the sheets from which cylindrical superconducting shields would be constructed. Stabilization can be effected by keeping the superconductor in close thermal contact with a cold base material.

Because thin cross-section wire is not as sensitive as the flux jumping just described, effective magnetic shields have been constructed in the form of solenoids. The current induced in the wire will be such that the magnetic field energy inside the solenoid is minimal. Since the current is constant throughout the solenoid, fields in some parts of the volume will be overcompensated and in others undercompensated if the vacuum field was not spatially constant. In principle, this problem can be eliminated by proper spatial distribution of the solenoid windings.

The shielding property of the superconductor should be constant in time. The current decay that results from field creeping is estimated to change the inside field by one part in 10^4 in seven years.

As shown in Fig. 3-92, the superconducting shield of the central cell NBIS is constructed in two parts, one inside and one outside the vacuum chamber. The niobium-titanium superconducting material is laminated onto an aluminum cylinder that provides both mechanical support and cooling of the niobium-titanium. As mentioned, close thermal contact of the niobium-titanium sheet is necessary for the thermal stability that prevents flux jumping. The aluminum cylinder is cooled to 4.2 K by liquid-helium-carrying coils. It is surrounded on either side by LN_2 shields and, on the side of a room-temperature wall, by an additional thermal radiation shield. The external shield section, which is movable for ion source servicing, is terminated by a flat, washer-shaped disk that allows the external shield structure to be shortened considerably. The disk provides access to the beamlines of service lines. The shielding properties of the superconducting shield, featuring a shortened external structure with the disk, have not been evaluated in detail because the appropriate computer model is not currently available. Chargin and Anderson²⁵ show that such a termination to a superconducting cylindrical shield can be used effectively to shorten the cylinder. The alternate choice of a 4-m-long external shield in the form of a constant-diameter cylinder would reduce the magnetic field at the point of the source grids to 2 G.

Neutron Shield. While neutronics calculations and results are covered in a separate section, it is important to understand some neutronics considerations that influence the beamline design.

The first wall is designed for a neutron flux of 2.0 MW/m^2 and, because the operation is steady state, the lifetime and heat deposition due to gamma rays, neutrons, and secondary radiation will be important considerations. Beamline-related problems include (1) heat deposition in the cryopanel and superconducting shield and (2) radiation damage of the ion-source insulator. In general, heat deposition into the cryogen is important to system capital cost since the heating influences cryoplant size. Operating costs also increase because 600 to 1000 W must be expended in the plant for each watt deposited in the cryogen. The other critical area, related to damage of the

Figure 3-92. Superconducting shield design.

ion source insulation, is important because it has a strong influence on the lifetime of this component. The radiation limits used to govern the design were:

Shield heat load	10 MW/cm ³
Dose in polyimide insulator	5 x 10 ⁹ rad
Dose in Macor insulator	6 x 10 ⁸ rad

Neutron damage to beamline components is an inherent problem because the beam duct can readily transport neutrons from the plasma to the components either by direct line-of-sight propagation or by scattering neutrons from the wall surface. Scoping calculations were performed at TRW, using the TART NP code to study the influence of duct shape on outside neutron flux. To obtain the maximum number of sources per beamline, sources are arranged azimuthally adjacent to each other, separated by an angle of 3.3 deg. The deposition rate onto an aluminum sampler at the duct exit is determined as a function of duct width (Table 3-31). Increasing the number of beams per duct, i.e., increasing the duct width in θ , did not increase the energy deposition. This result was anticipated because the plasma diameter is smaller than the duct opening. In addition, doubling the duct width increased the energy deposition by almost a factor of 2. From these results, a preliminary duct shape was selected. The duct is V-shaped in θ , and the beams are stacked side by side in this plane. In the plane containing the plasma axis, and at the plasma end, the duct is made as narrow as neutral-beam transport considerations allow to limit neutron and gas streaming into the duct. At the source end, the duct is widened to enhance pumping but left narrow enough to keep the first cryopanel in the shadow of the neutron shield surrounding the duct.

As detailed in the neutronics chapter of this report, the radiation flux and neutron heating of the various components was calculated for the detailed neutral beamline configuration. The results are given in Table 3-32. Both the heating of cryogenic panels and the expected lifetime of source insulation are found to be within design requirements.

Systems Design

Instrumentation, Control, and Data Acquisition. Monitoring and control of neutral-beam injector operations are performed by the instrumentation, control, and data-acquisition system (ICDAS). Functionally,

Table 3-31. TART results.

Beams/ duct	Duct width in Z (cm)	Neutron edep	Neutron flux	T edep	T flux	Total edep
2	60	1.25-2	9.14+15	4.83-3	4.87+15	1.73-2
3	60	1.16-2	1.01+16	6.46-3	5.36+15	1.80-2
4	60	1.08-2	9.41+15	5.69-3	4.75+15	1.65-2
5	60	1.16-2	1.06+16	5.35-3	4.91+15	1.7-2
4	120	2.18-2	1.99+16	1.09-2	9.70+15	3.27-2

Table 3-32. Results of neutron streaming and shielding calculations for the central cell neutral beamlines.

Superconductive shield

Average heat load (MW/cm ³)	0.47
Neutron fluence (E > 0.1 MeV) after 5.4 FPY (n/m ²)	1.76 x 10 ²²
Dose in polyimide after 5.4 FPY (rad)	2.57 x 10 ⁹
dpa rate in copper (dpa/FPY)	2.1 x 10 ⁴

Cryopanel

Heat load (mW/cm ³)	
Front cryopanel	0.96
Rear cryopanel	0.027

Ion source

Neutron dose rate in Macor (rad/FPY)	2.17 x 10 ⁸
Ion source replaced after 2.77 FPY	

the system is composed of three interrelated subsystems: data acquisition, control, and interlock.

1. The data-acquisition subsystem consists of sensors, sensor-signal processing electronics, and displays capable of monitoring routine injector operating parameters and presenting them to the operator in a meaningful manner. The provision to acquire diagnostic data during the operation is also included.

2. The control subsystem provides for adjustment of beamline operating conditions. Control inputs allow ion source voltages to be adjusted, event timing to be specified, and mechanical devices to be positioned. The effect of a control input on the system may be ascertained via the data-acquisition system.

3. The interlock subsystem is an independent portion of the ICADS. Its role is to ensure complete safety for operating personnel and to protect beamline and reactor equipment in the event of improper injector operation or a hardware malfunction within injector subsystems.

The following sections describe the requirements placed on the ICADS and design concepts for a QQ system capable of satisfying them.

General. A computer-driven instrumentation, control, and data-acquisition system will be used for this NBIS. The system, configured like that of the TDF reactor and compatible with it, will use CAMAC hardware interfaced to a computer.

To understand the trade-offs involved in the design of the ICADS, it is worthwhile to review the CAMAC standard. The CAMAC system was developed to standardize protocol for the transmission of data and control signals between instrumentation modules and a digital controller. It imposes mechanical requirements on compatible hardware, thereby allowing flexible data-acquisition and control systems to be configured.

The basic element of the CAMAC system is the module that provides the interface between the external sensors and controls and the system. Physically, as many as three modules reside in a crate, the standard mounting structure, which includes power supplies and intermodule connectors and wiring. The modules communicate with the crate-contained modular controller via dataway, a standard interface bus. The crate controller provides the capability for addressing individual modules and for transferring data and

control signals to and from them. Physically, the crate controller resides at two locations, or slots, in the crate. In the most common CAMAC-configured system, the controller provides no intelligence, but simply interfaces the dataway to an external TDF computer via a data highway. This is the configuration of the TDF instrumentation and control system, an the eventual form of the neutral-beamline ICDAS.

Data Acquisition and Display. The data-acquisition subsystem is required to sense, process, and display a limited set of process-related and diagnostic data.

Each beamline will be instrumented with sensors and monitors to perform, at minimum, the measurements required on MFTF-B. These include sensors that provide inputs to the interlock electronics (e.g., valve status), that measure support subsystem performance (e.g., gas pressures), and that provide beam diagnostic data (e.g., cooling water temperature rises and flow rates).

Special displays will be provided for the power-supply monitors and the beam-energy diagnostic measurements. Oscillographs are to provide displays of ion-source power-supply waveforms. Panel displays will be used to read out static supply voltages. Similarly, energy deposition on all instrumented surfaces (collimators, shine-through, and ion dumps, neutralizers, etc.) will be displayed on panel meters. It was suggested that displays be interfaced to the ICDAS via CAMAC, a concept that will be followed in the proposed design.

Control. The NBIS control system is required to provide two general types of control inputs. The first, referred to as event timing, responds to a TDF facilities clock and produces properly sequenced triggering signals to beamline components. These produce the staged operation of valves and power supplies necessary to produce the ion beam. The same timing circuitry produces triggering signals to initiate data acquisition in the ICDAS. The second category of control inputs allows the beamline to be operated manually, that is, independently of the computer. Manual controls provide for adjustments of power-supply voltages, the actuation of valves and motors, the resetting of fault shutdowns, etc.

During the early stages of operation, the beamlines must be capable of complete manual operation for checkout, debugging, and source conditioning.

Beamline manual control inputs are provided by control-panel-mounted switches. Thumb-wheel and rotary selector switches, interfaced directly to the power-supply control electronics, program output voltages and currents. Similarly two- and three-position toggle switches apply power to control relays to activate valves and positioning motors.

The sequencing or staging of beamline operations is performed by CAMAC-standard timing-pulse generator modules. After initiation of counting by a start-pulse input from the TDF ICS, time-successive, independent outputs are available from the comparators. They, in turn, initiate the typical control and monitoring activities. Flexibility in sequencing is obtained by allowing manual control of the comparison register's inputs, which is accomplished by control-panel-mounted thumb-wheel switches. These switches are interfaced to the dataway in a manner analogous to that used for the displays: all switches are bused together and provide a set of common inputs to a single BDC-to-binary converter, and the output feeds a digital-input gate module. The thumb-wheel switches are scanned under microprocessor control, and the selected data are applied to the dataway. The switch-read operation is followed by a CAMAC write, whereby individual comparators are loaded with a programmed set-point time interval. Because of the expandable nature of the CAMAC-standard architecture, inclusion of these additional control provisions is easily obtained should it become necessary.

Interlocks. The function of the interlock subsystem is to ensure that the NBIS is not allowed to operate in a mode hazardous to operating personnel or damaging to beamline components. Because of the high potential or serious damage that improper injector operation may produce, the interlocks themselves are required to be failsafe. Successful beamline interlock subsystem design requires a careful assessment of potential failures or improper operating modes, the selection of parameters indicative of these, the choice of sensors capable of adequately metering these key parameters, and finally a reliable hardware mechanization of the interlock function.

The interlock system is required to operate independently of the computer, to be as failure free as feasible, and to be so configured as to preclude operation of the NBIS or any of its subsystems when doing so would present a hazard or result in damage to the apparatus. Each beamline is required to

contain its own interlock system. It is reasonable to assume that other beamline subsystems will be similarly instrumented if a fault internal to the subsystem endangers the safety of operating personnel or of the equipment itself.

The sensors monitoring critical parameters would be combined with inputs derived from other NBIS and TDF interlock sensors to create the overall commands to inhibit system operation if a malfunction is detected.

The most fundamental interlock function is to prevent the operation of the sources if the proper beamline operating conditions are not met. For example, the beam may not be operated if the source isolation valve is closed, the cooling water is not flowing, the system pressure is not sufficiently low, the bending magnet is not providing the correct field, etc. In general, any single failure to satisfy the interlock conditions will inhibit the beam. Even in the absence of beam, damage may be done to beamline components because of improper conditions within the beamline. For example, to avoid boiling off liquid helium, it is necessary to ensure that the liquid nitrogen has cooled the cryopanel sufficiently before the helium is introduced. It is clear that some interlock functions must be staged; that is, certain beamline operations must be performed in a specified order, and the interlocks must be inspected correspondingly.

Personnel safety is the critical interlock function. The presence of high voltage, high fluxes of neutrons, and x rays requires extreme precautions. All areas containing hazardous equipment will be required to have limited access. Further, the equipment contained therein will be interlocked to preclude jeopardy to service personnel. A viable safety-motivated interlock is to equip each restricted access door with a key-operated lock and further require that the key be inserted into an interlock in the control room before energizing the beam. This will ensure that all personnel are in a safe area during NBIS operation.

In addition to inputs derived from beamline components, other TDF systems provide interlock signals; for example, a plasma must be established in the reactor before the beam is injected to avoid damage to the shine-through dumps. The injector power supply subsystem contains its own independent interlock system that provides a single input to the beamline interlock system.

The electronics comprising the interlock system will be composed of high-reliability components. The circuitry will be hardwired and configured to provide failsafe operations through selective redundancy. That is, single point failures (or reasonable combinations of failures) will always produce a beam "off" condition. Both a self-check capability and status indicators will be provided; the status indicators will show the condition of all critical beamline components directly. The assertion of interlocks will be accomplished by audible and visual alarms to notify operators of a malfunction.

Service Requirements. Each NBIS unit requires a multitude of power, coolant, and cryogen supplies to function properly. These services have to be made available at the place of beamline deployment. An overall summary is given in Table 3-33.

Electric Power. There are two kinds of power-consuming devices within the beamline: the sources and the reflection magnet. While the magnet is serviced by a simple, low-voltage, medium-current line, each source requires several high-voltage and high-current lines. Table 3-34 gives a detailed account of the power supply needs and their parameters. Note that the data are given for only one of four sources. Electric and I & C cables for each source will be harnessed together and terminate preferably at a single multiple connector (Fig. 3-93). Such an arrangement would facilitate quick replacement of the source.

Coolant. It has been calculated that only about 3.44 MW of the 5.6-MW beam at each source will pass through a front collimator. The difference, 2.16 MW, is ultimately dissipated within the vacuum envelope of the injector. If all four sources operate at the same time, some 8.7 MW will have to be removed from the beamline. Assuming that the coolant will be water and allowing an average temperature rise of $T = 30^{\circ}\text{C}$ yields a flow rate $Q = 64.4$ liters/s, i.e., 232 milliliters/hr. The coolant velocity in supply lines should not exceed 200 cm/s; hence, the total required flow cross section is about 322 cm^2 . Roughly 90% of the heat is generated at the four ion dumps. The remaining 10% is created at the negative ion dump, devious ion shield, collimators, and source and is summarized in Table 3-35. Note that all of the coolant is subject to tritium recovery.

Table 3-33. Service requirements for eight beamlines.

Operation at maximum power Service	Normal operation scenario	
	(32 sources)	(24 sources)
Electric power (MW)	179.2	134.4
Cooling (liters/s H ₂ O)	640	480
Cryogenics (liters/s)		
LN ₂	1.088	0.816
LHe	0.452	0.339

Table 3-34. Power-supply requirements.

Parameters	Arc	Acceleration grid	Gradient grid	Suppressor grid	Magnet
Voltage	100 V (full load)	20 to 80 kV	0 to 60 kV to ground -2 to -30 kV to acceler- ation grid	1 to 5 kV	50 V
Current	4000 A	80 A	± 3 A	0 to 16 A	300 A
Regulation	$\pm 3\%$		$\pm 1\%$ to acceler- ation grid		
Tracking			$\pm 5\%$ to acceler- ation grid

Figure 3-93. Electrical and fluid connectors to ion source.

Table 3-35. Coolant requirements.

Component	Power to be removed (kW)	Water flow rate at $\Delta T = 30^{\circ}\text{C}$ (liters/s)	Supply line identification (cm)
Positive ion dump (each)	1948	15.5	10
Negative ion dump (all 4)	260	2.1	4
Devious ion shield (all 4)	128.5	1.0	3
Front collimator (all 4)	167	1.3	3
Rear collimator (all 4)	76.6	0.61	2
Reflecting magnet	15	0.12	1
Neutralizer (all 4)	76	0.61	2
Source (each)	100	0.80	3
Drift duct	<u>760</u>	<u>6.0</u>	7
Total (whole beamline)	9675	77.0	

Cryogen Supply. In Section 3.3.3, we concluded that the cryopump at the 20 K system for each beamline requires 0.102 liters/s of liquid nitrogen at 77 K and 0.0425 liters/s of liquid helium at 4.2 K. Magnetic shield needs are 0.034 liters/s of liquid nitrogen and 0.014 liters/s of liquid helium per beamline. Separate lines are required for the front cryopanel, rear cryopanel, and internal and external magnetic shields. Each line must have a flexible section terminated by a connector.

Gas Feed. Each source has two gas lines: fuel and purge. An equivalent of 70 A of the fuel is converted into beam while about 80 A is ejected from the source as neutral gas. The total particle current of 150 A to be supplied by the gas feed is equivalent to

$$Q = \frac{I_s}{eN_L} \approx 35 \text{ cm}^3/\text{s} \text{ at standard pressure.} \quad (3-70)$$

This does not constitute a problem for deuterium injectors; where tritium is handled, however, special precautions have to be taken to prevent an accidental release to the surrounding atmosphere. These precautions apply especially to cut-off valves and connectors.

Dry nitrogen will be universally used in TDF as a purge gas. Each source must be capable of being purged independently.

Vacuum Forepump. Each beamline has a separate transfer pump external to the injector.

Installation and Servicing. NBIS units are to be deployed in locations that are not always directly accessible by overhead crane. Moreover, it may not be possible to replace some short life components (i.e., sources or dumps) without disturbing at least some beamline subsystems. This section identifies the access problem.

Installation. The NBIS vacuum vessel can be disassembled into two major parts (Fig. 3-94): the nose section that leads to the drift duct, and the cylindrical section that contains all the other beamline components.

Figure 3-94. Vacuum-vessel disassembly.

The nose section is basically an empty shell that is permanently attached directly to the TFF vacuum vessel at the narrower opening. External stiffeners and reinforcement bars may be added to improve rigidity and stability. A carriage on short tracks will be used to support the cylindrical section of NBIS, which represents 95% of the unit's bulk weight. During installation (or servicing), the cylindrical section would be dropped onto the carriage from an overhead crane and would then be moved forward to couple with the nose section. Except for the interface flange, there are no electrical or mechanical connections between the nose and the cylindrical section. Bolts in that flange are attended remotely, manually, or preferably without moving any biological shielding material. The magnetic shield cap with all cables and connectors is also delivered by crane and rests on the tracks. Movement of the carriages is controlled by electromechanical actuators mounted between the tracks.

Servicing. Mechanical design and configuration details for the NBIS units are consistent with the following servicing philosophy. Short-life and high-failure-rate components (e.g., ion sources and dumps) are to be replaced with the beamline in situ, causing minimal disturbance to the main assembly. These components will then be transferred to a cell for servicing, disassembly, or scrapping. On the other hand, access to other beamline components can be gained only by major disassembly, which includes removal and transport of the cylindrical section to the hot cell. Each scenario will be discussed in detail.

In Section 3.3.3, "Neutron Shield," we determined that the ion sources penetrate the rear of the biological shield. Since each source has an individual shield, there is no radiation hazard in the area of the service connectors as long as all the sources are in place. Consequently, all the connectors and source fasteners may be attended manually. Once the source isolation valve is loose and the fasteners are removed, the source can be removed by a remotely operated manipulator. A dummy shield can be placed into the empty space temporarily to seal off the radiation.

The sequence will be as follows:

1. Power disconnected (Fig. 3-95a).
2. Magnetic shield moved back (Fig. 3-95b).
3. Faulty source disconnected.

4. Faulty source-isolation valve shut.
5. Faulty source fasteners removed.
6. Faulty source removed (Fig. 3-95c).
7. Dummy shield or a new source put in place.
8. Steps 1 through 5 reversed.

Note that the magnetic shield cap was not deactivated although it was moved. It turns out that an identical sequence is required to service the ion dumps. However, their projected lifetime is substantially longer than that of sources, so their attendance is of lesser importance.

The lifetime of most other beamline components is comparable to or exceeds the life of the TDF device, however, accidental failures cannot be excluded in a system as complex as the NBIS. Repairs connected with such failures may require an extensive effort. As a result of this and the configuration of the magnetic shielding, it is preferable not to attempt detail disassembly on site. This would create a radiation hazard in an area where there is manipulator support. A more favorable approach is to transfer the entire cylindrical section to a hot cell, where it can receive proper attention. Although this process requires major disassembly, the actual number of joints and connections is quite limited and the whole task can be performed in a short time. The detailed sequence is as follows:

1. TDF magnet coils detached.
2. TDF and NBIS cryopanel brought up to room temperature.
3. TDF brought up to air (dry nitrogen fill).
4. NBIS power disconnected (Fig. 3-96a).
5. Magnetic shield cap moved back, and shield around flange removed (Fig. 3-96a).
6. Flange fasteners removed, cables and hoses disconnected, cylindrical section moved back (Fig. 3-96a).
7. Cylindrical section transferred by overhead crane to hot cell for detail subassembly.
8. Shielding cap placed in lieu of cylindrical section to seal off radiation leakage from inside TDF.

Figure 3-95. Servicing a source.

Figure 3-96. Removing cylindrical section for servicing.

Central Cell NBIS, Key Technical Issues

Ion Source. Ion source development is at the state where full-power operation at the required current densities is possible for several seconds. Source lifetime is normally determined by the reliability of the thermionically emitting cathode. A new development using electrodeless rf discharge has considerable promise in terms of long life and reliability. The second problem is extraction grid erosion, which can influence ion source performance not so much because of evolving structural problems, but because of the loss of optimum surface shape for load focusing.

Present ion sources can achieve the beam divergence of 0.5 assumed in the central cell beamline design. While this divergence is sufficient to obtain the required power deposition in the plasma without exceeding the maximum design power density on the shine-through beam dumps, sources with smaller divergence are needed to increase the amount of beam intercepted by the plasma and to allow the sources to be placed a large distance away from the plasma. This would reduce the demands on radiation and magnetic shielding of the sources and neutralizer.

Power-Absorbing Surfaces. Both the ion dumps (which absorb the energy of the residual ions) and the shine-through dumps (which receive the neutral beam not trapped in the plasma) operate at a power deposition rate just below the state of the art. Currently, this can only be achieved by inclining the dump surface with respect to the particle trajectory. Unfortunately, this carries the penalty of a higher sputtering rate and, therefore, a shorter dump lifetime.

From Monte Carlo calculations, it was found that 80-kV tritium neutrals or ions are deposited an average of 3000 Å below the beam-dump surface. Since the transport of hydrogen and its isotopes in metallic lattices proceeds by interstitial diffusion, the transport rate can be high particularly for the elevated beam-dump operational temperature. First calculations show that the tritium deposition into the cooling waters of the beam dumps is high enough to make tritium processing of the cooling water necessary. Dump designs should be developed to limit tritium diffusion; for example, use laminates of several materials, some of which have small tritium solubility.

Magnetic Shielding. To limit divergence growth of the neutral beam and ion source, the neutralizer and the space between the neutralizer and the reflection magnet must be shielded from stray magnetic fields. Shielding this volume is difficult because the local magnetic fields have a magnitude of 0.8 to 2 T and are almost colinear with the neutral beam axis. However, effective shielding can be achieved with either ferromagnetic or superconducting material. Shielding with ferromagnetic material has the advantage of being free of maintenance requirements. Its disadvantage is weight, which amounts to 200 tons if the entire beamline is to be shielded and to 12 tons if individual components are to be shielded. One option is to use an external soft iron cylinder around the beamline to reduce the interior field to 100 G; final field reduction can be achieved with individual component shields. A more attractive approach is to use an active superconductor (niobium-titanium) cylinder. Although such shields have been built and tested on a small scale, their effectiveness at the size required by the TDF/NBIS has not been confirmed.

3.3.4. End Cell Beam Requirements

This portion of the report addresses the three neutral-beam injectors required for the anchor cell. The injectors are designated by function as (1) transition pumping (TP), (2) anchor cell pumping (ACP), and (3) anchor cell sloshing (ACS). Figure 3-97 shows the layout of the injectors in the two principal planes of the anchor and end cell regions. The purposes of this section are (1) to document the results of the FY 1982 preconceptual design effort on TDF and (2) to describe the key issues encountered.

During the preconceptual design effort, there was considerable interchange between physicists and engineers to solve anchor cell access problems. This was the key to our success.

The design descriptions are not detailed because this was a preconceptual design effort. Performance allocations and assessments have been carried out at the major component level. Particulars of the detailed design are left to subsequent design efforts, where hardware design support is included. In general, specified component characteristics are within the state of the art.

Figure 3-97. Anchor-cell NBI locations.

A summary of salient requirements allocated to the anchor cell neutral-beam system is given in Table 3-36. In addition, geometric constraints are imposed by the device and its major components. As evident in the discussion that follows, the yin-yang coil pair used in the anchor cell imposes considerable constraints. Before we interpret the requirements, however, something should be said about the coordinate system used.

Figure 3-97 shows one end of TOF in plan view. The plasma axis (going away from the central cell) is designated the Z axis. Coil M-3 has its major radius open in the Y-Z plane; hence, the aforementioned plan view is in the Y-Z plane, with the X coordinate out of the plane of the paper. The opposite end is rotated 90 deg, so references to beams in specific planes, etc., have to be rotated/translated properly.

Note that all three injector requirements are specified in Table 3-36. Injection angles are given with respect to the plasma axis (Z axis). The injection plane is specified to ensure that the injected beam meets the assumed trapping fractions. This dependence of trapping fraction on the plane of injection comes about because the plasma is assymetrical. The beam intercepts are specified in terms of the magnet field along the Z axis. Figure 3-98 shows the translation of these intercepts into Z-axis positions. It also shows the interrelationship of the beam footprints. The species mix is specified to ensure consistency in modeling both the physics and neutral-beam performance. Species mixes of this quality are achievable in current sources. The trapping fraction is not a requirement on the beamline design directly, but does allow calculation of required beam-dump power-handling capability.

Specifically, 70% of the injected current goes to the beam dump in the case of the anchor sloshing beam, and only 40% of the transition neutral-beam current goes to its dump. In the path traversed by the anchor pumping beam (upstream in the end cell particle fan), there is significant charge exchange; hence, an additional loss factor of 30% must be applied. The pumping beam is made large enough so the power intercepting the plasma is of the specified amount ($1.45 \text{ A} \times 80 \text{ keV}$ or 116 kW/anchor). There are no serious nuclear shielding problems in the anchor cell nor the end cell. The neutron flux on the wall is about $3 \times 10^{10} \text{ n/cm}^2 \text{ s}$ in the anchor cell requires that the sloshing beamline be properly shielded. No significant cryopanel heating or

Table 3-36. Summary of anchor cell neutral-beam requirements. (Note that all have a beam energy of 80 keV, a species mix F/H/T (%) of 80/15/5, a neutron flux of 3×10^{10} , and a pulse length capability of S.S.).

Parameter	Anchor		Transition pumping
	Pumping	Sloshing	
Injection angle (deg)	≤ 15	90	30
Injection plane	Y-Z	X-Z	Y-Z
Magnetic field at intercept (T)	1.0-2.5	2.85	3.0-1.0
Injected current ^a (A)	2.9	4.2	60
Assumed trapping fraction ^b (%)	30 ^c	30	60
Magnetic flux (G) (axial/trans. at source)	68/<1	800/600	300/40

^aFull energy total (both ends).

^bNot a requirement.

^cPassage through the fan will trap an additional 30%.

Figure 3-98. Neutral-beam plasma targets.

radiation damage is expected. Finally, the magnetic flux was specified in a simplistic manner to indicate grossly the size of the magnetic shielding problem. Detailed plots of the field around the injector will be given in the discussion section that follows.

3.3.5. End Cell Beam Discussion

Approach. Figure 3-99 shows the penetrations of the three neutral beams into the anchor cell. (Note: "anchor cell" is used loosely here to cover both the anchor cell that is the yin-yang coil pair M-2/M-3 and the transition coil T-1.) Each of these neutral-beam injectors will be described in detail, followed by a discussion of key issues encountered in the preconceptual design. All the beamline performance estimates and major component sizings were done with the aid of a computer model developed on the Engineering Test Facility Program.

Component Design

Transition Pumping Neutral-Beam Injector. Table 3-37 summarizes parameters of the transition neutral-beam injector. Six narrow beams form a fan that covers the Z-axis intercept from 4.7 to 6.0 m. This interface is shown in Fig. 3-99 and detailed in Fig. 3-100. The heat flux distribution of the six-beam fan projected on the Z axis is shown in Fig. 3-101. Part of the rationale for this six-beam configuration is suggested by comparing it with the two-beam fan composite. Either option injects the same power over the Z = 4.7 to 6.0-m region, but the beam flux outside this desired region is much higher for the two-beam fan. This spillover heats the choke coil, which is undesirable.

Figure 3-100 shows the six sources configured in a line array spaced 25 cm apart, and Table 3-38 gives the parameters for these ion sources. More details of the beam design layout are shown in Figs. 3-102 through 3-104. Note that the high-failure-rate items (ion sources and ion beam dumps) are accessible from the back. The particulars of the beamline are summarized in Table 3-37. Figure 3-97 shows the injector positioned within the end cell. Access for maintenance is through the back panel, where all connections are

Figure 3-99. Anchor cell vacuum vessel.

Table 3-37. Summary of transition beam parameters.

Energy (keV)	80	Source current (A)	14.34
Power injected (MW)	3.793	Source separation (cm)	25
Full energy	2.880	Cryopump area (m ²)	
Half energy	0.674	Gas cell chamber	12
One-third energy	0.238	Drift cell chamber	7
Prime power (MW)	7.311	Beamline diameter (m)	2
Efficiency (%)	51.9	Beamline transition	
Full energy current (A)	36	section length (m)	0.7
Number of sources	6	Drift-duct cross section	
Divergence, 1/E (deg)	0.25 x 0.5	width/height (cm)	80/55
Beam crossover distance (cm)	835	Drift-duct length (cm)	200
		Cost (\$M)	6.6

Figure 3-100. Transition-pumping-beam geometry.

Figure 3-101. Transition beam plasma power density for two configurations.

Table 3-38. Characteristics of a transition
neutral-beam ion source.

Characteristic	Performance
Discharge	
Current density (A/cm^2)	0.3
Species mix (%)	80/15/5
Gas efficiency (%)	50
Gas flow/source (Torr-liters/s)	3
Maximum stray B (G)	4
Extracted beam	
Aperture grid	Rectangular
Grid transmission efficiency (%)	60
Beam divergence (deg)	0.25 x 0.5
Area (cm^2)	80
Ion beam current, all energies (A)	14.4
Full energy (keV)	80
Grouping	
Sources/beamline	6
Beamlines/ena	1
Spare sources	1
Total number of sources	12

3-251

Figure 3-102. Transition beam injector--plan view.

Figure 3-103. Transition beam injector--elevation view.

Figure 3-104. Transition beam injector--end view showing connections.

made (see Fig. 3-104). The injector assembly is mounted on roller bearing surfaces and can be removed in total from the back. The injector is cylindrical (2-m diam and 4-m length) and encased in an 18-cm-thick ingot iron magnetic shield. Cryopumps are cylindrical, similar to the Doublet 111 beamline. A modification to the cryopanel design will allow the LN₂ chevrons the closing motion needed to regenerate portions of the cryopump while the beamline is operating. Such a design has been developed on the flat cryopanel used in the end cell. As seen from Figs. 3-102 and 3-103, two chambers are to be pumped--one in the gas cell area and one in the drift chamber. The pressures in the chambers are estimated to be 1.1×10^{-5} and 4.8×10^{-6} Torr, respectively. A power balance analysis that reconciles the power and efficiency numbers in Table 3-36 is portrayed in Fig. 3-105. Each unneutralized full energy beam striking the ion dump has a power of 317 kW. The peak power density is taken from

$$i = \frac{I_t}{16 RXRY} \left[\left(\operatorname{erf} \frac{RX + Y}{Z\theta} + \operatorname{erf} \frac{RX - Y}{Z\theta} \right) \right]$$

and the peak at $y = x = 0$

$$x \left(\operatorname{erf} \frac{KY + Y}{Z\phi} + \operatorname{erf} \frac{RY - Y}{Z\phi} \right) \quad (3-71)$$

and

$$i_{\text{peak}} = \frac{I_t}{4RXRY} \left(\operatorname{erf} \frac{RX}{Z\theta} \operatorname{erf} \frac{RY}{Z\phi} \right) = 7.2 \text{ kW/cm}^2, \quad (3-72)$$

where

- $RX = X0(1-1/F),$
- $RY = Y0(1-1/F),$
- $\theta = \text{divergence in the X direction,}$
- $\phi = \text{divergence in the Y direction,}$
- $l = \text{distance to the dump,}$
- $I_t = \text{beam power,}$
- $\bar{F} = \text{focal length.}$

ORNL-DWG 82-3179 FED

FROM ACCELERATOR

- 807 x 6 @ E
- 152 x 6 @ E/2
- 51 x 6 @ E/3

NEUTRALIZEREFFICIENCY

- 0.01 @ E
- 0.75 @ E/2
- 0.79 @ E/3

TO ION DUMP

- 317 kW x 6 @ E
- 37 kW x 6 @ E/2
- 10 kW x 6 @ E/3

TO PLASMA

- 2,880 kW @ E
- 675 kW @ E/2
- 229 kW @ E/3

Figure 3-105. Transition beam power fractions.

For the ion dump configuration shown in Figs. 3-102 and 3-103, the peak power density is reduced to $1 \text{ kW}/(\text{cm})^2$ by adjusting the dump angle to about 6 deg. The source spacing is such that there is no significant overlap of the ion beams on the ion dump. Figure 3-106 shows the neutral beam pattern on the beam dump. There is substantial overlap in this case, but the power densities are lower because the source to beam-dump distance (1) is much longer, and the plasma traps 60% of the power.

The stray magnetic field in the vicinity of the transition pumping beam injector is shown in Fig. 3-107. The low divergence source requires that the stray magnetic field inside the injector around the ion source and neutralizer be less than 4 G. The shielding required in the transverse direction is $\sim 100/4$, or a factor of 25. The field strength in the axial direction is an order of magnitude higher. Because the shielding factor is so modest, the shield thickness is set by the flux density saturation limit of the shield material (ingot iron). Making this assumption, the thickness is given by

$$t = \frac{D}{\left(\frac{B_{\text{SAT}}}{B_{\text{EXT}}} - 1 \right)} = 18.2 \text{ cm} , \quad (3-73)$$

where BSAT is the saturation limit of the

$$S_t = \frac{\mu t}{D} + 1 = 911 \text{ transverse field} , \quad (3-74)$$

and

$$S_A = q S_t = 638 \text{ axial field} , \quad (3-75)$$

where μ is the permeability (10^4) and $q(0.7)$.

Anchor Cell Pump Beam. The anchor cell pump beam and the anchor cell sloshing-beam injectors are small (1 m diam x 2 m long) source injectors that supply about 200 to 300 kW of injected power. Figures 3-108 through 3-110 show details of the injector design layouts. Two ion sources are built into the system, one for operation and one for a spare.

There is a 30% trapping fraction of the pumping beam as it traverses the particle fan of the end cell on its way to the plasma target inside the yin-

Figure 3-106. Transition beam dump patterns.

Figure 3-107. Magnetic fields around transition pumping beam.

PLAN

ALL DIMENSIONS IN cm

AC
SLOSHING
BEAM

AC
PUMPING
BEAM

A	361	1725
B	45	35
C	4	6
D	13	1.2
E	35	25
F	9	6

Figure 3-108. Anchor cell beam injector--plan view.

Figure 3-109. Anchor cell beam injector--elevation view.

Figure 3-110. Anchor cell beam injector--end view showing connections.

yang coil pair (see Fig. 3-97). Because of the great source-to-plasma distance, the absolute beam extent (distance $\times$ beam angle) is large (Fig. 3-111). To get power through the constricted penetrations of the vacuum vessel, the angular extent of the beam must be limited. In theory, this small angular extent can be achieved by collimation of a modest divergence beam, use of a low-divergence beam, or a combination of both. Collimation wastes power. Low-divergence ion sources are under development, with the present state of the art at 0.25×0.9 deg (built and tested); a near-future (FY 1983) development has a goal of 0.25×0.25 deg. The anchor pumping beam has been sized on the basis of low-divergence (0.25×0.25 deg) optics. If this divergence is not attained successfully, the source size will be increased in power and the collimation necessary to achieve the desired absolute beam extent with sufficient power will be applied. This backup solution is practical because only a few hundreds of kilowatts of power is required.

The $0.25\text{-deg-}2/e$ beam contour, when projected onto the plasma at a distance of 1830 cm and a grazing angle of 15 deg, spreads out from $Z = 9.6 \text{ m} \pm 0.66$, depositing 86% of its power in this region ($Z = 8.94$ to $Z = 10.26$), thus meeting the pumping beam requirement. At $Z = 8.95$, the sloshing beam is injected; hence, there is a slight overlap in this region.

Figure 3-112 shows the details of the anchor cell penetration of the pumping beam. Table 3-39 summarizes the pumping beam parameters. The figure shows the beam dump located inside the yin-yang coil pair. Although the beam dump is expected to be a long-life item, access for maintenance through the particle fan exit is planned. This opening, which is ~ 20 cm in height (X-direction), was not sufficient for pumping beam ingress and was, therefore, enlarged to 238 cm, as shown in Fig. 3-112. It scrapes the beam at about the $2.5/e$ contour, thus passing $>90\%$ of the beam power. The figure also shows the contour limited in the orthogonal direction ($Y \cos 15$ deg). This is effected by collimation back in the injector.

The anchor pumping beam power fractions from a single operating source are given in Table 3-39. A pictorial presentation of the power balance for this injector is in Fig. 3-113. Part of the 194 kW of full energy is trapped in the fan, leaving 136 kW at the plasma. This corresponds to 1.7 A, compared to the required value of 1.45 A. For the full-energy ion beam, the power density on the dump shown in the design layout is $1.58 \text{ kW} \times \sin 10 \text{ deg} = 0.275 \text{ kW}/(\text{cm})^2$. Table 3-40 summarizes the characteristics of the ion source.

Figure 3-111. Anchor cell pumping beam geometry.

3-264

Figure 3-112. Anchor cell pumping-beam access.

Table 3-39. Summary of anchor cell pumping beam parameters.

Parameter	Capability	Parameter	Capability
Energy (keV)	80	Source separation (cm)	25
Power injected (MW)	0.256	Cryopump area (m ²)	
Full energy	0.194	Gas-cell chamber	1
Half energy	0.046	Drift-cell chamber	0.25
One-third energy	0.016		
		Beamline length (m)	2
Prime power (MW)	0.412		
		Length of beamline	
Efficiency (%)	51.9	transition section (m)	0.35
Number of sources	2	Drift-duct cross section	
		width/height (cm/cm)	35/25
Divergence, 1/E (deg)	0.25 x 0.25		
		Drift-duct length (cm)	150
Beam crossover			
distance (cm)	1960	Cost (\$M)	0.75
Source current (A)	5.8

Figure 3-113. Anchor cell pumping beam power fractions.

Table 3-40. Characteristics of the anchor-cell pumping beam ion source.

Characteristics	Performance	Characteristics
Performance		
<u>Discharge</u>		Area (cm ²) 32
Current density (A/cm ²)	0.3	Ion-beam current,
Species mix (%)	80/15/5	all energies (A)
5.8		
Gas efficiency (%)	50	Full energy (keV)
80		
Gas flow/source		
(Torr-liters/s)	1	<u>Grouping</u>
Power efficiency (A/W)	0.5	Sources/beamline
2		
Maximum stray B (G)	4	Beamlines/end 1
		Spare sources 1
<u>Extracted beam</u>		Total sources 4
Aperture grid	Circular	
Grid transmission		
efficiency (%)	60	

Table 3-39 shows the cryopump load to be small; 1 m^2 handles the gas in the first chamber at $\sim 10^{-5}$ Torr. In the second chamber, a significant portion of the gas is conducted through the short drift duct into the end cell, which is continuously pumped by large cryopanel. The remainder is handled by 0.25 m^2 of panel, operating in a chamber pressure of $\sim 5 \times 10^{-6}$.

One difference between the anchor pumping and sloshing beams is the particle deflector, which resides around the drift duct and transition section on the pumping beam injector. A particle flux of about $300 \text{ W}/(\text{cm})^2$ impinges on the injector walls. The magnet flux that guides these particles is almost totally along the axis of the beamline (see Fig. 3-114). For the drift-duct dimensions planned, the power intercepted by the injector duct would be $\sim 250 \text{ kW}$ without a diverter. The diverter modifies the magnetic field to introduce a perpendicular component (X direction) that bends the particle flux around the injector. A field of $\sim 100 \text{ G}$ should suffice. Heat-transfer surfaces line the drift-duct and transition section outside surfaces of the injector to provide a particle dump.

The ion source and gas cell of the beamline must be shielded from the magnetic fields of the diverter and TDF stray fields. Assuming a field magnitude of 140 G , the shield thickness is 1.2 cm and the shielding factor about 120.

Figure 3-115 shows the beam-dump patterns for a surface broadside to the beam. The two sources have essentially the same pattern, separated by only 0.5 cm . The beam dump is only required to handle one beam at a time. Each beam has a peak power density at broadside of $\sim 360 \text{ kW}/(\text{cm})^2$ --a very modest load, even at broadside. Figure 3-112 shows the surfaces of the dump at 35 and 15 deg. The surfaces were sized and set at angles to cover areas where the flux is greater than $\sim 10 \text{ W}/(\text{cm})^2$. This dump is extracted by sliding the assembly out of the "goose-tailed" mounting socket (see Fig. 3-112, end view), rotating it 90 deg, and extracting it through the particle-fan egress.

Anchor Cell Sloshing Beam. Except for ion source size and divergence, this injector is of the same basic type as the anchor cell pumping beam. The beam is injected into the plasma fan through its broad dimension at $Z = 8.95 \text{ m}$ (Fig. 3-116), which accommodates a 90 -deg injection angle. An

Figure 3-114. Magnetic fields around anchor-pumping injector.

Figure 3-115. Anchor cell pumping beam-dump patterns.

Figure 3-116. Anchor cell sloshing-beam plasma patterns.

external beam dump is positioned on the anchor cell wall. Figure 3-117 shows the passage of this beam through the small radius slot of the M-2 coil to the dump. At the coil slot egress to the dump, the vacuum vessel wall is at the 3/e beam contour in the Y direction for a 0.25-deg beam divergence. This spacing assures the wall loading will be small--below 70 W/(cm)^2 . It is also implicit that this passage have low transmission losses (neglecting plasma trapping, of course). In the orthogonal plane, the divergence is 0.5 deg. At the plasma axis, the 2/e extent along the axis is $\pm 10 \text{ cm}$. Hence, about 86% of the sloshing beam power is injected into the region between $Z = 9.05 \text{ m}$ and 8.85 m . The exploded view of the beam pass in Fig. 3-117 shows the plasma fan drawn to scale. The plasma width matches the 1/e contour at the axis. Ideally, the divergence (1/e) should be smaller to improve the trapping fraction. However, 1/e values of less than 0.25 deg represent a high development risk.

The design layout for this injector is shown in Figs. 3-108 through 3-110. As with the other injectors, access for maintenance and connections is through its back. The injector is enclosed in a nuclear shield. A neutron wall loading (at the anchor cell wall) of $3 \times 10^8 \text{ n/(cm)}^2\text{-s}$ is estimated. This is not a significant level in terms of cryopanel heating or radiation damage, however, the shield ($\sim 25 \text{ cm}$ in thickness) is required to meet dose levels 24 hours after shut down.

Table 3-41 gives a summary of anchor cell sloshing beam parameters. The full-energy-power fraction is 210 kW, corresponding to a current of 2.6 A where 2.1 A is required. This power level is available from a single source. As for the pumping beam, there is a spare source. The characteristics of the ion source are summarized in Table 3-42. A power balance is given in Fig. 3-118. In this case, the ion-dump power density is slightly lower because the divergence is double in one dimension, even though the injected power is slightly higher. The power density is 863 W/(cm)^2 at broadside, or 150 W/(cm)^2 on the ion dump surface.

Figure 3-119 shows the redundant beam crossover with the operating beam at the plasma. This crossover results in a $\sim 23\text{-cm}$ separation of the beam patterns on the dump surface some 2.7 m away. This dump is positioned approximately broadside to the beam. The power density is 1.95 kW/(cm)^2 at broadside. (A 30-deg dump surface would reduce this density to a tolerable level.)

Figure 3-117. Anchor cell sloshing beam access.

Table 3-41. Summary of anchor cell sloshing beam parameters.

Parameter	Capability	Parameter	Capability
Energy (keV)	80	Source separation (cm)	25
Power injected (MW)	0.277	Cryopump area (m ²)	
Full energy	0.210	Gas-cell chamber	1.8
Half energy	0.049	Drift-cell chamber	0.25
One-third energy	0.017		
		Beamline (m)	
Prime power (MW)	0.537	Diameter	1
		Length	2
Efficiency (%)	51.5	Length of transition section	0.35
Full energy current (A)	2.63	Drift-duct cross section, width/height (cm/cm)	45/35
Number of sources	2		
Divergence, 1/E (deg)	0.25 x 0.5	Drift-duct length (cm)	35
Beam-crossing distance (cm)	600	Cost (\$M)	0.84
Source current (A)	6.3		

Table 3-42. Characteristics of anchor cell sloshing-beam ion source.

Characteristics	Performance	Characteristics	Performance
<u>Discharge</u>		Beam divergence (deg)	0.25 x 0.5
Current density (A/cm ²)	0.3	Area (cm ²)	35
Species mix (%)	80/15/5	Ion beam current	
		(all energies) (A)	6.3
Gas efficiency (%)	50	Full energy (keV)	80
Gas flow/source			
(Torr-liters/s)	1.3	<u>Grouping</u>	
Power efficiency (A/W)	0.5	Sources/beamline	2
Maximum stray B (G)	4	Beamlines/end	1
		Spare sources	1
Extracted beam		Total sources	4
Aperture grid	Rectangular		
Grid transmission			
efficiency (%)	60		

ORNL-DWG 82-3211 FED

Figure 3-118. Anchor cell sloshing beam power fractions.

Figure 3-119. Anchor cell sloshing beam dump patterns.

Cryopumping demands are small in this injector. One square meter of pump is sufficient to handle the gas load in the first chamber at a pressure of 7×10^{-6} Torr. In the drift chamber, the duct conductance and a 0.25-m^2 panel share the load at 6×10^{-6} Torr. Both cryopumps are >20% oversized to allow continuous operation with five sections pumping and one section regenerating.

Figure 3-120 shows the axial and transverse magnetic field strengths. Because the injectors are close to the coils in the anchor cell, the flux density is highest for the sloshing beam. To avoid saturation, the shield should be >13 cm thick, which gives a shielding factor of ~1300, compared to the 350:1 (1400 G/4 G) required. Although this shield could be made smaller if the sloshing injector were moved back, doing so would increase the extent of the absolute beam width and result in higher vacuum-vessel wall loading in the beam passage through M-2. The magnetic shield, which weighs 5.7 tons, provides nuclear shielding as well. The concrete nuclear shield could be reduced somewhat.

Key Technical Issues

Anchor Cell Beam Access Requires Low Divergence and Collimation. Along with functional and performance requirements for anchor-cell beam injection, there are constraints. These constraints impose limitations on the solutions to beam access. Specifically, the salient constraints are (1) coil geometry, (2) TDF device stray magnetic fields, (3) plasma shape, (4) source position limited by device geometry, and (5) technology development. Figure 3-121 shows a plan view of the anchor cell (both the yin-yang coil pair and transition coil). Note that the coil geometry blocks the egress of the anchor pumping beam for the required 15-deg injection angle and Z-axis intercept. Figure 3-122 shows the limited ingress of the M-3 coils' vacuum-vessel opening. The sloshing beam requires injections at $Z = 9$ m and normal to the axis. The enlarged view (Fig. 3-121) near this injection point shows the opening set by the M-2 coil spacing and its vacuum vessel. For the transition pumping beam, there are at least three possible interference points between the drift-duct pass and the device. In addition, the proximity of the choke

Figure 3-120. Magnetic fields around anchor cell pumping beam.

Figure 3-121. Beam access constrictions.

Figure 3-122. beam acceptance angles.

coil makes it desirable to limit this beam's angular extent. Probably the most serious of the transition beam constraints is interference with the circular "I" beam providing structural support and a mounting surface.

Solutions Considered. During the preconceptual design study, a number of access solutions were investigated. First, anchor cell vacuum-vessel penetrations for the sloshing beam were considered. The two possible entries were (1) through M-2 and (2) between M-2 and M-3. Because the trapping fraction of the plasma is important to efficiency, the sloshing beam had to be positioned to pass through the broad dimension of the plasma, which entailed shooting it through the small radius of M-2. (Note that this solution also provides beam egress to an external beam dump.) The alternate solution, with the penetration passing between M-2 and M-3 at ~ 45 deg, yielded only a 5% trapping fraction.

In the case of pumping beams, both anchor and transition, the choice of vacuum-vessel penetration (injection angle and axial position) could be traded off with beam energy. For the anchor pumping beam, penetration through the exit port for the particle fan was deemed best. Positioning the neutral-beam injector in the plane of the fan assured a good trapping fraction. The 15-deg injection was chosen to limit the overlap with the sloshing beam. At this angle, the 80-keV energy was appropriate. Even at the 15-deg angle, beam width must be restricted to avoid interaction with the sloshing beam. (See Fig. 3-98.) The ingress through the M-3 small radius opening was only about 20-cm high. This also imposed restrictions on the beam width of the anchor pumping beam, but in the orthogonal plane. Hence, to meet access constraints on the anchor pumping beam, the beam must be nearly symmetrical and have a narrow beam width. Further, as noted earlier, a beam egress was impossible; therefore, the dump must be located inside the yin-yang pair. The 15-deg injection angle and Z intercept region (i.e., between $Z = 10.02$ and 9.2), along with the beam positioned in the Y-Z plane, dictates this dump location.

Three solutions were considered for the transition beam, each involving different beam widths. Initially, two sources were considered, each using divergence (~ 0.8 deg) sufficient to cover the $Z = 4.7$ to 6.0 intercept at the required injection angle of 30 deg. Here the beams were positioned in the Y-Z plane (the plane of Fig. 3-121's view) because it provided the greatest access in the orthogonal plane (X-Z). Also, the plasma being nearly

symmetrical over the intercept imposed no preferences (as in the sloshing beam case) from the standpoint of trapping fraction. A second approach used a single beam with ~ 1.5 -deg divergence, the advantage being that a second source could provide 100% redundancy. The beam crossover point in this case had to be at the plasma; this, in conjunction with the relatively wide divergence, required a wider drift-duct passage and, hence, greater interference with the vacuum vessel and its support ring. The third approach--a six-beam fan of small-beam-width beams--is shown in Fig. 3-121. Here the choice of beam crossover was based on making the drift-duct channel as narrow as possible. Consideration was given to moving this crossover back near the support ring, which would necessitate closer source spacing (less than 25 cm). This solution of a six-beam fan provided a better energy deposition pattern and reduced the loading on the choke coil by about 2:1 from the two-source solution (see Fig. 3-101). In the orthogonal plane (Fig. 3-123) the useful divergence is limited slightly in the vicinity of the T-1 coil pass and by the height of the plasma as it comes out of the choke coil. At a nominal source-to-plasma distance of 1200 cm, a 0.5-deg divergence is 20-cm high (X direction), which matches the plasma diameter. Increasing the divergence significantly beyond 0.5 deg would have a degrading effect on the trapping fraction, result in a larger transition beam-power requirement (already at ~ 3 MW), and increase the size of the beam dump or the load on the choke coil. The size of the beam dump is constrained by the access between the T-1 coil yoke and the shield wall of the central cell. Figure 3-123b shows a cost comparison of the approaches, which indicate only a 10% variation in cost.

Divergence Needs. This discussion of beam access indicates, in general, that the solutions required small beam widths. Small beam widths are achieved by using sources with low divergence augmented by collimation and by positioning them close to the plasma. As noted, source position is constrained by device geometry and the difficulties of shielding against the environment imposed by the device. The two major environmental hazards are stray magnetic fields and the particle fan in the end cell in which the pumping beams reside. The nuclear environment in the anchor cell is not severe. Although neutron flux levels are at least two or three orders of

A

B

Figure 3-123. (a) Beam power loss to to beam scraping. (b) Beam cost increases slowly with number of sources.

magnitude below those of the central cell, shielding is required. Maintenance is inhibited by activation. Radiation damage of beamline components or nuclear heating are not major concerns.

For the sloshing beam, the major concern is the stray magnetic field. This problem is mitigated by moving the beamline back, which increases beam extent at the coil constriction. Figure 3-120 shows the field strength to be about 1 kG for the source 6 m from the plasma. At this position, to clear the vacuum vessel walls of the anchor cell, the angular extent of the beam must be limited to about 1.4 deg (20 cm/820 cm) in the Y direction (see Fig. 3-117). This angular extent is satisfied by a 0.25-deg divergence. Magnetic shielding to achieve this beam width is essential. For example, with the 0.25-deg divergence source, the residue magnetic transverse field must be limited to less than 7 G.

The angular extent desired can be achieved by using a low-divergence (goal of 0.25 deg-1/e) source and collimation. Figure 3-123a shows the impact of scraping the beam with a collimator to achieve small beam widths. For example, to limit the angular extent of a 0.5-by-0.5-deg beam to 0.25 by 0.25, about 85% of the power is lost or, equivalently, the beamline must be made approximately seven times larger in power. The pumping beam would grow from 0.256 to 1.8 MW, with the cost increasing from \$2.0 to \$3.0 million.

The anchor pumping beam for the TDF is scraped by the ingress to the vacuum vessel enclosing M-3 (see Figs. 3-99, 3-112, 3-121, and 3-123). The source is positioned 16.2 m from this vacuum-vessel opening (see Fig. 3-97). With this opening enlarged to ~28 cm for beam passage, an angular extent of 0.99 deg (28 cm/16.2 cm) is the limitation in the X direction. A 0.25-deg-1/e divergence beam would have its 2/e contour coincide with this limitation, and only 14% of the beam power would be lost (see Fig. 3-123a) if the beam were symmetrical and scraped an equal amount in both planes. Scraping in the orthogonal plane (Y-Z) is done back at the beamline because the particle fan requires a slot-shaped opening in the vacuum vessel. As noted previously, this collimation is required to keep the anchor pumping and sloshing beams from overlapping in the plasma. While the pumping beam could be moved further upstream in the particle fan to reduce the beam extent, maintenance would become more difficult and fan particle flux would increase, creating high

loading on the neutral-beam enclosure walls. Figure 3-117 shows that the magnetic field would be no problem if it were necessary to move the pumping beam in closer.

Finally, the transition beam was required to have a divergence of 0.25 by 0.5 deg, based on the 1200-cm source-to-plasma distance and the plasma target dimensions. The magnetic field profile for this neutral-beam location is shown in Fig. 3-107. From the standpoint of magnetic field, the beamline could be moved up slightly, but not enough to make divergence requirements significantly easier. Maintenance would be slightly more difficult, and particle fan loading would increase slightly but is not a consideration.

Status of Low-Divergence Source Development. A recent source was built with a measured divergence of 0.25 by 0.9 deg, and a design for an ion source with a divergence of 0.25 by 0.25 deg is under development. Sources with divergences of 0.5 by 0.8 deg are more common, however. Developers are finding parameters such as cathode construction and the uniformity of ion-source plasma density important in achieving low divergence. Small sources with reasonable aspect ratios (height to width) $\sim 2:1$ are thought to offer lower development risk. Of the three source needs discussed, the anchor pumping beam is the most critical for divergence. It should be as close to a 0.25-by-0.25-deg divergence capability as possible. The other two sources specified at 0.25 by 0.5 deg could be relaxed somewhat in the wide dimension. As stated at the outset, the approach proposed is a combination of low-divergence optics in conjunction with collimation. If the divergence desired is not achieved fully, source size could be increased and a higher percentage of the beam could be scraped to achieve the equivalence of low divergence.

Direct Recovery Development Risk Is Worthwhile. In the foregoing discussion the ion dump for the transition pumping beam had to be designed to handle a heat flux of $7.23 \text{ kW}/(\text{cm})^2$. This was accommodated by setting the beam-dump surface at 6 deg with respect to the beam, thereby reducing the incident flux to $750 \text{ W}/(\text{cm})^2$. At this angle, however, the sputtering rate is 25 times higher than at broadside for the same heat flux. Dump life is estimated at 1/3 FPY. For all six beams, the full-energy power dissipated in

the ion dump is ~1.9 MW. A direct-recovery device would lower the heat flux to the ion dump by a factor of 10:1; it would also increase overall beamline efficiency from 51 to 61%. In doing this, the power dissipated at the dump is about 1.2 MW less, which allows the thickness of the beam dump to increase. The particle rate is expected to be about the same for the recovery and no-recovery cases; however, the energy is 10:1 less, which impacts the sputtering rate. Figure 3-124 shows the trade-off of beam-dump angle and expected life for both the recovery and no-recovery cases. Clearly, direct energy recovery, although it requires further development, is an improvement. A proof-of-principle experiment was run, and a report was written.²⁶ Figure 3-125 shows a schematic of the experiment for a 20-A-35-keV hydrogen beam. Recovery efficiencies of >80% were measured.

Magnetic Shielding Requirements for Low Divergence. Low divergence is critical to beam access in the anchor cell. Source divergence in the 0.25-to 0.5-deg range is required. On the basis of simple theory, we know that the residue magnetic flux inside the shield injector enclosure both steers and broadens the beam. A charged particle moving through a B field perpendicular to its velocity vector traces a circular path with a radius dependent on both particle energy and B field flux density. The circular path in the neutralizer is terminated when there is a charge exchange and the ion is neutralized and no longer under the influence of the field. This somewhat random event leads to a scattering of the normally low divergence beam with a nonzero mean. Although the steering error (bias) is easily compensated for, the divergence degradation must be tolerated. A quantitative modeling of this distortion was done by J. Conrad.²⁷ Figure 3-126 shows the dependence on beam energy and flux density to contain the beam distortion to within 20%. The residue fields perpendicular to the beam must be less than 4 G for the 80-keV beam energy.

Drift-Duct Design Optimization. For multiple source beamlines, drift-duct optical efficiency depends on geometry and beam divergence. The drift-duct cross section of a continuous duct is made smallest if beams from the sources cross over in the center of the drift-duct length. This minimum cross section duct reduces neutron and plasma impurity streaming into the beamline and

Figure 3-124. Ion-dump characteristics.

Figure 3-125. Direct-recovery configuration.

Figure 3-126. Divergence degradation from magnetic fields.

minimizes TUF vacuum-vessel penetrations. For the anchor cell, the drift duct is not continuous and is composed of the beamline drift duct and the anchor cell vacuum-vessel penetrations. Each of the three neutral-beam injector interfaces is different. In the case of the six-source transition beam, the width of the beam fan is restricted as it passes the vacuum vessel even though no formal duct is constituted by the vacuum-vessel penetration. Here the beam crossover is at the center of this restricted passage. For the anchor cell pumping and sloshing injectors, the beam crossover is at the plasma. The rationale here was that redundant sources must illuminate the same plasma target, which is only possible with the crossover at the target, not upstream in the drift duct. Further, the main constriction for the pumping beam is the penetration of the vacuum vessel, which is 1620 cm from the source but only 300 cm from the crossover at the plasma. The crossover of operating and redundant source beams was not placed in the center of this constriction because the illuminated region of the plasma would be source-beam dependent and the beam dump extent would be enlarged. Because the two source beams are in the plane of the particle fan, which already requires a slot-shaped egress, little impact was incurred by shifting the crossover. The sloshing beams' sources are also aligned with a slot-shaped penetration in the vacuum vessel, in this case that of the plasma pumping channel. Moving the source beam crossover would have little impact on the sloshing beam injector's drift duct, which is a small portion of the total drift-duct extent (i.e., injector drift duct plus vacuum-vessel penetration). With the crossover at the plasma, both beams illuminate the plasma target and the beam-dump extent is smaller than it would have been with the crossover at the drift-duct center (see Fig. 3-121).

Neutral-Beam Injector Sizing for Acceptable Availability. A steady-state device such as the TDF inherently requires high availability subsystems. Total duplication of all subsystems is prohibitively expensive. Selected redundancy can meet the availability requirement in a cost-effective manner. The first ground rule is that the injector itself (e.g., structure, shields, vacuum boundary, power supplies, auxiliary/support equipment, etc.) has a long mean time between failures (MTBF). Second, higher failure rate components can be oversized and/or duplicated. Finally, complete loss of any

function is a serial failure; that is, the TDF would be down. Partial failures can be sustained in most cases. Degradation is graceful in that small reductions in plasma heating power in a given region do not have a catastrophic effect. Some preliminary investigations (failure-effects analysis, in a sense) support the latter conclusion. For example, the transition pumping function was found to be quite forgiving of small changes in power and illuminated region.

No quantitative allocation of availability was assigned to the anchor cell neutral-beam injectors. The approach taken was to oversize the sources by ~20% in all injectors. Because the power requirements of the anchor pumping and sloshing beams are so small, the provision of redundant sources would have little impact. Power supplies would be switched between sources. For the transition pumping beam, the six-beam fan has inherent redundancy. With five of six sources running, the impact on performance should be acceptable. Finally, the cryopumps are oversized by at least 20% so that five of six sections can provide full operation while the sixth panel/section is regenerated.

3.4. MICROWAVE SYSTEMS

3.4.1. Overview

The two microwave systems used as a thermal barrier in the TDF differ only in power level and frequency. Figure 3-127 shows the position of the microwave ports (two sets per system) in the anchor cell. Both input and output ports have been provided since only 50 to 75% of the power is trapped (single-pass absorption) by the plasma. Launchers, fitted to receptacles that are inserted into the input ports, are supplied by circular waveguide. All remaining components are located outside the reactor hall in a "hands-on" area. In the discussion that follows three issues are broached, namely (1) component availability, (2) window survivability, and (3) power control.

3.4.2. Requirements

Table 3-43 summarizes requirements for the two microwave systems, one at 35 GHz, the other at 60 GHz. A total power (for both ends of the device) of 500 kW at 35 GHz and 676 kW at 60 GHz is required. Injection is at 9.5 m and 10.5 m along the z-axis. Mode control is required, with the ϵ -vector oriented perpendicular to the magnetic flux. The neutron flux must be specified to determine the suitability of rf window material and the need for nuclear shielding. Cyclotron resonance can occur inside the waveguide. If the window (vacuum break) is too far from the plasma edge, where the magnetic field supports resonance at the transmitted frequency, breakdown can occur. Magnetic flux data at the 9.5- and 10.5-m injection points indicate that resonance is possible behind the launcher. This necessitates positioning the window close to or within the launcher.

3.4.3. Design Description

Section cuts of the design layout shown in Fig. 3-128 were made at the microwave injection points. Each 10-cm-diam input port enters the anchor cell at ~45 deg with respect to either the x or y axis and is fitted with a mounting flange at the outside of the vacuum vessel. A launcher receptacle

Figure 3-127. ECRH/anchor cell interface.

Table 3-43. Summary of anchor cell microwave requirements.

Parameter	System 1	System 2
Frequency (GHz)	35	60
Power (kW)	500 ^a	676 ^a
Position in Z (m)	9.5	10.5
Mode	-- ^b	-- ^b
Pulse length (s)	5.5	5.5
Absorption ^c (%)	~50	~50
Neutron flux (W/cm ² -s)	~3 x 10 ⁸	~3 x 10 ⁸
Magnetic flux (T), X-Z/Y-Z at launcher	~2.6/0.3	~0.3/2.8

^aTotal (both ends).

^bExtraordinary.

^cSingle pass.

Figure 3-128. Cross section of anchor cell vacuum vessel at ECRH launcher.

inserts into the port and is secured to the mounting flange by a ring. This arrangement facilitates maintenance and provides support for the launcher assembly.

Figure 3-129 is an elaboration on the launcher assembly, which is composed of a short section of smooth, overmoded circular waveguide, a cooled BeO window, and mounting flanges to interface with the launcher receptacle and transmission lines. Floracarbon-100 (a trade name) cools the window. This window is tailored after the Varian window design for the gyrotron. The mode compatibility of this window with the HE_{11} mode is a component detailed design problem that, given a little development, is assumed to be tractable. A short section of corrugated circular waveguide is provided between the window and the transmission line disconnect, which is mounted to the ECRH receptacle. The entire transmission line is pressurized to about 1 atm of sulfur hexafluoride (SF_6) and cooled with a water pipe wrapped about the outside over the portion within the anchor cell.

The egress ports shown in Fig. 3-128 act as dumps for the refracted beam of the launcher. After the untrapped power passes through the egress, either absorbing or reflecting surfaces are needed to prevent returned power. The objective of these "dumps" is to minimize reflections off the vacuum-vessel walls (inside the plasm chamber) that might inject power at spurious locations. Their success depends on the vacuum-vessel geometry, magnetic fields, and plasma characteristic, the last two of which have an impact on the degree of beam refraction. A rule of thumb for estimating this impact is to compare the plasma to cyclotron frequency. If the ratio is less than say 0.2 to 0.1, the refraction of the ECRH beam should be minimal. This appears to be the case for the anchor cell; therefore, the egress port is expected to be an effective dump. Application of a ray-tracing code is needed to confirm the degree of refraction.

Figure 3-130 shows details of the transmission system from the launcher to the high-power oscillator (gyrotron), where undesirable modes are filtered out by the mode filter. The mode is then converted from the desired mode (probably TE_{02} or TE_{03}) to the HE_{11} , which is transmitted through the low-loss corrugated waveguide. A high-power switch and dummy load provide a local checkout capability, and three separate SF_6 systems provide double protection against tritium release during an accident. The monitoring

Figure 3-129. ECRH launcher detail.

Figure 3-130. ECRH transmission system.

provisions shown are, specifically, the arc detector and directional coupler, which report conditions at the gyrotron output, and another coupler at the launcher end, which provides samples' forward and reverse power level to estimate injected power and launcher VSWR. The transmission line is a 2.5-in.-i.d. corrugated waveguide.

The block diagram in Fig. 3-131 shows the equipment and support system interfaces required for one end of the TDF. Table 3-44 summarizes the system parameters. This equipment provides 250 kW of 35-GHz and 338 kW of 60-GHz microwave power; the prime power comes in at 13.8 kV for the high-voltage system. Table 3-45 gives the supply parameters for a single gyrotron oscillator. Each gyrotron has its own modulator to provide individual control. The cooling requirements for the launcher and transmission line internal to the anchor cell are small (on the order of 10 kW). A fiber-optics system is used to transmit, monitor, and command signals between the processor and the translators located at the control or monitoring sampler. Interface with the main instrumentation and control (I&C) of TDF is by means of a fiber-optic buss. The monitoring and control processor has a distributed architecture, receiving and transmitting microlevel commands to this I&C buss. Microscheduling is done in the processor and transmitted to the controllers. Raw monitoring data are smoothed and formatted for local diagnostics or transmission to the I&C.

Figure 3-132 shows transmission-system loss estimates. Most of the larger contributors to this loss are associated with mode-control components. At present, there are few data on the mode mix at the gyrotron output. The only data taken were cold test data on the 60-GHz tube, indicating that >90% of the output would be at the desired mode. The 0.3-dB loss corresponds to a 93% mode purity from the tube, a total transmission loss of 1.7, or 0.33 dB. For the 35-GHz system, the tube output is assumed to be 200 kW. At the launcher, the power would be 135 kW, or 270 kW total compared to the 250 kW required. Similarly, the 60-GHz tube is assumed to output 275 kW, which gives a power of 185 kW per tube at the launcher, or 370 kW total compared to the 338 kW needed. Gyrotron efficiency is expected to be approximately 45%. Using this figure, the 0.33-dB transmission loss, and an assumed 5% loss in the modulator power supply gives an overall efficiency of ~28%.

Figure 3-131. ECRH system block diagram.

Table 3-44. Summary of system parameters (based on end values).

Parameter	System 1	System 2
Frequency (GHz)	35	36
Power (kW)		
Generated	400	500
Injected	250	338
Prime power (kW)	944	1285
Efficiency (%)	28.6	28.6
Gyrotrons	2	2
Launchers	2	2

Table 3-45. Supply parameters for gyrotron oscillator (based on 60-GHz tube developed by Varian).

Beam voltage (kV)	80	LHe (l/h)	~1
Beam current (A)	8	LN ₂ (l/h)	~0.5
Gun-anode voltage (kV)	20	Water (gpm)	TBS
Heater voltage (V)	12	Oil (gpm)	TBS
Heater current (A)	2		

ORNL-DWG 81-3342A FED

*ALL LOSSES
IN dB's

Figure 3-132. ECRH loss summary.

Component Availability. The least available major component is the high-power gyrotron. Long-pulse (hundreds of milliseconds) and cw versions of the 28-GHz gyrotron are presently available. At 35 GHz, medium- (tens of milliseconds) and short-pulse (less than 1 millisecond) tubes are available from Thomson CSF. The Naval Research Laboratory (NRL) developed a 35-GHz, short-pulse, 70-kW tube and is now working on a 35-GHz, 1-MW version that provides hundreds of kilowatts but a short pulse. A 35-GHz cw tube is clearly feasible but needs development. A scaling of frequency from the 28-GHz version is a possibility. A scaling effort to produce a 56-GHz tube from the 60-GHz design is currently underway.

Development of the 60-GHz cw tube is proceeding nicely. One vendor, Varian, has operated a tube at 124 kW for up to one hour. This tube was demonstrated at well over 200 kW (its required power output) for short pulses. Hughes, another vendor, has reached 280 kW at short-pulse durations and additional low-power or beam-only tests at tens of milliseconds. Short-pulse, high-frequency (>90 GHz) tubes have also been built and tested. The 60-GHz, 275-kW cw tube is not available today, but should be obtainable by the time TDF requires it.

Other components have limited availability. Transmission systems in which the mode is controlled have been developed at GA Technologies (28 GHz in the past, 60 GHz currently) and are under development at Princeton (60 GHz) as well. The high-power switch needs development. Other fusion microwave systems have similar needs; therefore, the components should be available for TDF. The mode purity of these devices will be important in achieving the aforementioned transmission efficiency.

Window Survivability. In the design description the window was positioned facing the plasma. Normally this position would be of concern because of neutron /radiation damage and the possible deposition of impurities from the TDF. The radiation damage threshold is $\sim 10^{20}$ n/cm². With a neutron flux at the wall of $< 10^{10}$ n/(cm)²-s, 10^{10} s or ~ 300 FPY would be needed to reach a damage level--hardly a concern. The planned window will be recessed about 5 cm to reduce the deposition of impurities. While it would be desirable to move the window back further in the guide, there is concern about forming a plasma in the guide. Because of the high magnetic fields in the

vicinity of the launcher, it is possible to have cyclotron resonance at the driving frequency (35 or 60 GHz) if the vacuum break is positioned outboard from the resonance surface.

Power Control. Mode control, which is expensive in terms of transmission system loss, cost, and complexity, is needed to control power deposition in the transmission system. A mixed mode system can have a transmission loss of less than 10% compared to the 33% estimated for the TDF microwave system. However, unknown and/or spurious modes can reduce power-handling capability on the transmission system and cause power deposition in unwanted portions of the plasma. The result is lower effective heating in desired portions of the plasma. Overall, however, the mode-controlled system may have higher efficiency.

Power absorbed by the plasma vs that of the cavity (plasma chamber) walls and its opening is another part of the power-deposition control problem. This problem has been modeled using a power-balance approach. Power transmitted to the plasma is:

$$T_{\text{plasma}} = 1 - \text{losses} \quad (3-76)$$

$$\text{Losses} = Q_{\text{loaded}}/Q_{\text{unloaded}} \quad (3-77)$$

$$Q_{\text{loaded cavity}}^{-1} = \text{power in/w} - \text{energy stored} \quad (3-78)$$

$$= 1/Q_{\text{plasma}} + 1/Q_{\text{apertures}} + 1/Q_{\text{cavity walls}} \quad (3-79)$$

where

$$Q_{\text{aperture}} = \frac{8\pi \text{ volume of cavity}}{\text{Wavelength} \times \text{area of aperture}}, \quad (3-80)$$

$$Q_{\text{unloaded}}^{-1} = 1/Q_{\text{aperture}} + 1/Q_{\text{cavity walls}}, \quad (3-81)$$

$$Q_{\text{plasma}} = \frac{-4\pi \text{ length of cavity in wavelengths}}{\ln [1 - \text{single-pass absorption}]}, \quad (3-82)$$

$$Q_{\text{cavity walls}} = 3\text{-cavity volume}/28\text{-cavity wall surface area} , \quad (3-83)$$

$$\delta = \text{skin depth} = 1/\sqrt{\pi f \mu \sigma}, \quad (3-84)$$

where μ is permeability, f is frequency, and σ is conductivity.

The power transmitted to the plasma was estimated at about 72%, based on a single-pass absorption of 0.5. This means that 28% of the 370 kW of 60-GHz and 270 kW of 35-GHz power is either absorbed or reflected by the cavity. Hopefully, most of this 180 kW is carried away by the dump ports opposite the injection ports, but this depends on the amount of microwave beam refraction as it passes through the plasma. The more exact solution is to do a ray-tracing analysis for the plasma magnetic fields and cavity geometry. Another approach would be to evaluate the impact of microwave power reflected to other points in the plasma. It is possible that the impact is slight and that a more exact analysis of power flow is not required.

The power-balance approach is expedient but gross. The cavity geometry is very complex. The power injected into the plasma is beamed or focused not omnidirectional, hence more power should be absorbed by the dump ports than by any port of a simple cavity. Clearly more investigation is needed.

3.5. LOW-ENERGY DEUTERIUM INJECTION

The systems defined in this section inject low-energy deuterium into the machine to provide (1) initial gas fill, (2) a plasma target for the anchor cell neutral-beam injectors during the startup scenario defined in Table 3-46, and (3) low-energy ion-flow stabilization during operation. All nascent high-energy ions, both deuterium and tritium, are supplied with neutral-beam injectors described in Section 3.3.

Before startup (see Table 3-46), the vacuum vessel is pumped down and backfilled to a few mTorr of hydrogen when performing glow-discharge cleaning. Gas injectors, also known as gas puffers, perform this function. After glow-discharge cleaning is completed, plasma guns located on the end walls inject a low-energy plasma into the anchor cell to provide a target for the associated neutral-beam injectors. About 0.1 s after the initial startup phase, the gas injectors are phased out and pellet injectors are phased into operation. Frozen pellets are injected into the central cell to establish the stabilizing low-energy ion flow through the mirror-plug region needed for continuous stable operation. The requirements and design features of these three types of deuterium injectors are described in detail below.

3.5.1. Gas Injectors

Summary. Deuterium gas injectors, also known as gas puffers, will inject into three cells: the left anchor cell, the central cell, and the right anchor cell. During glow-discharge cleaning and startup, deuterium gas will be injected into all three cells.

Two independent gas injector systems will be provided, either of which can independently meet the requirements. The use of components proven in gas-puffing systems of present-day fusion experiments and the two-system redundancy ensure high system availability. The shelf life of the equipment is the driving factor that determines system reliability.

Table 3-46. TDF startup scenario.

Topic	Operation	Equipment
(1) Initial surface cleaning (or after major open maintenance)	Glow-discharge cleaning about 100 hours	<ul style="list-style-type: none"> ● Insertable anodes in each anchor and in central cell. ● 1-kV power supply for slow discharge of a few $\mu\text{A}/\text{cm}^2$ at wall. ● H_2 fill and pumping system for a few mTorr pressure with ≤ 1-hr gas-recycle times.
(2) Plasma buildup	Initial phase about 10 ms	<ul style="list-style-type: none"> ● Two or more MFTF-B type 4-cm-i.d./15-cm o.d. plasma stream guns at each end wall. ● Locate guns to map in along B to C cell only as needed during first 10 ms. Each gun injected about 20 Torr-liter/s D_2 plasma (about 226 A equivalent) at a few eV average energy.
	Tandem ϕ about 20 ms	<ul style="list-style-type: none"> ● Bring on 28 GHz ECRH power (≤ 100 kW) in each anchor). ● Add about 10 Torr-liter/s D_2 for each anchor (rough estimate). ● Add about 15 Torr-liter/s D_2 for the central cell. ● Sequence in sloshing NBI in anchor.
	Barrier formation about 100 ms	<ul style="list-style-type: none"> ● Add main 56-GHz ECRH power (≤ 1 MW total). ● Increase D_2 in central cell to about 30 Torr-liter/s. ● Add startup (about 100 A at 40 kV) NBI in C cell possibly from derated 80-kV spare NBI set. ● Sequence in part of pump NBI in transition sections. ● Reduce anchor gas feeds to zero.
	Barrier equilibrium operation about 100 ms	Sequence in 80-kV NBI to full power; adjust pump NBI and pellets for max n_r , wall loading Γ , and stability.

Requirements.

1. Deuterium gas injectors will be provided to control pressure in the vacuum vessel before and during the initial startup phase.
2. The central cell and end anchor cells will each be provided with two independent gas injectors.
3. Sufficient system redundancy will be provided to ensure operation when one injection system is out of service.
4. Two operating modes will be provided, one for pulsed flow and one for modulated flow of the deuterium gas.
5. Gas flow rates will be sufficient to backfill the vacuum vessel and establish the desired deuterium vacuum pressure.

Design Description. The gas flow rate depends on the size of the vacuum vessel, the capacity of the vacuum cryopumps, and the time required to backfill the vessel to the desired vacuum pressure. Flow rates given in Table 3-47 are ultraconservative inasmuch as they are more than the pellet injector and neutral-beam injector rates combined during steady-state operation. The high flow rates permit rapid backfilling of the vacuum vessel if necessary. These rates can be reduced greatly by adjusting the upstream pressure regulator, Pd, and the flow valve controller, FVC, shown in Fig. 3-133.

Referring to the deuterium gas flow-system diagram of Fig. 3-133, we see that there are three categories of gas injection system equipment: the gas supply, control valves and injectors, and the I&C for measuring and controlling gas flow. Because of interfacing, some fuel-recovery and fuel-processing equipment is also shown. LAC, CC, and RAC are mnemonics for left anchor cell, central cell, and right anchor cell, respectively. The deuterium gas supply system is shared with the deuterium neutral-beam injectors and the pellet injectors. Several types of control valves are needed: remote-shutoff valves (RM), pressure-regulator valves (PRV), modulated flow-control valves (FCV), and pulsed selector valves (PSV) that connect to the injector nozzles. The I&C components consist of pressure regulators (Pd), flow-valve controllers (FVC), programmable satellite computers, and the manual/auto (M/A) transfer station. For the pulsed flow-control mode, the pressure regulators and flow-control valves are preset; then, the PSV are opened and closed as commanded by the programmable control.

Table 3-47. Gas injection requirements/design data.

Gas injection systems	2
Central cell injection nozzles located near pellet injection tubes	2
End cell injection nozzles	4
Left-end anchor cells	2
Right-end anchor cells	2
Total maximum gas flow rate (g/hr)	360
To central cell	160
To each end cell	100
Gas composition (% D ₂)	>99
Operating control modes	
Mode 1	Pulsed
Mode 2	Modulated

Fig. 3-133. Flow diagram of deuterium gas system.

For the modulated flow-control mode, the pressure regulators are preset, the desired PSV are opened as needed, and the FCV are controlled to provide the desired modulated flow.

Key Issues. Gas-puffing systems are well developed for use with present-day experimental fusion machines. The pulsed selector valves and FCV should be close to the injector nozzles so that the entry of gas into the vacuum vessel is not delayed significantly. Large-diameter lines between the PSV and injectors reduce delay time, but may introduce radiation streaming problems unless the lines follow a longer contorted path that, in turn, increases delay time.

3.5.2. Plasma Gun Injectors

Summary. Two or more MFTF-B plasma stream guns will be located on each end wall for plasma field mapping and to provide a target plasma for the anchor neutral beams during the initial 10 ms of startup. The plasma gun assemblies will be modified so that they do not retract when dormant (not in use). A movable tungsten shield will close to provide nuclear radiation shielding when the guns are dormant. The plasma guns will be located about one meter off the axis of the machine, in the plane of the plasma fan at the end walls. Openings between the particle and dumps must be sufficiently large (about 1/2 m in diameter) to accommodate the expanding plasma when it emerges from the guns.

Requirements. Except for additional nuclear radiation shielding, the detailed performance requirements for the plasma guns will be the same as those for MFTF-B. The general requirements for the plasma guns are given in Table 3-48. If the guns are reliable enough, only two are needed on each end wall. Otherwise, additional plasma guns are needed as quick replacements for failed units. Most of the time, the plasma guns are dormant. Their shelf life under limited radiation exposure is the driving reliability factor.

Cooling for the radiation shielding must be provided on a continuous basis. The movable shield must not degrade the performance of the plasma gun when it is in the fully open position. Air-cooling of the gun assembly will

Table 3-48. Startup plasma gun requirements.

-
- At least four startup plasma guns will be provided to create a low-energy plasma target for neutral-beam injection into the anchor cells.
 - The plasma guns will use the design developed and tested for MFTF-B.
 - At least two guns will be located on each end wall, about 1 m off the machine axis, and oriented in the plane of the end particle fan.
 - Enough redundancy will be provided for assure operation with one plasma gun out of operation.
 - Each plasma gun will be independently replaceable.
 - The plasma guns will be protected from radiation dosage and heat damage at all times.
 - Movable radiation shielding must not interfere with plasma gun operation when in the open position.
 - Movable shield open and close time will be about one minute.
 - Performance will be the same as specified for the MFTF-B plasma guns.
-

be provided even though the heat capacity of the components is large enough to absorb the energy dissipation of a single 10-ms shot without damage.

Design Description. Plasma gun design and interface data are given in Table 3-49. The plasma gun arc is the source of low-energy electrons that ionize deuterium gas injected before the pulsed line discharge. Plasma density in the gun is relatively high, but is reduced about two orders of magnitude at the anchor cell by means of magnetic expansion.

Figure 3-134 is a schematic diagram of the plasma gun, and Fig. 3-135 is a connection diagram to the power supplies and gas systems. The air-cooling system will be needed when the plasma guns are tested and for initial flux-mapping operations. The pulsed arc is formed along the magnetic field lines between the molybdenum cathode and anode washer stack. An auxiliary pulsed magnet can be positioned to control plasma flux mapping. A pulsed piezoelectric valve is used to admit the deuterium gas to the plasma gun.

The arc contains roughly 2 kA of low-energy electrons that produce about 250 A of plasma flow from the gun along the magnetic field lines. A relatively low-voltage (~1-kV), pulse-forming network power supply provides a 2500-A pulse to the gun. Likewise, the magnet power supply consists of a 16.7- μ F capacitor bank discharged to provide 1760 A to the auxiliary magnet that produces a 3-T magnetic field in the gun. Energy stored in the magnet power supply is about 2.5 times the energy stored in the arc power supply.

Figure 3-136 shows a sectional view of the shielded plasma gun housing assembly, including the rotatable muzzle shield and actuator. The heavy eccentric muzzle shield will require considerable drive torque for positioning, but its opening and closing time is about one minute so that a worm gear box can be used to multiply the torque of a fractional horsepower motor. The fixed-position nuclear shielding is water-cooled. The movable shield may be cooled by radiation and conduction, or it may be water-cooled separately as described below.

Key Issues. Cooling of the movable radiation shield is the major design consideration because the peak heat load on the shield surface is about 300 W/cm². There are at least two possible solutions to the problem. One solution is to spring-load the drive shaft so that the backside of the

Fig. 3-134. Schematic sectional view of one of four startup plasma guns.

Fig. 3-135. Plasma-gun supply system diagram.

Fig. 3-136. Sectional view, showing the location of the TDF startup plasma gun assemblies (two guns on each end cover).

Table 3-49. Plasma-gun design and interface data.

Guns (2 on each end-cell cover)	4	Cooling air flow (Torr-liters/s)	TBD
Location	$r = \pm 1$ m	Magnet pulsed-power supply	
Current/gun (A)	2000	Amps (kA)	2
Arc electron temperature (eV)	5 to 250	Volts (kV)	2.4
		Power, 10 ms (MW)	4.8
Plasma		Accel cathode pulsed power supply	
Pulse length (ms)	10	Amps, 10 ms (kA)	2.5
Pulse flow (A)	250	Volts (kV)	1
Ion temperature (eV)	10	Power, 10 ms (MW)	2
Density (cm^{-3})	10^{13} to 10^{14}	Energy (kJ)	20
Deuterium gas flow (Torr-liters/s)	22		

tungsten is in firm contact with a thin graphite plate secured to the water-cooled stainless steel plate. The graphite reduces the sliding friction and transmits heat from the tungsten to the stainless steel. Another solution is to provide water-cooling channels down the hollow drive shaft and water-cool the tungsten shield. Flexible hydraulic hoses connecting the drive shaft to the water supply lines must allow for 180-deg rotation of the shaft. In this case, the tungsten cap shield is secured to a water-cooled chromium-copper base plate that is secured to the drive shaft.

3.5.3. Pellet Injectors

Summary. To stabilize the plasma during continuous operation, small frozen deuterium pellets are injected into the central cell plasma to provide the low-energy part of the plasma spectrum. As they penetrate into the plasma, the frozen pellets ablate. Upon heating, the pellet ablation gases interact with high-energy ions and become a source of low-energy deuterons that help stabilize the plasma.

For reasons given below, many small frozen deuterium pellets are injected into the plasma with a velocity >1500 m/s to provide a quasi-continuous flow of low-energy plasma that supplements the high-energy plasma flow through the central cell mirror plug.

Centrifugal pellet injectors now under development can be modified to meet the requirements defined below. Two dual-channel centrifugal injectors are proposed, one at each end of the central cell. These injectors will provide adequate pellet penetration and not interfere with test-module installation and removal. Although injection into the small neck of the central cell plasma is desirable for enhanced pellet penetration, the plug coils and beam-dump obstructions make injection into the thick part of the plasma neck more desirable.

Requirements. A summary of the pellet injector design criteria is given in Table 3-50. These criteria are the basis for the pellet-injector data requirements given in Table 3-51. To prevent large variations in plasma density, the number of atoms contained in the pellet mass must be no more than 10% of the ion population in the central cell. This limits the maximum pellet

Table 3-50. Pellet-injector design criteria.

- All stabilizing warm ion flows will be supplied with deuterium pellet injectors.
 - Pellets will be injected in the central cell region only, preferably near the ends of the cell, where the plasma penetration is greatest.
 - Pellet mass will be limited to 10% of the ion population of the central cell.
 - Pellets will have sufficient injection velocity to penetrate the plasma at least 30% of the plasma radius so that warm ions exist in the largest part of the plasma volume.
 - Sufficient pellet redundancy will be provided to assure operation with one injector out of service.
 - Pellet injectors will be located so that they do not interfere with the removal and replacement of other components nor degrade their performance.
-

Table 3-51. Pellet injector requirements.

Pellet size (g)	34	Magnetic field in vicinity of pellet injector (T)	0.2
Pellet diameter (mm)	0.6	Pellet velocity (m/s)	≥ 1500
Injection point, Z (m)	± 3.35	Pellet rate (s^{-1})	500
Injector distance to plasma (m)	5.4	Reliability	≥ 0.90
Scatter-cone half angle (deg)	< 0.5		

diameter to 0.6 mm. The analysis in Section 2.3.2 shows that a pellet velocity >1500 m/s will satisfy the minimum requirements. The velocity-penetration relationship is highly sensitive to the plasma electron temperature. Some velocity reserve is needed to accommodate the uncertainty in the plasma electron temperature. A pellet injector program is currently underway to achieve pellet velocities near 2000 m/s.

Design Description. Centrifugal injectors are needed to meet the high-injection pellet rate requirement. Centrifugal injectors now under development are potentially capable of meeting the requirements if high reliability (>95%) is not a requirement. To improve system reliability, two channel pellet injectors (Fig. 3-137) are needed so that one pellet injector can provide the required pellet rate if the other one fails. Normally only one of the two pellet channels would be in operation, with a pellet rate of 250/s. When one injector fails, the second channel in the other injector will be activated so that one injector supplies 500 pellets/s.

The design data for the centrifugal pellet injectors are given in Table 3-52. Some variation in arbor speed is provided so that the pellet velocity and rate can be varied over a limited range. The top speed corresponds to 2000 m/s. A pellet collimating tube is needed to limit the maximum half-cone scatter angle to 0.5 deg. This technique has been demonstrated with Teflon tubing at lower pellet velocities.

There are six types of interface connections to the pellet injectors, four fluid tube lines, electrical cable, and mechanical interface connections to the supporting structures and isolation valves. When a pellet injector fails, the isolation valve is closed, the injector is purged to remove deuterium and tritium, the supply-line valves are closed sequentially, and the electrical power is turned off.

An outline drawing of the pellet injector is shown in Fig. 3-138. The injector weighs about 1000 kg, excluding support structure. The isolation valve assembly consists of two rotary isolation valves, with provision for vacuum removal of leakage gas through the valves. The assembly also contains sensors for measuring pellet rate and pellet velocity.

Figure 3-137. Two-channel pellet injector flow diagram (one of two).

Table 3-52. Centrifugal injector data.

Two-channel injectors	2
Arbor speed (rpm)	27,000-36,000
Pellet velocity (m/s)	1,500-2,000
Pellet rate, max (s^{-1})	450-600
Scatter cone half angle with collimating tube (deg)	<0.5
Interface connections/injector	6 types
Liquid helium (g/hr)	360
Liquid nitrogen (g/hr)	1800
Deuterium (g/hr)	75
Vacuum (Torr-liters/m)	500
Electrical, start/run (W)	1500/750
Mechanical	?
Approximate weight (kg)	1000
Approximate space of cylindrical envelope (m)	
Diameter	1.00
Length	1.40
Auxiliaries/injector isolation valve	2
Control instrumentation	6 sensors
Pellet velocity	2 channels
Pellet rate	2 channels
Pellet mass	2 channels

Figure 3-138. Outline of centrifugal pellet injector, showing principal features and interface connections.

The fluid supply lines connect to the pellet former of the injector. The drive motor and its electrical connectors are located at the other end. The ends of the injector should be readily accessible for equipment servicing and maintenance.

Key Issues. Pellet injectors have not yet been developed to meet the high-velocity, high-pellet-rate requirements. Continuous operation under these conditions could present reliability problems. Considerable reliability testing will be needed to assure high pellet injector availability.

The injectors are mounted in an awkward position, as shown in Fig. 3-139. The details of the supporting structure and alignment provisions have not been developed. The pellet injector tube, shown in Fig. 3-140, must not interfere with either the plug coil or the beam-dump penetrations, nor must it allow excess nuclear radiation into the S-1 coils. To meet these requirements, the injector has to be mounted at an unusual angle relative to the machine. It cannot be readily supported from the floor of the building. These considerations could present maintenance problems in obtaining access to and aligning the injectors.

Figure 3-139. Sectional view of TDF reactor, showing the location of one pellet injector and its mechanical interface.

Figure 3-140. Dual isolation-valve assembly and the pellet drift-tube extension.

3.6. VACUUM VESSEL

3.6.1. Main Vacuum Vessel

The main vacuum vessel is basically a skin that wraps around the outside of the entire machine. It is made up of a series of plates welded together, with penetrations at various places for beamlines, test modules, and other services for the reactor vessel.

Together with the bulk shielding, the vacuum vessel performs several functions. First, it provides a vacuum containment for the plasma chamber and the superconducting magnets. It also supports the magnet thermal shielding and aids in structural support of the magnet cold mass. As mentioned earlier, the vacuum skin must provide access ports for component accessibility and feedthrough. Finally, it provides structural support for test-module installation and the neutral-beam dumps.

Essentially only two things are between the plasma and the outside: the shield and vacuum-vessel skin. Thus, there are two locations for the vacuum vessel--one inside the shield and one outside the shield. A trade study was performed at General Dynamics to determine which location would provide the optimum configuration. In summary, the study concludes that "...the baseline vacuum vessel should be located external to the shield.

It was decided that the central cell solenoid magnets should have their own vacuum vessels to eliminate warming the magnets every time maintenance or service is required on the TDF machine.

Because of the large intermagnetic forces that act to pull the solenoids inward, massive intercoil support beams were needed to support the magnets. Since the beam must go from cold mass to cold mass, this would provide an undesirable heat leak. To eliminate the heat leak, the magnet vacuum vessel and LN_2 thermal shields were extended to cover the intercoil support beams as well.

When connected, the central cell neutral beamlines will also be part of the main vacuum volume. Because they will be installed with their own vacuum vessel, an interface will be provided to seal the two vessels together. This interface will have to provide structural support for the neutral beamlines as well.

The other major interface connections to the vacuum vessel are the test modules. These modules are in six locations between the central cell solenoids and have, perhaps, two actual modules per penetration. This interface will have to provide all of the structural support for the modules, as well as for the vacuum. A seal design employing a separate vacuum and structural connection is used to eliminate some problems that arise from this type of interface. A cross section of this configuration is shown in Fig. 3-141.

3.6.2. Manufacturing

A comprehensive manufacturing plan will be required for the manufacturing, installation, and test phase of the TDF vacuum vessels.

Fabrication

Magnets. The magnet vacuum vessels will be comprised of three welded subassemblies.

1. On the first subassembly, the inner-radius-ring welded assemblies will be made up in four quarters from rolled 1/4-in. x 5-ft x 12-ft, 316 CRES plate, welded together to form the required diameter.
2. On the second subassembly, the outer-radius rings will be subassembled in a similar manner from 1/2-in.-thick rolled plate.
3. On the third subassembly, the side subassemblies will be fabricated from segments, approximately eight per side, plasma arc cut from 1-3/4-in.-thick plate stock for the S-0 magnet and from 2-in.-thick plate for S-1. The segments will be welded together to form a complete annulus.

The inner- and outer-radius rings will be welded to a side annulus plate to form a channel cross section. This welded subassembly is made ready to receive the magnet cold mass.

End Cells. The end cell vacuum vessels will be fabricated in welded subassemblies limited by transportation and handling capabilities.

The entire end cell vacuum vessel will be built up "in plant" with jig and fixture shoring-type supports (tooling). Operations include cutting,

Figure 3-141. Test module seal.

shearing, rolling, bending, heat-treating, and grit blasting of the 1- to 2-in.-thick plate. After the subassemblies are completely welded and inspected, the tack weld that held them in the completed assembly will be cut away and the subassemblies will be prepared for shipping. The planned size of these subassemblies is about 10 x 15 x 60 ft. Each subassembly is scheduled to weigh less than 90,000 lb.

Central Cell and Anchor Cell. These vacuum vessels will be fabricated from rolled 3/8-in.-thick 316 CRES plate stock. Welded subassemblies will be employed to alleviate handling and transportation problems. Circumferential rings will be welded in segments and fitted to the in-plant assembled vessels. After fit-up of the rings and inspection of the completed welds, the vessels will be dismantled and shipped to the installation site.

Weld-joint preparation for the vacuum vessels will include, as a minimum, a 60-deg beveled edge for 100% weld penetration. The first weld pass from each side of the subassemblies will be performed with the automatic gas metal-arc weld (GMAW) process, or the gas tungsten-arc weld. The remaining passes would be accomplished with a high-rate deposit process, such as flux core metal arc. To minimize warpage and distortion, welding will be done on both sides of the material when possible.

Quality assurance procedures will be used throughout all the welding processes. X-ray and ultrasonic inspection of welds would be a part of these procedures.

A shipping and transportation plan will be part of the manufacturing plan for the vacuum vessel. The shipping plan will develop routes and envelopes of the vacuum vessel's subassemblies. Producibility engineers, in concert with structural and system engineers, will ensure adherence to the envelope parameters during the detail design phase.

Installation

Magnets. The magnet vacuum vessels are assembled onto the completed magnet cold masses. The magnet, with its thermal shield in place, is positioned inside the vacuum vessel's channel-shaped annulus subassembly and the support struts are located and attached. The opposite side plate is then

welded in position with a close-out weld. Because of the inspection and test procedures involved, it is desirable to make this close-out weld in an in-plant atmosphere, which will require special handling by shipping and transportation personnel. The completed S-1 magnet is 25 ft in diameter and will weigh 160 tons.

End Cells. The end cell vacuum-vessel subassemblies will be received on site, inspected, and stored in a prepared marshalling area. After the central cell vacuum vessel and magnets have been positioned and secured and the end cell support structure is positioned, installation of the end cell vacuum vessel will begin. The subassemblies will be erected on shoring similar to that employed during the in-plant assembly. Manual tack welding will be used throughout erection of the subassembly. When all assemblies are in place, a complete first-pass weld will be accomplished manually, using gas-tungsten arc weld. The remaining passes will be accomplished with a flux-core, metal-arc weld process. All welding will be controlled procedurally and will be in accordance with ASME codes. Quality assurance will be maintained by 100% weld inspection; x-ray and dye penetrant procedures will also be employed.

Central Cell and Anchor Cell. As-shipped welded subassemblies will be received at the erection site. They will be inspected and stored in a marshalling yard until scheduled for use. Shoring, braces, and scab plates will hold the central cell assemblies in place during tack and first-pass welding. The welds for the central cell vacuum vessel will be completed and inspected, and any repair or rework will be accomplished. Also, vacuum bagging will be used for limited testing of all openings. When this testing is completed satisfactorily, the magnets can be installed.

When the magnets and central cell vacuum vessel have been satisfactorily located and mounted, anchor cell buildup will commence. Again, shoring, braces, and scab plates will be used to hold the subassemblies in place during tack and first-pass welding, and all welding will be controlled procedurally. The circumferential rings will be assembled onto the vessel skin and welded in place, and all welds will be inspected.

The scab plates are to be ground off, any unnecessary or interfering shoring is to be removed, and the alignment and center line of the installation is to be established or verified by optical means.

Structural Analysis. The magnet vacuum vessel and the external vacuum vessel are to be made of 316 CRES with an operating temperature of about 100 °F. These vessels are to be designed to withstand 1 atm pressure. In addition, the external vacuum vessel has to resist loads due to the shielding material. These loads depend on how the shielding material is attached to the vacuum vessel; however, for initial sizing, these procedures will be followed:

1. Design the shell thicknesses for external pressure, using the ASME Boiler Code as per NB 3133.3 and NB 3133.4 of Section III, Division I.
2. Add a safety factor of 2 when sizing shell thickness even though the Boiler Code design procedure allows a sufficient margin of safety.
3. Undertake the detailed design of ring stiffeners and other shield-load attachment points at a later stage.
4. When the Boiler Code does not cover the loading situation, use conventional analysis procedures with a sufficient factor of safety.

Central Cell Magnet (S-0 and S-1) Vacuum Vessel. These magnet vessels are basically toroidal shells having a rectangular cross section under an external pressure of 1 atm. The Boiler Code does not address the problem of rectangular section toroids under external pressure. Hence, an approximate method was used to size these vessels. The outer surface of the torus was treated as a cylindrical shell under external pressure and, hence, was designed according to the Boiler Code. Because of the nature of the simplifying assumption, a safety factor of at least 5 was deemed advisable. A step-by-step procedure for designing a cylindrical shell under external pressure is outlined in NB 3133.3 of ASME Boiler Code, Section III. With this procedure, a thickness of 2 in. was found sufficient.

The magnet vessel side plates were treated as bending annular plates under 1 atm pressure--a very conservative assumption. Two-inch-thick side plates were stressed to a maximum bending stress of 16,740 psi. For 316 CRES at 100 °F, the yield stress (as per the Boiler Code) is 30,000 psi. This

resulted in a safety factor of 1.79 using the allowable tension even though the Boiler Code permits an allowable tension of 1.5 times the tension allowable when checking for failure under maximum bending stress. Hence, a 2-in.-thick vacuum vessel was deemed sufficient to withstand 1 atm pressure.

External Vacuum Vessel. The configuration of the external vacuum vessel changes from a cylinder to a spherical segment, as shown in Fig. 3-1. This drawing shows transition sections where the shell is approximately conical or spherical. As discussed previously, the Boiler Code procedure was used, and thicknesses were determined using an additional safety factor of 2. Table 3-53 lists vacuum-vessel thicknesses at various locations.

Materials. Because of its availability and cost relative to other, more exotic materials, stainless steel was chosen as the baseline material for both the main and magnet vacuum vessels. Stainless steel is also nonmagnetic, a requirement of any material used in this application. See Table 3-54 for the mechanical properties of 304L stainless steel at elevated temperatures.

3.6.3. First Wall--Mechanical

Design. Design of the first wall required a balance between the two main requirements, heat transfer and neutronics. Other requirements are structural rigidity and physical compatibility with the rest of the machine. Physical compatibility is a relatively minor requirement because we are concerned only with the central cell first wall.

The main task was to provide a surface over which a coolant fluid could flow, but which would not be destroyed by the fusion environment in too short a time period. To accomplish this, we used two concentric cylinders that run the length of the central cell. The coolant water flows between the two cylinders and through a series of vanes positioned within the cylinders to maintain axial flow (see Fig. 3-142.)

An alternate configuration would be to use a series of parallel trace tubes welded to the outside of the inner cylinder, which would also run the length of the central cell.

Table 3-53. Vacuum-vessel thicknesses for the TUF external vacuum vessel.

Location	Thickness (in.)
Central cell	0.75
Anchor cell	1.125
End cell	2.75
End dome	3.25

Table 3-54. Mechanical properties of 304L stainless steel at elevated temperatures.

Temperature (°F)	Modulus of elasticity (ksi)	Poisson's ratio ν^*	Coefficient of thermal expansion (in./in./°F)	Yield stress (ksi)	Tensile ultimate (ksi)	10,000-hr creep-rupture (ksi)
200	28.0	0.3	8.4	60	120	90
300	27.5	0.3	9.2	60	115	87
400	27.0	0.3	9.6	56	110	83
500	26.2	0.3	9.9	54	108	75
600	25.5	0.3	10.3	50	106	71

*Estimated.

Figure 3-142. First-wall configuration.

A third configuration would provide separate first-wall sections for each test module and shield segment. Although this configuration would be basically the same as the full length design, there would be many short walls instead of one long one.

Wall thicknesses were initially dictated by thermodynamics requirements, but their present thickness is a result of stress requirements. A center support is also needed to limit bending under the weight of the water. The length of the first wall is dictated by task definition.

Replacement or servicing of the central cell first wall can only be done by disassembling the center cell shield sections should the full length design be chosen. While it would be easier to maintain the individual first-wall segments, manifolding and water piping would be more complex. From the standpoint of maintenance, a modular or sectional first wall would be best. It would provide a concise piping supply and return lines and facilitate the removal of sections.

Manufacturing

Fabrication. The details of the first wall will be made from stainless steel, using such processes or operations as layout and rough cut, roll form, bend on brake, and route or shear to final size.

Five welded subassemblies will be made from these details. The first-wall subassembly will be made by resistance welding 0.080-in.-thick corrugated formed sheet to the rolled and welded inner bore. The outer channel skin will be rolled, formed in four sections, and resistance welded to the corrugated channel. The sections will be longitudinally welded together. These welds will employ backup bars. The second and third assemblies will be two annulus rings made up of an outer radius ring and a side plate. The annulus assembly will have an "L"-shaped cross section, one left-handed and one right-handed. The fourth assembly will be two annulus rings welded into end-plate assemblies. The fifth assembly will be two stacks made from a tube and a built-up rectangular transition section.

Installation. The first assemblies to be installed will be the stacks, which will be inserted into openings and clamped in place. The first-wall

assembly will be fed into place from either end and located on station. The third and fourth assemblies will be fed in over the first-wall in-bore and located on station. The side-plate annulus ring will meet the first wall's outer skin. Then the stack will be welded to the outer radius ring of the annulus assembly, and the side plate of the annulus assembly will be welded to the outer skin of the first-wall assembly. All welds will be in accordance with the ASME vessel code. The end plate annulus rings will then be welded into place as close-out of the first wall is accomplished.

Thermal Hydraulics. The 6-m-long, 28-cm-radius central cell first wall must accommodate a heat loading of 10 W/cm^2 . Assuming an inlet water temperature of 40°C and a temperature rise of 20°C , a water flow rate of 180 gpm is required to remove the heat. The 1.7-fps water velocity will cause a pressure drop of 0.3 psi through the channels, requiring 20 W of pumping power. A pressure of 10^6 psi will be adequate to suppress boiling. The maximum steel temperature reached at the front surface of the first wall is 144°C , with a 54°C conduction drop in the front plate.

Structural Analysis. The first wall, as shown in General Dynamics drawing TDF 016, consists of a cylindrical 5.5-m-long shell with a support at mid-length and a 28-cm inner radius. The shell wall is of corrugated sandwich type construction, with the corrugation in the axial direction of the shell. The average operating temperature is about 250°F , and the material chosen for first-wall construction is 304L stainless steel. The coolant flow produces an internal pressure of 100 psi inside the wall.

The Boiler Code does not have a design procedure for a sandwich type cylindrical shell with a pressure load inside the shell wall. Hence, the following failure modes were assumed, with Boiler Code design procedures being followed when applicable.

1. Failure of the outer skin as a cylindrical shell under internal pressure.
2. Failure of a rectangular pressure vessel equivalent to the trapezoidal flow ARCA under internal pressure.

3. Failure of a monocoque cylinder equivalent to the sandwich cylinder under external pressure.
4. Failure of the inner skin as a cylindrical shell under external pressure.

Analyses based on the Boiler Code for the four possible failure modes shown above indicate that modes 1 through 3 can be avoided if the outer and inner skins are each 0.125 in. thick and have a corrugated metal thickness of 0.051 in., while mode 4 can be avoided if the thickness of the inner skin is increased to 0.30 in. Even though mode 4 is the worst-case failure mode, this assumption would be conservative should the corrugation separate from the outer skin. Hence, we can conclude that the outer skin can be 0.125 in. thick, while the inner skin should be 0.30 in thick.

First-Wall Material Properties. Because of its availability and cost, steel was chosen as the baseline material for the first wall. Stainless steel meets the neutronics and thermodynamics requirements, so the use of exotic expensive material is not warranted. Table 3-54 shows the property values of the 304L stainless steel used in performing material-related stress and thermodynamic analyses.

3.6.4. Structural Support

The structural support for the TDF mirror machine is similar to a bridge structure. It consists of a series of I-beams and support struts that carry the weight of the entire machine.

The overall structure of the machine can be broken into three distinct sections: (1) the central cell, (2) the end cell, and (3) the beamlines.

1. The support for the central cell magnet consists of two trusses that cover approximately 100 deg of the magnet vessel bottom. Included in the magnet structure is the magnet cold-mass support and the intercoil magnetic support beam. The cold-mass support consists of four pairs of struts that support the magnet inside the vacuum vessel. These struts are designed to operate in tension, so the cross section can be low and long, providing a low heat loss.

2. The end cell support is made up of one wide center-support truss that runs the length of the end cell and four tall, stabilizing towers that provide lateral stability. The main weight component taken up by the center section.
3. The beamline supports posed a more complex problem than either the central cell or end cell. The beamlines are situated at odd angles (at angles in two planes). To leave more floor space under the machine, it was deemed desirable to support the beamlines from above or at the end cell dome.

A support arch, which goes from the side walls up to the beamlines and connects under the anchor cell, was designed as the primary load-carrying support. In addition, some tension members that reach back to the anchor cell region will provide extra support by transferring a portion of the load to other parts of the machine.

The support for the anchor cell magnet set is in two parts: magnetic load and weight load. Most of the magnetic load is taken up by eight struts in tension. The weight load and partial magnetic load is taken up by a beam-truss structure between the anchor magnet vacuum vessel and the inner wall of the shield.

A bucking ring, which welds to the inner wall of the shield, is added to react the magnetic forces acting on the choke coil.

Manufacturing

Fabrication

Magnets. The magnet supports will be fabricated from structural shapes and detail plates that are plasma arc cut from stainless steel. Saddles and footprint pieces will be cut, sized, and saddle-rolled to shape. Assembly will be accomplished using standard shop aids (clamps, braces, and shoring) to position the details for welding. These supports could be assembled in plant or on site as costs and schedules demand. To hold costs down and permit detail batching, the six support assemblies will have a common set of details.

End Cells. The support assemblies will be fabricated in much the manner as the central cell. Because of its size, however, the center-line support assembly will have to be final assembled on site. As a cost-saving measure, the end cell supports may be made from structural steel in lieu of stainless steel if analysis of the magnetic field permits it.

All welding will be to structural standards, and appropriate quality assurance procedures will be applied.

Installation

Magnets. The completed support assembly will be positioned and affixed to mounting plates anchored in the facility floor. With the use of jacks and alignment optics, the supports will be shimmed in position and welded in place.

End Cells. These supports will be installed like those for the magnets.

Structural Analysis for TDF End-Cell and Central-Cell Support: Weight

Loading. The end cell was conservatively assumed to be supported by its 15 vertical members. The central cell was similarly assumed to be supported by its two vertical members. Remaining members were assumed to provide horizontal stability only and were not included in load-carrying capability.

The material for the structural members was designated as 316 stainless steel having an assumed yield of 30 ksi. Column dimensions were selected from the AISC Manual of Steel Construction, using the 36-ksi yield-stress tables. An additional factor of safety was used to account for the higher yield stress of the tables.

Based on the stress analysis, the central columns for the end cell support should be W12 by 12-133; all other members should be W12 by 12-79. For the central cell supports, the columns should be W10 by 10-72, and the other members should be W6 by 6-15.5. Sizing should be used as a preliminary guide only, not for manufacturing.

Preliminary Stress Analysis of the Anchor-Ceiling Vacuum-Vessel Support Structure. The vacuum vessel was assumed to be supported by its vertical members only. Nonvertical members were assumed to take lateral loads, provide stability, and leave the vertical columns in compressive loading only.

The material for the structural members was designated as 304L stainless steel. The environment was assumed to be 200 °F, giving a yield stress of 60 ksi.

The inboard region is supported by five vertical members. All members are designated as 4- x 4- x 3/8-in. square structural tubing. The four vertical support members of the outboard region are designated as 5- x 5- 1/2-in. square structural tubing. Again, these are preliminary sizings only and are not intended for manufacturing.

3.7. SHIELDING

3.7.1. Shield--Mechanical

The TDF central cell bulk shield is basically a hollow cylinder with an inner radius of 85 cm and an outer radius of 175 cm. The first 15 cm of the shield is a water volume. The water is required for both neutronics and heat transfer. The last 10 cm of the shield is boron carbide, a composite material. The remainder of the shield is steel.

The major factor to be considered in shield design is the sheer mass of the steel. Two methods were theorized to design the shield sections: (1) Shield sections could be built up of large steel plates welded together to form a framework "box" (egg crates); it was felt that this would assimilate a solid piece of steel fairly well. (2) Shield sections could be spun-cast into solid cylinders; however, the size of the application may not allow this option.

Boron carbide is so hard it requires a diamond to cut and is, therefore, difficult to work with. Fortunately, the vendor can supply it to us in the section shapes and sizes we dictate so that no cutting is required.²⁸

Maintenance and replaceability of the shield should be a relatively small task. Maintenance is limited to the water channels at the inside radii. Shield sections such as the superconducting magnets should never have to be replaced, so no ease-of-removal features have been worked into the design. Should removal be necessary, segments could be cut out in the reverse order of assembly.

Manufacturing

Fabrication. Shield fabrication will involve such processes as rolling, shearing, drilling, and plasma arc cutting. Details will be cut from 3/4-, 1-, 2-, and 3-in.-thick stainless steel material. To-size boron carbide plates will be procured from a qualified subcontractor, and the to-size tungsten insert will be obtained from a tungsten producer.

All details will be inspected and their conformance to the engineering design identified before they are delivered to the site by common carrier.

Installation. All shield details will be received at the site and stored. The central cell vacuum vessel will have been built up to the horizontal center line, and the outer shield ring will have been roll-formed in pieces approximately 4-1/2 by 8 ft. The plates will be laid in place in the bottom of the vacuum vessel and structurally welded together up to the horizontal center line. Then the 3/4-in.-thick ring halves and ribbing will be positioned to form rectangular egg crating, and the details will be welded in place with high-deposit fillet welds. These egg crates will then be filled with boron carbide sheets. Segments cut from 5000-1b billets, with mating steps cut radially into each side, will be positioned in this bottom half of the shield structure. Tack welding will be performed between each segment to hold the segments in place. Concurrently, in another fabrication area, the water-jacket assembly is to have been built up from the inside wall out, using pressure vessel quality welds. This assembly is to be inspected and integrity tested before it is lowered into place on the bottom half of the 2-ft-thick shield wall.

When the water jacket and its associated plumbing are in place, the top half of the 2-ft-thick ring-wall assembly will continue. Where wall openings are encountered, temporary shoring or frames will be used to permit the placement of blocks. The top block will be keystone-shaped to fit in the stepped sides of the segments.

The top half of the outer shield ring will have been subassembled in manageable sizes at a convenient site. The egg crating of these sized and roll-formed welded subassemblies will have been welded in place, and the boron carbide plates will be located in the crating. The final operation will be to place these assemblies on top of the existing shield structure and weld them in place.

When the shield structure is complete, the rest of the vacuum vessel will be installed.

Thermodynamics. The TDF central cell shield must be cooled to remove volumetric nuclear heating. Water was chosen as the cooling medium because of its good neutronics performance with steel. The 15 vol% of water in the shield was also dictated by neutronics performance, not cooling requirements. Therefore, it is necessary to determine if the shield will meet cooling requirements. An analysis of the shield configuration is shown in Fig. 3-143.

Figure 3-143. TDF magnet models for neutronics analysis.

The water is assumed to be uniformly distributed and to flow circumferentially around the shield. The inner and outer surfaces of the shield are assumed to be insulated, i.e., no heat is transferred out of the shield except through the water. The nuclear heating profile used in the thermal analysis of the shield is shown in Fig. 3-144.

A total of 4.7 MW of heat must be removed from the 128-cm-long shield section. One-thousand gallons per minute of water at 150 psi will cool the shield and suppress boiling. The inlet temperature and temperature rise of the water are assumed to be 40 and 20 °C, respectively. The water velocity of 1.3 fps results in a small pressure drop through the shield. The predicted temperature reaches a maximum of 324 °C on the inner shell and 320 °C on the outer shell.

3.7.2. First Wall

Summary. The primary function of the first wall in the central cell is to protect material test samples from the non-neutron environment associated with a mirror fusion plasma. To accomplish this, the first wall design cannot significantly attenuate or moderate the 14-MeV neutron flux of this facility. We used the neutron and gamma transport code ONEDANT to analyze several design options from a neutronics perspective. On the basis of these analyses, we scoped a first-wall mechanical design. The design consists of a stainless steel, channeled structure that is cooled with water and modular so that segments can be easily removed from the central cell for maintenance or replacement access. Neutron flux effects, as measured by the reduction of material displacement per atom (dpa) potential, indicate a reduction of only about 25% with this design. We recommend redesigning the structure with aluminum.

Requirements. Most material test samples are to be exposed to only the 14-MeV neutron flux associated with a D-T burning plasma. Consequently, the first wall must provide a barrier against the thermal radiation and neutral atom flux associated with this type of plasma. The wall must absorb all of the neutral flux and as much of the electromagnetic radiation as possible. Heat loads will necessarily be high, resulting in a need for adequate cooling

Figure 3-144. Nuclear heating in the TDF central cell bulk shield.

and subsequently in requirements that the wall be leaktight and interfaced with a heat-exchanger system.

Neutral flux wall loadings above 10 W/cm^2 mean that first-wall material will be sputtered. A design adequate to ensure that the resulting material loss will not compromise the integrity of the first wall over the facility lifetime would degrade the neutron flux at the material test samples. The alternative chosen was to require that the first wall be easily replaceable and removable through the blanket test ports.

Discussion. The single most important application of TDF is for nuclear testing. Large test volumes and neutron wall loadings of 2 MW/m^2 at a radius of 25 cm make it an ideal materials test platform. The reality of this engineered device, however, is that, in addition to the required 14-MeV neutron flux, other less desirable plasma components are also produced. In the absence of a first wall, test samples would be exposed to both a radiant heat load and a flux of neutral atoms. Both fluxes would significantly heat the test samples, and the latter can give rise to sputtering and surface blistering. Although this is an appropriate environment for some sample testing (diverter and limiter surfaces of a tokamak and first walls of any fusion driver), most materials testing in this zone will be performed in the shielded, neutron-only conditions outboard of the first wall. The majority of unshielded materials testing has been reserved for the specialized blanket test facilities.

Clearly, the most important function of the first wall is to provide the shielding needed to ensure that test samples are exposed only to neutrons. Specifically, it must provide this protection with a minimal impact on the 14-MeV neutron flux, both in terms of attenuation and energy moderation.

A second significant consideration for the first-wall design derives from the sputtering issue. Two distinct neutral fluxes are incident on the first wall. The first is a lower energy component that averages about 100 eV and originates in the halo plasma. A higher energy flux, averaging about 30 keV, is traceable to charge-exchange events with neutral-beam-injection deuterons and tritons. This combined flux is responsible for a sputtering rate of about 0.13 mm/FPY on the first wall. The sputtered material is, in theory, trapped by a halo plasma and pumped out through the end cell. Should the halo not intercept the wall material, it will enter the lower portion of the plasma, where it will ionize.

As a relatively high-Z material, it will radiate away a great deal of plasma heat, resulting in plasma control and yield problems and in additional radiation shielding concerns. If the halo intercept theory does not prove out, the first wall can provide a suitable substrate upon which a sacrificial, low-Z armor could be placed. Sputtered armor that penetrated the halo plasma would have a significantly lower radiation cooling effect than would the steel, assuming similar material sputtering rates.

Constraining the first-wall design to have a minimal impact on the 14-MeV neutron flux necessitated a neutronics analysis of various mechanical designs that met all other requirements. ONEDANT, a discrete ordinates neutron and gamma transport code, was used for this purpose. A 30-energy-group neutron and 12-group gamma library complete with kerma and dpa response function data was used for analysis. Modeling of the system geometry is depicted in Fig. 3-145; it consists of a one-dimensional radial slice through the TDF central cell. Full-energy neutrons are uniformly produced in the 10-cm-radius plasma, stream through the 15-cm-radius void space, and are incident on a pair of zones of variable thickness that represent the first-wall structure and coolant, respectively. A third variable-thickness region is a void that is followed by a thin sample zone of aluminum. This last zone is used as the first-wall neutronics effects reference. The variable used to measure the 14-MeV neutron flux degradation associated with a given first-wall design is the dpa rate in the aluminum reference zone. Atomic displacement rates are indicative of both neutron flux strength and average neutron energy and, as such, provide an excellent diagnostic for determining relative flux-degradation effects.

Preliminary mechanical design scoping and the ease-of-replacement requirement suggested that only standard structural materials need be considered. Both aluminum and stainless steel were investigated, with either water or nitrogen being considered as coolants. Clearly, flux degradation is proportional to the thickness of the structure and coolant regions, and a gas has a smaller effect than a liquid does (see Table 3-55). Of interest, however, was a quantification of this effect with thickness and material. Results obtained for several thickness combinations are tabulated below. Degradation results are reported as percent reductions in dpa with respect to the no-first-wall reference case.

Figure 3-145. First wall neutronics modeling geometry.

Table 3-55. Flux-degradation results.

<u>Structure</u>	<u>Coolant</u>	<u>Wall thickness (cm)</u>		
		<u>0.4</u>	<u>0.635</u>	<u>1.0</u>
Aluminum	Water	15	19	23
	Gas	9	13	17
Stainless steel	Water	25	31	38
	Gas	17	23	31

These results indicate that aluminum is substantially more neutron transparent than an equal thickness of steel and, consequently, a more desirable candidate. No mechanical reasons exist to mandate the use of steel over aluminum, particularly in light of the ease of replacement and repair required of the design. Aluminum has the further advantage of having all short-lived activation products; as a result, it poses less of a waste management problem. On the basis of its superior neutron transparency properties and approximately equal mechanical factors, aluminum was preferred as the first-wall structural material. However, time did not permit completion of the aluminum wall design. An interim design using stainless steel is included in the baseline.

As expected, the gas-coolant option provides for better neutron transparency than does a equal thickness of water cooling. A decision as to what coolant to use for the first wall requires comparison of more than just neutronics. Heat fluxes of 10 W/cm^2 from direct plasma heating and volumetric heat loads of about 25 W/cm^3 from neutron energy deposition place a substantial requirement on first-wall cooling. Pressurized gas cooling would be possible, but the price of high velocities, large pumping powers, and substantial film drops. These factors combined put additional structural requirements on the design and, as such, mean lower neutronic transparency than the comparison at equal zone thickness would suggest. In addition, shorter first wall lifetimes resulting from temperature-enhanced radiation damage problems would increase first wall replacement and failure rates. It is clear, for the TDF application, that the small neutronics penalty associated with water cooling is far less severe than the consequences associated with the gas coolant concepts.

In summary, neutronics considerations (in light of the mechanical issues driving the choice of coolant) suggest that aluminum and water be used for the first wall materials. Every effort should be made to make both materials as thin as possible within the mechanical constraints associated with the environment and the overall design.

3.7.3. Bulk Shield

Summary. The main function of the TDF central cell bulk shield is to protect the superconducting solenoid magnets (S-0 and S-1) from bulk penetration of the neutron and gamma fluxes of the central cell. A detailed one-dimensional neutronics and photonics model is adequate for analyzing the radiation attenuation of the many shield designs investigated. Although three damage/heating requirements were identified for bulk shield attenuation performance, peak magnet heat load became the driving requirement. If this most stringent requirement could be met with design, the other two damage criteria were satisfied with substantial margin. A two-region bulk shield has been chosen for the TDF. Region 1 consists of an 80-cm-thick steel (Fe-1422) annulus cooled with 15 vol% water. This is wrapped by region 2, a 10-cm-thick B_4C (boron carbide) thermal neutron shield. Neutronics analysis showed that the neutron absorber in region 2 was more effective in reducing the magnet heat load than an equal thickness of lead. This suggested that neutron heating (through capture and the emission of charged particles or prompt gamma) dominated gamma streaming from the bulk shield into the magnet, at least with respect to peak heating--the design requirement. All three design requirements were satisfied with margins exceeding a factor of two even when the beam duct streaming contributions to S-1 were included. In addition, this design allows for approximately 60 cm (radially) of material test area behind the first wall.

Requirements. Only three major requirements were established for the neutronics/photonics analysis of the TDF bulk shield. Because the shield function is to protect the superconducting magnets (considered lifetime components), all three requirements place limits on the allowable radiation damage to magnet components. The most stringent requirement is the peak magnet heat load. Maximum neutron plus gamma heating of any liquid-helium-cooled portion of the magnet should not exceed $60 \mu W/cm^3$ from all sources. This requirement is established to ensure that the nuclear heat load on the magnet does not cause excessively high cryogenic plant costs and power. It is significantly below the maximum heat load allowed for magnet cryostability.

The second requirement is that the peak radiation dose to the polyimide electrical insulation of the magnet not exceed 5×10^9 rads over the 5.44-FPY lifetime of the facility. Doses must be kept below this limit to ensure that radiation-induced property changes in the insulator do not lead to failure of the material's electrical isolation function.

The third requirement is that the peak dpa in the copper stabilizer of the superconducting winding pack not exceed 1.1×10^{-4} before the first magnet anneal at room temperature. This first anneal should occur after two FPY because only about 85% of the damage can be recovered. A shorter initial anneal period results in increased anneal frequencies later in the magnet life and in a corresponding decrease in continuous machine run time. Since most long-term blanket tests will be performed with a mature machine (late in machine life), frequent anneal requirements will have a negative impact on the test program.

In addition to the bulk neutron and gamma streaming damage, a beam duct streaming component must also be considered. As will be discussed later, the neutral beam injector ducts provide penetrations into the bulk shield that effectively move neutron and gamma sources far closer to the S-1 magnets than do those that must pass through the bulk shield. Adequate tungsten shielding has been provided to protect against this beam duct penetration component, but the bulk shield damage requirements must be lower to account for beam duct streaming contributions. Thus, two sets of requirements have been established for the bulk shielding neutronics analysis. One set each for the S-0 and S-1 magnets is listed in Table 3-56.

Discussion. The superconducting central cell solenoids are lifetime components. Lead times for the construction of these magnets and the cost of replacing them once the machine has been assembled would preclude continued operation of the device. Consequently, every effort must be made to ensure their protection. The most obvious source of damage to the magnets is the intense neutron flux of the central cell plasma. A neutron wall loading of 2 MW/m^2 at the first wall radius of 25 cm must be lowered about five orders of magnitude to reduce the magnet damage to acceptable levels. The shielding needed for this reduction must fit between the material test samples at a radius of approximately 50 cm and the inside bore of the magnet vacuum vessel at a radius of 175 cm.

Table 3-56. Radiation damage and heating limits (bulk only).

	S-0	S-1
Peak magnet heat load ($\mu\text{W}/\text{cm}^3$)	60	35
Peak dose in polyimide insulator ($\text{rads}/\text{FPY} \times 10^8$)	9	8.8
Peak dpa in copper stabilizer ($\times 10^{-4}$)	1.1	
(Before first anneal--2 FPY) ($\text{dpa}/\text{FPY} \times 10^{-5}$)	5.5	4.7

Neutronics and photonics analyses were performed with the one-dimensional, discrete ordinates code ONEDANT. A coupled 30-group neutron and 12-group gamma library complete with kerma and dpa response functions was used for the transport calculations. Because both total nuclear heating data and peak heating data were required for each magnet, modeling was performed carefully so as to be as near to the actual mechanical design as possible. Cross sections of both the S-0 and S-1 magnets are shown in Fig. 3-143; the associated dimensions are radial locations with respect to the plasma center line. Only the multi-layer insulation material of the thermal shield zone was not modeled. The MLI region consists of low-density (0.01 g/cm^3), low-z material with minimal protection capability or damage concerns and, as such, was treated as a void.

Figure 3-146 shows the remainder of the neutronics model from the plasma center line ($r = 0.0$) to the outside of the bulk shield (i.e., $r = 175 \text{ cm}$, the inside bore radius of the S-0 and S-1 magnets). Full-energy (14-MeV) fusion neutrons are uniformly produced in the 10-cm-diam plasma region (normalized to a 2-MW/m^2 wall loading at 25 cm), stream through a 15-cm-diam void, and are incident on the first wall. The first wall was modeled as a 1-cm-thick water coolant sandwiched by two 0.25-cm-thick aluminum plates--a close approximation to the channeled first wall structure. The effect of changing to a stainless steel structure will not change the shield design. A material test sample region was assumed to exist to a radius of 50 cm, but was taken as a void to investigate the worst-case shielding requirements. The remaining region ($r = 50$ to 175 cm) was the bulk shield design flexibility region, where the various design concepts were investigated for neutronics compatibility with the magnet damage limits.

Several materials were considered in the neutronics analyses. Preliminary calculations of the nuclear heat-deposition rates mandated use of a water-cooled shield for heat removal. The water also provides an excellent neutron moderator. It quickly reduces the energy of the fusion neutrons, thereby decreasing the magnet damage potential and increasing the capture probability in the shield zone. Structural materials investigated included steel (Fe-1422 and 316 stainless steel), tungsten, lead, and boron carbide. Several shield configurations were also tested. Homogeneous and heterogeneous designs, as well as graded shield concepts, were scoped where different water

Figure 3-146. Bulk shield neutronics modeling geometry.

fractions were used in various areas of the shield. Shield thicknesses were also varied to determine shield attenuation factors that could be used for extrapolations to higher or lower wall loadings.

Neutronics results showed that 15 vol% of water in the shield was optimum for 316 stainless steel, Fe-1422, and tungsten. It provided enough moderation without displacing too much of the absorbing structural material. Although tungsten provided the thinnest shield, being about a factor of 2 thinner than the two steel options, tungsten costs and fabricability quickly eliminated it as a possible structural material when it became clear that there was adequate shield space for the less effective, but significantly cheaper, steels. The 316/304 stainless steel option was then eliminated because of the higher activation levels associated with its nickel content. The steel composition chosen for fine-tuning neutronics analysis was Fe-1422, which consists of 82% iron, 14% manganese, 2% nickel, and 2% chromium.

The final step in the neutronics analyses was to determine the effectiveness of either a gamma shield or a thermal neutron shield around the outside of the Fe-1422/water major shield. Neutrons captured in the bulk shield give rise to deeply penetrating gamma rays that can pass through the remainder of the shield and contribute to nuclear heating. Lead is an excellent gamma shield; therefore, 10 cm of lead was placed just inside the magnet bore. A neutronics analysis with the lead shield showed a reduction in magnet heat deposition; when a similar 10 cm of neutron absorber (boron carbide) was investigated, however, a greater reduction in magnet heating was observed. Thermal neutrons escaping the bulk shield are apparently captured in the magnet materials and, through charged-particle formation or capture gamma production, result in more nuclear heating than the streaming gamma radiation from the bulk shield.

On the basis of the neutronics analyses and some cost considerations, a water-cooled and moderated Fe-1422 steel shield (85/15 vol% composition of steel and water) followed by a 10-cm-thick boron carbide thermal neutron shield provides adequate shielding within the space available. The appropriate thickness of the steel/water zone was determined by a trade study. Neutronic analyses showed that a 70-cm-thick steel/water shield (followed by boron carbide) marginally satisfied the design criteria. The same configuration with an 80-cm-thick steel/water zone satisfied all design requirements by at least a factor of 4. In this trade study, the fabricated

cost of the extra 10 cm of steel/water shield was compared with the lifetime operating cost of the cryosystem required to remove the additional nuclear heating associated with the 70-cm-thick steel/water shield. Fabricated shield costs were about \$5.2 million and \$4.7 million, respectively, for the 80- and 70-cm-thick cases (90 and 80 cm total thickness). The \$0.5 million difference compares with the \$1.5 million additional cryosystem operating cost. Clearly, thicker shields are economically superior even in this conservative trade study. In addition, thicker shields provide more margin, which means higher possible wall loadings and fewer, less frequent magnet annealings. It is even possible, although marginal, that the polyimide insulator of the magnets could be replaced by the cheaper, but less radiation-resistant, G10 insulator.

The recommended bulk shield consists of an 80-cm-thick ($r = 85$ to 165 cm) steel/water zone of 85 vol% Fe-1422 and 15 vol% water followed by a thermal neutron shield zone of 10-cm-thick ($r = 165$ to 175) boron carbide. Table 3-57 lists the neutronics performance of this shield configuration for bulk contributions only (no neutron beam duct damage or heating included). All three damage limits have been met with substantial margin. The energy multiplication factor is the ratio of total energy absorbed by all components of the neutronics model (i.e., first wall, shield, and magnet) divided by the fusion neutron energy input. This quantity reflects the magnitude of the prompt gamma production within the shield. The magnet energy deposition fraction is the fraction of total system energy that leaks from the shield and is absorbed in the magnets in the form of nuclear heating.

Nuclear heat deposition in the magnets and bulk shield must be removed. Proper use of the neutronics analysis results provided information on the heat deposition in both the S-0 and S-1 magnets. The data are summarized in Table 3-58; again, they do not include beam duct contributions for the S-1 magnets, which will only add about 20% to the overall cooling requirements. Similarly, nuclear heat deposition in the bulk shield was computed and is plotted in Fig. 3-144.

3.7.4. Streaming and Shielding Analysis for the Neutral Beam Duct

Summary. A detailed three-dimensional neutronics and photonics calculation has been performed to analyze radiation streaming and shielding in the neutral beam injector (NBI) system of TDF. A tungsten shield in the 35-cm-wide space

Table 3-57. TDF bulk shield performance (neutronics).

Energy multiplication factor	1.52
Magnet energy deposition fraction ($\times 10^5$)	1.65
Superconducting peak heating ($\mu\text{W}/\text{cm}^3$) (design limit = 60)	10
Insulator peak-radiation damage ($\times 10^{-7}$ rads/FPY) (design limit = 8.8×10^8 for polyimide and 9.0×10^7 for G10)	2.4
Copper stabilizer peak damage ($\times 10^6$ dpa/FPY) (design limit = 5.5×10^{-5})	7.6
Magnet nuclear heating (W)	319
(sum of 3 magnets) for liquid helium	208.5
and LN_2	26.8

Table 3-58. Total magnet nuclear input by zone (bulk only).

Zone	Power deposition (W)	
	S-0 magnet	Each S-1 magnet
Vacuum vessel	24.3	29.6
Radiation shield	7.81	9.53
Helium vessel	34.6	49.3
Insulator	0.82	1.21
Grade I conductor	17.6	26.0
Grade II conductor	0.62	0.88
Grade III conductor	0.012	0.032
Total	85.8	116.5

available between the corner of the central cell solenoid and duct wall provides adequate shielding for the magnet. The peak radiation damage and nuclear heating in the magnet are well below specified design limits. Acceptable heat loads were obtained in the aluminum cryopanel. An extra 10-cm-thick tungsten shield in front of the steel source structure reduces the absorbed radiation dose in the Macor insulator considerably, resulting in an estimated lifetime of 2.77 FPY.

Requirements. Adequate shielding is needed to shield the central cell solenoids against radiation streaming into the neutral beam injection duct. Because of the limited space between the magnets and duct wall, the option of using the more effective tungsten for shielding must be considered. Another requirement of the shield design for the duct is to eliminate excessive nuclear heating in the cryopanel. The radiation effect on the Macor insulating electrode support structure in the ion source is of greatest concern. Because of the low radiation dose limit for Macor, adequate shielding is essential to increase the life of the ion source.

Discussion. Fusion reactors must accommodate a variety of penetrations. The purpose and size of these penetrations vary depending on the reactor type.²⁸⁻³⁰ Proper shielding is needed to protect the vital components in the penetration from excessive radiation damage caused by radiation streaming. In TDF, different penetrations are used for pumping, heating, fueling, and ion sloshing beams. Because neutron source density in the anchor and end cells is about six orders of magnitude lower than that in the central cell, only streaming in central cell penetrations is analyzed. The central cell penetrations in TDF have different sizes and injection angles and must fit between the central cell solenoids S-0 and S-1. Radiation streaming into these penetrations can lead to adverse radiation effects in the superconducting magnets. It is, therefore, essential to provide enough shielding between the duct wall and the magnets to reduce the radiation effects in them to acceptable limits.

A major penetration in the central cell of TDF is the neutral beam injection duct, which is characterized by a large size. The duct opening at the first wall is 0.3 by 0.6 m. Four such penetrations are at each end of the central cell. Other central cell penetrations include pellet-fueling and

test-specimen-tube penetrations, which are smaller (about 7.5 cm diam) and have less severe streaming problems than the ducts. Using a simple neutronics analysis based on one-dimensional calculations, we estimated that a minimum shielding thickness of 15 cm is needed between these penetrations and the magnets.

Evaluation of the radiation flow into the ducts requires an accurate knowledge of the neutron source and the geometrical configuration of the shield, as well as the size of the ducts. The MCNP³¹ continuous, energy-coupled, neutron gamma Monte Carlo code was used to model the reactor geometry. The neutron source was sampled from the neutron linear source density distribution along the machine axis. The source density was normalized to a central cell wall loading of 2 MW/m^2 at a radius of 25 cm. A trapping surface was located at the entrance surface to the duct. At this surface, all particles entering the duct were counted according to energy and angle bins. Then the results were stored to serve as source distributions in later modeling of the duct itself. Cross-section data based on ENDF/B-V evaluation were used in the calculation.

The energy spectrum of neutrons and gamma photons streaming into the duct is shown in Fig. 3-147. The pronounced peak at 14.1 MeV in the neutron energy spectrum is due to the uncollided source neutrons (44% of the streaming neutrons) streaming directly into the duct. A total of 3.08×10^{17} neutrons having an average energy of 6.91 MeV and 5.63×10^{16} gamma photons averaging 1.44 MeV in energy stream into the duct each second. The angular distributions of neutrons and gamma photons are shown in Fig. 3-148. The angle θ is measured from the normal to the duct entrance surface. It is clear that the angular distribution peaks at normal incidence. The 65-deg injection angle between the duct and plasma center lines implies that $\cos \theta = 0.9$. Therefore, a large fraction of streaming radiation will go directly toward the ion source at the end of the duct. In general, shielding requirements for the ion source are more severe for larger injection angles.

Figure 3-149 shows the geometrical model used to model the NBI system in TDF. The model is an idealization and adaptation of the actual NBI system design. The duct opens from 0.3 by 0.6 m at the first wall to 0.7 by 1.18 m at a distance of 3.2 m from the plasma center line. A 1-cm-thick stainless steel duct lining was modeled in the calculation. Only the corner of central

Figure 3-147. Energy spectrum of neutrons and gamma photons streaming into the neutral beam duct.

Figure 3-148. Angular distributions of neutrons and gamma photons streaming into the neutral beam duct.

Figure 3-149. Geometrical model for the neutral beam duct in TDF.

cell solenoid S-1, which has the largest radiation effects from streaming particles, was modeled. The magnet winding pack consists of 2 vol% niobium-titanium, 23.88 vol% copper, 54.12 vol% 304 stainless steel, 10 vol% liquid helium, and 10 vol% polyimide insulation. The magnet case, cryostat, and vacuum vessel surrounding the winding pack have a total effective stainless steel thickness of 0.125 m, which was modeled in the calculation. A space of only 0.35 m is available for shielding between the vacuum vessel at the magnet corner and the duct wall. The low-activation steel Fe-1422 (14 wt% manganese, 2 wt% chromium, 2 wt% nickel, and 82 wt% iron) was used in the shield. The shield consists of 56 vol% Fe-1422, 26 vol% B_4C (0.87 a.f.), 14 vol% lead, and 4 vol% water. The option of replacing Fe-1422 by tungsten in the shield zone at the magnet corner was considered to provide adequate magnet shielding.

The deflection magnet and aluminum cryopanel were included in the model. The NBI system uses a 0.05-m-thick superconductive shield that was also modeled in the calculation. Of greatest concern in the ion source was the effect of radiation on the insulating electrode support structure. Because of its fabricability, Macor machinable glass-ceramic is an attractive candidate insulation material for NBI systems. Its structure consists of a set of mica crystals embedded in a glass matrix. Because both glass and mica may undergo radiolysis, an enhanced damage rate in Macor is possible. The Macor used in the ion source is concealed from the direct line of sight of the source neutrons by an effective 0.1-m-thick stainless steel structure. Preliminary results showed that the ion source is not adequate to protect the Macor from excessive radiation effects. An extra 0.1-m-thick tungsten shield, with holes to allow for passage of the beams, is used in front of the stainless steel structure.

The NBI system model used in the three-dimensional Monte Carlo calculation employs neutron and gamma surface sources at the duct opening with energy spectra and angular distributions obtained from the previous streaming calculation. An angular source-biasing technique was used to get statistically adequate estimates for the quantities of interest. Completion of the MCNP run of 50,000 histories resulted in statistical uncertainties of less than 20%.

The shielding requirements for superconducting central cell solenoid S-1 are determined by a number of radiation limits. The dpa in the copper stabilizer should not exceed 1.1×10^{-4} before the first magnet anneal. This corresponds to a 20% increase in resistivity. Annealing at room temperature results in only 80% recovery of radiation-induced defects.³² A minimum of two FPY is needed before the first magnet anneal, which results in a period of 1 FPY between the final two anneals for an estimated reactor lifetime of 5.4 FPY.³³ Therefore, the dpa rate in copper should not exceed 5.5×10^{-5} dpa/FPY. Mechanical strength tests of irradiated magnet insulators show that polyimides are 5 to 10 times more radiation-resistant than comparably prepared epoxies.³⁴ Polyimide is used as an electrical insulator in the TDF magnet coils. The design criterion used for polyimide is that the radiation dose not exceed 5×10^9 rad at the end of the machine's life. Irradiation of the superconducting material (niobium-titanium) results in degradation of the critical current density. In this work, we require that the peak neutron fluence ($E > 0.1$ MeV) not exceed 3×10^{22} n/m², which corresponds to a 10% decrease in critical current density.³⁵ The limit on the peak heat load in the magnet winding pack is considered to be 0.06 mW/cm³. Because only radiation effects in the magnet, as a result of streaming into the neutral beam injection duct, are considered in this calculation, the radiation limits required are a factor of 2 lower to allow for the extra contribution coming through the magnet bulk shield.

Table 3-59 summarizes results for the Fe-1422 shield case and three options in which tungsten replaces Fe-1422 along the duct at the magnet corner. In Options I, II, and III, the tungsten shield lengths along the duct are, respectively, 0.4, 0.5, and 0.6 m. The extra tungsten shield is added to the side close to the first wall, as shown in Fig. 3-149, because most streaming neutrons will go into that part of the shield. The results show that Option I satisfies the design criteria for polyimide dose and superconductor fluence, but only Option III meets all design criteria. Therefore, we adopted Option III to shield the magnets against neutrons and gamma photons streaming into the duct.

The results for the superconductive shield are summarized in Table 3-60.

Although the results for dpa rate and heat load exceed the limits used for the superconducting magnets, the superconductive shield can be annealed more

Table 3-59. Results for the S-1 superconducting magnets.

	Fe-1422 shield	Tungsten shield		
		Option I	Option II	Option III
Peak dpa rate in copper stabilizer (dpa/FPY)	3.84×10^{-4}	1.1×10^{-4}	5.82×10^{-5}	8.4×10^{-6}
Peak dose in polyimide after 5.4 FPY (rad)	3.56×10^9	9.5×10^8	5.7×10^8	8.26×10^7
Peak magnet heat load (mW/cm^3)	0.1	0.052	0.041	0.025
Peak neutron fluence ($E > 0.1 \text{ MeV}$) in superconductor after 5.4 FPY (n/m^2)	4.16×10^{22}	1.13×10^{22}	6×10^{21}	8.7×10^{20}

Table 3-60. Results for the superconductive shield.

Average heat load (mW/cm ³)	0.47
Neutron fluence (E > 0.1 MeV) after 5.4 FPY (n/m ²)	1.76×10^{22}
Dose in polyimide after 5.4 FPY (rad)	2.57×10^9
dpa rate in copper (dpa/FPY)	2.1×10^{-4}

frequently because of the small volume. Further, total nuclear heating is small and does not require an excessive refrigeration system. The front and rear cryopanel have average heat loads of 0.96 and 0.027 MW/cm³, respectively, which are much lower than the design limit of 10 MW/cm³ used in this study.

An experimental study of radiation effects in Macor³⁶ indicated slight damage by 14-MeV neutrons at a fluence of 10^{22} n/m², which was estimated to correspond to a dose of 6×10^8 rad. Our calculations show that the dose rate in the Macor insulator is 2.17×10^8 rad/FPY, implying that the ion source insulators will last 2.77 FPY as far as radiation is concerned.

3.7.5. End Cell Magnet Shielding

Summary. Shadow shields have been designed to protect the transition and anchor coils against central cell neutrons streaming into the end cell. Four shadow shields are needed. Each shield is 0.45 m thick and 0.5 m wide. The neutron source in the end cell is much smaller than the one in the central cell. No radial shielding is required, and the stainless steel in the magnet case and cryostat will provide adequate shielding against neutrons originating in the end cell plasma.

Requirements. Radiation damage to the transition and anchor coils can result from fusion neutrons generated in the end cell plasma, as well as from the central cell neutrons streaming into the end cell. A limited space is available for radial shielding between the plasma halo and the magnets. This shielding is required to adequately protect the end cell coils from excessive radiation effects.

Discussion. While the peak neutron-linear source density in the central cell of TDF is 1.39×10^{16} (14.1 MeV) n/cm·s, the peak source density in the end cell corresponds to only 2.8×10^{11} D-D (2.45 MeV) n/cm·s and 2.4×10^{10} D-T (14.1 MeV) n/cm·s. The numbers are normalized to give a wall loading of 2 MW/m² at a radius of 25 cm in the central cell. To determine the shielding requirements for the transition (T-1) and anchor (M-2 and M-3) coils, one must consider fusion neutrons originating in both the central and end cells.

The smallest distance between the coils and plasma center line is 25 cm. These coils are surrounded by a 15-cm-thick case and cryostat, which have a total stainless steel thickness of 5 cm.

The peak neutron wall loading at the inner surface of the coils, resulting from neutrons generated in the end cell plasma, is 10^{-5} MW/m². Based on a one-dimensional calculation, Fig. 3-149 gives the dpa rate in copper stabilizer vs shield thickness for different values of the wall loading. The shield has the same composition as that used in shielding the neutral beam injection penetration, namely 56 vol% Fe-1422, 26 vol% B₄C, 14 vol% lead, and 4 vol% water. The limit on the damage rate in copper stabilizer is 5.5×10^{-5} dpa/FPY, which is the same as that used for central cell coils. Previous results for shielding central cell coils revealed that the dpa limit in copper is the design driver; therefore, the shielding requirements for the end cell coils have been determined on the basis of the dpa limit. The results in Fig. 3-149 show that a shield thickness of 3 cm is needed for a wall loading of 10^{-5} MW/m². Hence, the magnet case and cryostat will adequately shield against neutrons generated in the end cell plasma.

Almost all fusion neutrons are produced in the central cell. Because of this, central cell neutrons that stream into end cell may cause excessive damage to the end cell coils. Damage results when central cell neutrons pass through the 17.5-cm-diam opening of the choke coil at $z = 4.5$ m. The neutron linear-source density in the central cell drops rapidly at the choke coil. An effective central cell length, determined by the points at which the source drops to half its peak value, is 6.8 m. Large portions of the end cell coils will be concealed from the direct line of sight of central cell source neutrons.

Coil parts that get a direct contribution from central cell source neutrons are in the axial ranges 7.5 to 8.2 m, 8.6 to 9.0 m, and 11.3 to 11.8 m. The peak neutron wall loading that results from central cell source neutrons streaming into the end cell is 0.009 MW/m², which occurs at the surface of anchor coil M-2, perpendicular to the plasma axis. This wall loading corresponds to a required shield thickness of 50 cm, as shown in Fig. 3-150. If the shielding effect of the magnet case and cryostat are taken into account, only a 45-cm-thick shield is needed.

Figure 3-150. Magnet shielding requirements for different neutron wall loadings.

Using the 17-cm flux lines to determine plasma halo size, we found that only a 5-cm space is available between the plasma halo and the T-1 and M-2 coils and that no shielding space is available for the M-3 coil. Hence, radial shielding cannot be used because of the limited space. However, adequate shielding can be provided with shadow shields, as shown in Fig. 3-151. The shadow shield used at the T-1 coil will adequately shield the parts of the T-1 and M-2 coils that have a direct contribution from central cell source neutrons. Although parts of the T-1 and M-2 coils with y dimensions > 0.33 m and parts of coil M-3 with x dimensions 0.20 m are concealed from the direct line of sight of the central cell source neutrons, the shadow shields have been extended, as shown in Fig. 3-150, to protect other parts of the coils against secondary scattered neutrons. The magnet case and cryostat will adequately protect other parts of the coils, at larger distances from the plasma axis, against scattered neutrons.

3.7.6. Biological Shielding

Requirements. Biological shielding is defined as the nuclear shielding needed to protect working personnel and the public, as opposed to magnet shielding or other component shielding designed to control radiation damage and heating. Magnet and component shielding requirements and designs are discussed fully in other subsections of this report.

Biological shielding requirements for the inside of the reactor room are dictated by the need for personnel access to the room 24 hr after shutdown of the machine, including access to all unshielded equipment and up to the unbroken surfaces of the TDF biological shielding. Dose criteria include a general room level of 2.5 mrem/hr, permissible local dose rates to the hands of 10 mrem/hr, and permissible highly localized streaming dose rates of 100 mrem/hr. A further constraint is that the working staff not receive a cumulative whole-body exposure of more than 20 person-rem/yr.

Biological shielding for the building walls and roof should be designed to limit the dose rate at accessible locations outside the building to 1 mrem/hr at any time during operation or shutdown. This criterion is set lower than the 2.5-mrem/hr maintenance dose rate inside the building because of the possibility that nonradiation workers (i.e., clerical help, vendors, visitors, etc.) will have access to the outside of the building. The annual dose at the site boundary should be limited to 10 mrem.

Figure 3-151. Shield configuration for the transition and anchor coils.

The release of radioactive gases does not affect the biological shield design, but is considered elsewhere in this report in connection with tritium system design.

Shield Design

Reactor. Shutdown gamma radiation originates from induced activation in the outer layers of biological shielding and in unshielded equipment just outside the shield, such as the support structure, crane bridge, etc. Earlier work on ETF and FED indicated, should the outboard shielding reduce the operating neutron leakage to a level of $\sim 2 \times 10^6$ n/cm²-s, that activation of the shield and outlying components would be kept low enough to result in a general shutdown dose rate of 2.5 mrem/hr 24 hr after shutdown. Therefore, the conceptual design of the TDF biological shielding is based on reducing the operating external neutron leakage to 2×10^6 n/cm²-s.

To limit nuclear heating in the solenoid coils, 0.9 m of shielding is incorporated between the plasma chamber and the solenoid coils of the central cell. However, an overall shield thickness of at least 1.2 m, assuming an optimized SS/H₂O or SS/B₄C configuration, is needed to reduce the leakage to 2×10^6 n/cm²-s. Therefore, additional biological shielding is incorporated between the solenoid coils to achieve this thickness. The solenoid coils are thick enough to double as biological shields in those locations.

The central cell beamlines are enclosed in biological shielding consisting of 2 ft of heavy concrete. Other penetrations into the central cell must have local shielding to prevent excessive streaming and activation.

Biological shielding requirements for the anchor cell are dictated by the intense neutron beam entering this cell from the central cell-- 2×10^{14} n/s-- as well as a local D-D source of 2×10^{14} n/s. With no shielding, the surface current would be about 3×10^8 n/cm²-s. About 2 ft of heavy concrete is needed around the anchor cell to reduce this current to 2×10^6 n/cm²-s.

Because of its large size, the end cell experiences a much lower average neutron flux on its surfaces (except on the center line). Except for the addition of a required shadow shield over the particle dump region, biological

shielding of 1 to 2 ft of ordinary concrete is estimated to be adequate for the end cell.

Building. The concrete walls and roof of the building serve as biological shielding, as well as structure and containment. The wall shielding requirement is dictated either by (1) attenuation of the operating neutron dose rate of ~200 rem/hr, or (2) attenuation of the activation gamma dose rate from a remotely removed portion of the central cell being transported to a hot cell without shielding. Roof shielding is dictated by the skyshine neutron dose rate during operation for a detector outside the building on the ground level.

No calculations for TDF were performed in this phase of the study, but earlier results for FED indicated a 2-m-thick wall of ordinary concrete and a 1.3-m-thick roof, both preferably containing boron or having a low sodium content. Until more definitive information is generated for TDF, the walls are shown as being 2 m thick.

3.8. TEST FACILITIES

3.8.1. Summary

A key issue in TDF design is the development of testing capabilities that ensure the fusion community receives maximum benefit from this machine. Consequently, TDF test facilities development and test planning have proceeded logically, beginning with the consideration of general requirements for various types of fusion tests and, ultimately, with requirements for specific reactor blanket designs.

Five generic types or categories of tests are suitable for such a fusion device. They include two basic types of fusion engineering tests: blanket module and materials tests. Although these terms are not mutually exclusive, we generally distinguish between integrated blanket module tests, including materials interactions, thermal hydraulics, etc., and isolated long-term materials tests. Integrated tests are generally distinguished by short- to medium-term tests in easily accessible modules, while isolated tests are distinguished by moderate- to long-term tests of single specimens, possibly in remote areas of the machine. Short-term materials tests may generally be considered in either category.

The specific flux and fluence requirements provided herein are appropriate to blanket concepts currently under consideration. To develop test capabilities and program plans appropriate to realistic future fusion reactors, we present a generic test categorization for specific blanket concepts and use two blankets--MARS and the Fusion Breeder Program (FBP) hybrid design--to illustrate how the specific requirements for various blankets can be systematically categorized and integrated into TDF testing plans.

After we have compared the blanket requirements with current TDF design capabilities, we develop the key design impacts of the testing requirements, including specific locations of the materials and blanket module test areas and of the blanket test area configuration itself. The key factor in blanket module configuration is the vacuum seal location because short test times and rapid turnaround are important to the blanket test program. The result is an optional blanket port module, with an integral first-wall vacuum seal that

allows single blanket modules to be replaced rapidly without breaking the integral machine vacuum. This port could be replaced by integrated blanket modules (90- to 360-deg circumferential extent) that have their own external seal and, if desired, optional first wall. We compare the TDF capabilities with the previously developed test requirements, as well as with the capabilities of other proposed machines, and show that the TDF concept can meet almost all of the desired blanket and materials test requirements.

Finally, we discuss the remaining key technical issues, including the development of specific test plans that are coordinated with the TDF machine operating plan and with the individual blanket modules to be tested. Ultimately, the blanket test modules and the materials test specimens and configurations must be designed.

3.8.2. Requirements

These can be broken into two categories: blanket test and materials test requirements. Although the two are clearly related, specific testing capabilities in the TDF configuration allow for two distinctly different test environments. They are (1) long-term testing in relatively inaccessible parts of the machine and (2) short-term testing within removable modules that can be replaced or reconfigured relatively quickly.

We generally refer to two separate sets of tests and test requirements relevant to TDF: long-term materials tests (especially structural materials tests) suitable for long-term in-place testing and blanket module tests, including short-term materials and materials compatibility tests that can be performed within removable blanket modules.

Nuclear test requirements relevant to a wide range of future-generation fusion reactors can be conveniently divided into five categories: neutronics, thermal hydraulics and mechanical effects, tritium breeding/extraction, radiation chemistry effects, and radiation damage testing. Each category has a distinct set of test environment requirements that can cover a range of future reactor concepts. The first four categories generally cover blanket module testing, while the last category is basically for materials testing.

The basic blanket module test requirements are:

1. Neutronics tests require seconds at low flux.

2. Thermal hydraulics tests require minutes, up to the order of an hour, at moderate flux levels of 0.5 MW/m^2 or greater.
3. Tritium breeding and extraction tests can require from 1 to 10 hr for liquid breeders to about 300 hr for solid breeders, at a wall loading of 1 MW/m^2 . Although essentially 100% availability would be required during this period, small outages could be tolerated, especially early during a run.
4. Radiation chemistry testing, including materials compatibility effects and other integrated blanket tests, would require the longest test periods of the blanket module tests. On the order of 1000 hr would be needed to demonstrate integrated module operation and to obtain indications of radiation chemistry effects, while two or more years would be needed to qualify a blanket for a next-generation machine.

In addition, for a hybrid blanket, fuel-cycle equilibrium testing would be required over a full cycle. This would be comparable to testing requirements for category 4.

In category 4, and in the following discussion on materials testing, we distinguish between moderate-length demonstration or confirmation testing and long-term qualification testing. In the former we refer to tests that indicate whether a specific design functions as it was intended to function (a demonstration) or if specific designs or materials previously selected for future machines are appropriate, within the time and fluence constraints required, to meet time lines for the design or construction of such a machine (confirmation). By qualification testing, we mean a long-term test or series of tests that verifies that a blanket concept or specific material actually meets or is expected to meet the ultimate end-of-life conditions anticipated in the machine environment for which it is intended. This includes testing materials until actual end-of-life conditions are met or can be predicted reliably; it also includes testing modules until materials interactions, normal and abnormal conditions, overall reliability, and other factors are shown to meet module end-of-life criteria. In either case, qualification will generally imply tests following, or concurrent with, demonstration and confirmation testing, but of a longer and more rigorous nature.

Some materials test requirements follow:

- Testing of certain special materials, e.g., insulators, requires only low to moderate fluence (up to 0.1 MW-yr/m^2), and is not a limiting factor in setting machine requirements.
- At higher fluence levels (about 1 MW-yr/m^2), a fusion spectrum can provide useful data for scoping studies of structural and other materials.
- Longer-term materials testing, and materials qualification testing for a circa 2000 Engineering Test Reactor (ETR/DEMO) machine requires fluences of about 2 MW-yr/m^2 at typical wall loadings of 1 MW/m^2 .
- Materials qualification for commercial prototype machine end-of-life conditions requires fluences of about $5\text{--}15 \text{ MW-yr/m}^2$ at wall loadings on the order of 3 MW/m^2 .

Table 3-61 lists the five categories of blanket and materials tests and gives typical flux, test length, and fluence requirements for each. The categories are listed in order of increasing demand on test facility capabilities.

As this table indicates, the lowest level requirements cover neutronics testing, including tritium breeding verification, that can be performed over very short time periods at relatively low flux and fluence levels. Thermal hydraulic and mechanical tests require flux levels ideally within about a factor of 2 of the desired level of operation, i.e., approximately 1 MW/m^2 for a 2-MW/m^2 ETR or DEMO, and 3 MW/m^2 to possibly 5 MW/m^2 for a commercial prototype reactor. For thermal hydraulic and mechanical tests, tens of minutes would be required to achieve thermal equilibrium, during which time transient data can be obtained, followed by steady-state operation to obtain equilibrium condition information. Because of the minimal time and fluence requirements of this and the neutronics categories, such tests would be especially suited for the early stages of TDF operation when high long-term availability would not yet be attained. The following categories, all of which require longer term testing, could then be performed later in the machine life when higher availability would be expected.

The requirements for tritium breeding and extraction tests depend on the specific tritium breeding material in the blanket. These tests, as distinct from those in category 1, will be carried out up to equilibrium concentrations

Table 3-61. Categories of fusion test requirements.

Category	14-MeV neutron wall loading (MW/m ²)	Test length at 1 MW/m ²	Fluence per run (MW-yr/m ²)
1. Neutronics Power deposition Neutron calorimetry Tritium breeding	>0.01	0.1 to 10 s	10 ⁻⁹ ~ 10 ⁻⁷
2. Thermal--hydraulic Thermal--mechanical	>0.5	0.1 to 1 hr	10 ⁻⁵ ~ 10 ⁻³
3. Tritium breeding/extraction Liquid metals Solid breeders	>0.5	1 to 15 hr 10s to 100s of hours	10 ⁻⁴ ~ 10 ⁻³
4. Radiation chemistry effects Testing for corrosion and material property changes Qualification	~1.0 >1.0	~1,000-10,000 hr 2 ~ 10 yr	0.5 ~ 1 1 ~ 5
5. Radiation damage testing for structural materials ETR (confirmation) Prototype (qualification)	~1.0 2 ~ 4	2 ~ 4 yr 2 ~ 10 yr ^a	1-2 5-10

^aAt 2 to 4 MW/m².

of tritium, while the category 1 tests will only need to produce minimal levels of tritium sufficient to verify that tritium has indeed been produced. Because solid breeding materials retain larger tritium concentrations, longer test times will be needed for these materials to reach equilibrium. Hence, Table 3-61 lists separate test times for liquid metals (^{83}Pb - ^{17}Li and Li) and solid breeders (^{20}Li and LiAlO_2). In general, test times for liquid metals range from 1 to 15 hr with a first wall flux of 1 MW/m^2 , while those for solid breeders range up to several hundred hours. Essentially 100% availability would be required during these intervals because tritium levels would begin to decrease as soon as the neutron source was removed. Although this would present no particular problem early in the test interval, any drop in tritium level as the test progresses and higher levels of tritium are attained would take progressively longer to make up; this is especially true for liquids if extraction tests are being performed.

Tests of radiation chemistry and radiation-induced material changes will entail durations on the order of 1000 hr and up so that performance-degrading and lifetime-limiting problems can be examined. Many radiation-induced materials compatibility and corrosion problems can be examined in fission reactor environments. However, additional tests in a 14-MeV neutron environment will still be necessary to ensure that unforeseen materials compatibility or corrosion problems do not occur in blankets in later fusion reactors; those tests carried out in fission reactor environments will still be useful, especially in screening out potentially unsuitable materials, thereby reducing the number of candidate materials that TDF will have to test. This 1,000- to 10,000-hr test category also includes radiation-induced changes to material properties such as thermal conductivity, density, porosity and resistivity. Solid breeding blankets are especially sensitive to radiation-induced restructuring of ceramic tritium breeding materials, which will change tritium holdup characteristics and thereby affect the results of the tritium breeding/extraction tests. Radiation-induced changes in resistivity can significantly degrade the performance of high-field copper magnets. Finally, this category includes long-term blanket qualification testing, which is intended to examine those few module concepts and materials combinations that the shorter-term tests have indicated as being prime

candidates for future-generation blanket modules. Possibly two years would be desirable for a late 1990s "next-generation" device, while even longer periods, up to 10 yr, would be useful if TDF were used to test a blanket concept for a prototype reactor.

Structural radiation damage testing is divided into (1) materials confirmation tests for an ETR type machine and (2) materials qualification for a prototype machine. Because TDF would probably not operate long enough in advance of an ETR to be used to determine suitable materials choices, its most useful application would be to confirm those materials choices already made and to indicate any potential problems as early as possible. For a prototype machine however, TDF would operate early enough to select materials and to qualify them as well. The former tests would require fluences on the order of 1 MW-yr/m^2 to confirm the suitability of ETR blankets operating at 2 MW-yr/m^2 , while the latter would require fluences of about 5 MW-yr/m^2 or greater to qualify prototype materials candidates to fluences of 10 MW-yr/m^2 or greater.

3.8.3. Discussion

Design Issues. To more fully define TDF design requirements and develop an appropriate design, more specific testing requirements must be identified for fusion reactor designs now under consideration. To facilitate this, a generic categorization was developed to permit the needs of various fusion devices to be presented in a similar format useful in quantifying those factors important to the design of TDF test facilities. This generic test categorization is shown in Table 3-62.

The categories of information included for each test (among the test categories already discussed) include the type of test and the parameters to be monitored, the duration, the number of times and different test conditions, and the availability requirements. Together, these factors indicate the total test time required for each category of test. Table 3-62 also includes examples of the kinds of information appropriate to each column.

Preliminary test needs for two blanket concepts--MARS and the FBP hybrid blanket--appear in Tables 3-63 and 3-64. We chose these two concepts to illustrate some of the differences in TDF test requirements resulting from

Table 3-62. Generic test categorization. Full power and 100% availability are assumed for the duration of the test unless specified otherwise.

Test	Desired information	Duration	Number and conditions	Parameters monitored	Availability requirements	Total and time	Modules
Neutronics Thermal hydraulics Tritium breeding	Neutronics MHD effects Flows, temperatures Coefficients Tritium breeding ratio	About 5 hours	3 configurations with several tests per configuration	Temperature Neutron flux Coolant temperature, flow, pressure profiles Tritium level	100% over 5 hours	About 30 tests 150 hours	3 (or 1, configurable)
Fuel breeding: one full cycle	Neutronics Afterheat levels Irradiation effects	3 full-power months (6 at 50% availability)	Two conditions	As above, plus Out-of-reactor tests	50% over 6 months	2 tests 12 months total	1 (configurable)
Radiation effects/qualification threshold	Irradiation damage to qualification threshold	2 years (4 at 50%)	1 condition (nominal)	Flux Temperature Out-of-reactor tests	50% over 4 years	1 test 4 years	1
Intermodule seals ^a		5 hours-- 3 months	1 condition (nominal)	Flux Temperature Out-of-reactor tests	50%	1 test, up to 6 months	1

^aRequires 360-deg module. Could be obtained from a different blanket module concept.

Table 3-63. MARS blanket test categorization. Full power is assumed for the duration of the test.

Test	Desired information	Duration	Number and conditions	Parameters monitored	Availability requirements	Total and time	Modules
Neutronics	Energy multiplication Fraction of high-temperature heat	0.1 to 3 days (steady state)	About 5 configurations, several tests per configuration with various Coolant temperature	Neutron flux Temperature Coolant temperature, flow, pressure profiles Transient thermal effects Tritium level	100% over 1 to 3 days	>50 tests 150 days (one year at 50% duty cycle)	5 (or 1, configurable)
Thermal hydraulics	Heat-transfer properties		Lithium enrichment	Tritium in coolant			
Tritium breeding	Tritium breeding ratio Tritium permeation rates Initial burn-up rates Short-term transmutation products		Lithium quantity in solid breeder	Strain Out-of-reactor tests Chemical composition (mass spec.) of coolants	solid	(mass spec.) of	
Radiation chemistry effects	As above with longer-term irradiation tests Burnup	3 to 6 months (full power)	One condition (nominal)	As above, with priority toward materials interactions and tritium	50% over 6 to 12 months	One test, 6 to 12 months	1
Longer-term radiation effects/qualification	Longer-term effects of fusion neutron spectrum on material combinations Transmutation products Chemical reactions	1 to 2 years (full power)	One condition (nominal)	As above	50% over 2 to 4 years	One test, 2 to 4 years	1
Intermodule seal ^a		Up to 3 months	One condition (nominal)	Flux Temperature Out-of-reactor tests	50% up to 6 months	One test, up to 6 months	1 ^a

^aRequires 360-deg module. Could be obtained from a different blanket module concept.

Table 3-64. Fusion Breeder Program blanket test categorization.

Test	Desired Information	Duration	Number and conditions	Parameters monitored	Availability requirements	Total and time	Modules
Neutronics	Energy multiplication Fraction of high-temperature heat	0.1 to 3 days (tritium steady-state)	About 5 configurations, several tests per configuration with various	Neutron flux Temperature Coolant temperature, flow, pressure profiles Transient thermal effects	100% over 1 to 3 days	<50 tests 150 days (one year at 50% duty cycle)	5 (or 1, configurable)
Thermal hydraulics	Heat-transfer properties		Coolant temperature	Tritium level			
Tritium breeding	Tritium breeding ratio Tritium permeation rates Initial burn-up rates Short-term transmutation products		Lithium enrichment Lithium quantity in solid breeder	Tritium in coolant Strain Out-of-reactor tests Chemical composition (mass spec.) of coolants	solid	(mass spec.) of	
Radiation chemistry effects	As above with longer-term irradiation tests Burnup	3 to 6 months (full power)	One condition (nominal)	As above, with priority toward materials interactions and tritium	50% over 6 to 12 months	One test, 6 to 12 months	1
Longer-term radiation effects/qualification	Longer-term effects of fusion neutron spectrum on material combinations Transmutation products Chemical reactions	1 to 2 years (full power)	One condition (nominal)	As above	50% over 2 to 4 years	One test, 2 to 4 years	1
Intermodule seals ^a		Up to 3 months	One condition (nominal)	Flux Temperature Out-of-reactor tests	50% up to 6 months	One test, up to 6 months	1 ^a

^aRequires 350-deg module. Could be obtained from a different blanket module concept.

quite dissimilar blanket concepts. Although the most obvious difference between the two designs is the fuel-breeding nature of the hybrid blanket, there are others as well. They include wall loading (5 MW/m^2 at 59 cm for MARS vs 1.5 MW/m^2 at 1.5 m for FBP), tritium breeding material (^{83}Pb - ^{17}Li and LiAlO_2 for MARS vs Li for FBP), and coolant/multiplier (^{83}Pb - ^{17}Li coolant/multiplier for MARS vs Li coolant and Be multiplier for FBP). As a result, a number of generic test issues and design-dependent blanket factors can be enumerated. The generic issues include:

- Tritium breeding performance.
- Afterheat and afterheat cooling.
- MHU pressure drop.
- Intermodule seal performance.
- Heat transfer and heat removal.
- Materials compatibility, chemical effects and transmutation products.
- Structural effects, including thermal stress, differential expansion, LME, DBTT, temper embrittlement, helium generation, and corrosion.
- Abnormal conditions, including a loss of coolant flow (LOCF) and loss of coolant accident (LOCA).

Design-dependent issues for MARS include sintering, tritium solubility and diffusion in solids, and the rate of formation of T_2 . Specific hybrid issues include the choice of coolant (Li vs ^{83}Pb - ^{17}Li), structure (ferritic vs stainless steels), breeding ratio, fission rate, and fuel handling.

In addition to such test issues, the TDF machine and various materials and blanket test modules must undergo design verification testing before full D-T operation can begin. Although larger, higher-power machines are expected to require up to three years of initial testing, TDF's smaller size and power may necessitate only one year of such tests before reasonably intensive operation can begin. Two factors are important. First, all blanket modules must undergo extensive testing before they are qualified for use in TDF. They must appear suitable as candidates for machine testing and demonstrate high reliability, to preclude extensive negative impacts on TDF availability. Second, the test schedules should be coordinated with the overall TDF startup and long-term test phases to make maximum use of the testing times available.

The preceding discussions indicate a wide range of desirable test capabilities for TDF. Although specific TDF capabilities are dependent on many competing design issues, some key features important to the success of TDF as a test facility are:

- Steady-state, as opposed to pulsed, environment.
- Integrated neutron/heat-generation/magnetic conditions.
- Large area and volume relative to other high-flux/fluence 14-MeV neutron sources.
- Realistic fusion neutron spectrum.

Key TDF capabilities relative to testing requirements include 1.4 MW/m^2 wall loading at 25 cm radius, 7 m^2 of available test area (two 1-m-long blanket regions, and three long-term materials test regions), high short-term availability, if desired, neutron flux combined with 10 W/cm^2 surface heat flux.

With these capabilities, it is possible to compare TDF to other fusion-relevant neutron facilities. Table 3-65 compares the features of several existing and proposed test facilities. The machines considered include, in addition to the current TDF concept and current FMIT, FED, INTOR, and TASKA designs, proposed upgrades of TFTR (TFET-I, TFET-II, and LITE), an MFTF-B upgrade, and a possible TDF upgrade (TDF-SP). The operational requirements for TFET-I are taken from the Princeton Plasma Physics Laboratory proposal. They include pulsed operation with 6-s pulses at a wall loading of 0.2 MW/m^2 once every 600 s for periods of several hours, and a total lifetime of 10,000 D-T shots. The capabilities of TFET-II and LITE are taken from information provided by Paul Reardon to Panel Three of the Magnetic Fusion Advisory Committee on July 29-30, 1982. We have assumed that TFET-II and LITE will have the same fluence limit as TFET-I. The MFTF-B+ capabilities are from current studies underway at LLNL to examine the feasibility of operating MFTF-B in a D-T mode. In this mode, an upgrade would operate at a steady-state first wall flux of about 1 MW/m^2 for continuous runs lasting as long as 10 hr. We estimate a facility availability range of 1 to 5% for this concept. Finally, the TDF-SP concept represents a possible TDF improvement incorporating stable end plugs, which could give a first wall flux of from 2 to 3 MW/m^2 with availabilities between 50 and 70%.

Table 3-65. Fusion neutron facilities.

Facility	Flux (n/cm ² -s)	Duration (s)	Area (cm ²)	Neutron spectrum
FMIT	5 x 10 ¹⁴	SS	100 cm ³ (vol)	Nonfusion
	1.5 x 10 ¹⁴	SS	1000 cm ³ (vol)	Nonfusion
TDF	6-9 x 10 ¹³	SS	70,000	Fusion
TDF-SP	9-13 x 10 ¹³	SS	70,000	Fusion
TASKA	7 x 10 ¹³	SS	78,500	Fusion
INTOR	5.8 x 10 ¹³	100-200	120,000	Fusion
MFTF-Upgrade	4.4 x 10 ¹³	SS	10,000	Fusion
FED	2.2 x 10 ¹³	100	120,000	Fusion
TFTR upgrades:				
TFET-I	0.9 x 10 ¹³	6	100,000	Fusion
TFET-II	4 x 10 ¹³	10	~180,000	Fusion
LITE	15 x 10 ¹³	10	~180,000	Fusion

From Table 3-65, we can see that TDF combines a high flux with a large surface area, thereby allowing reasonably sized blanket modules to be tested, at the same time as a large number of small materials samples. However, these basic TDF characteristics must be used carefully in conjunction with blanket module design and test program planning to realize the maximum potential of the TDF concept. Thus, we will consider the previously discussed test requirements and their impact on the design and use of TDF test facilities.

The following characteristics are important with regard to materials specimen testing within the constraints of the basic TDF concept:

- Because of its small radius, TDF is characterized by the rapid radial falloff of neutron flux and, therefore, of α pa and helium production.
- Materials tests tend to be long-term relative to blanket module tests.
- Although many materials tests are passive, some have temperature, stress, or other requirements that will require interconnections with power or instrumentation.
- TDF will have about 10 W/cm^2 of particle flux (100 eV from the halo, and 30 keV from the plasma), which must be considered.

These characteristics lead to several design considerations. First, to maximize the test volume of useful materials in light of the rapid radial falloff, maximum use must be made of all available surface area for testing. For materials tests that can be carried out over the full life of the machine, this means utilizing even relatively inaccessible areas near the first wall. For moderate-term tests, considering that blanket module testing will require more frequent changeout than will materials test specimens, materials specimens can be located in areas adjacent to blanket modules, accessible whenever a blanket module is removed. However, some space must be available to allow for penetrations for electrical interconnections with specific materials test specimens. Finally, shorter-term materials tests may be conducted as part of the blanket module test program itself.

As seen in Figure 3-152, these requirements result in the use of all space in the central cell region not already used for blanket modules. These areas, denoted by asterisks, will be accessible whenever the appropriate blanket module is removed. Figure 3-153 shows a more detailed view and indicates the access for cables or coolant channels running between the magnet

Figure 3-152. TDF test configuration.

Figure 3-153. Detailed TDF test configuration.

bulk shields and the blanket modules. It also shows the location of a replaceable tube-bank first wall. Although many materials samples will require shielding from the plasma and halo neutral particle flux, this first wall will be removable, to allow for testing of unprotected samples, as well as for maintenance and replacement.

It is also important to consider two other factors: wall loading and availability. Over the long term, 50% (or greater) availability is highly desirable to maximize fluence capabilities for end-of-life testing. Alternatively, the same effect can be obtained by increasing the ultimate wall loading at a lower availability. In either case, short-term materials tests will also benefit because tests requiring lower fluence could be run more quickly; this would make the results available earlier and have a greater potential for impacting the choice of future materials to be tested. The shorter-term materials tests would also benefit from the quickest possible attainment of high availabilities early during the life of TDF. The key design considerations that result from these materials test requirements are thus

- (1) maximizing the attainable wall loading (up to 2 MW/m^2 needed to attain about 100 dpa over the machine lifetime) and
- (2) capitalizing on the small size and power of TDF and incorporating redundancies to realize a high availability over the machine lifetime.

Issues more relevant to the design of the materials test capsules and specimens themselves than to the design of TDF include temperature control, stress, pressure, instrumentation, etc. Although these issues must be addressed during the specific design of materials tests themselves, they must be pointed out here because they affect TDF design. For example, temperature control of materials specimens is expected to require temperatures constant to about 1 to 2% over a range of 50 to 700 °C. This will entail both heating and cooling and, therefore, require both electrical connections and piping and the pumping of coolants. Appropriate access must be provided for these lines and provisions made in case of failure, especially for pipe breaks. Finally, for both materials and blanket module tests, remote handling capabilities must be developed consistent with the testing requirements outlined here and with those determined later.

blanket module test requirements place an entirely different set of constraints on IDF test facilities and test program considerations. The two basic requirements are short-term availability and turnaround times. The short-term availability requirements are set by the following:

- Large numbers of tests will entail less than about 30 hr of continuous testing.
- Many of these tests will tolerate 1 to 2 hr of downtime, especially early in the test period.
- A single hour of downtime late in the test period could necessitate a 4- to 5-hr increase in test length, especially near the end of a 30-hr test.
- Therefore, in determining machine availabilities, 1 to 2 hr of downtime in 30 might be tolerable. However, a 4- to 5-hr downtime (during a 30-hr test) would probably necessitate a restart after allowing recovery to pre-test conditions.
- Shorter tests, although proportionately more stringent, should be more easily accommodated.

Some considerations impacting turnaround times follow:

- To maximize total machine availability, turnaround times should be no longer than the required test times.
- Many tests need run only on the order of 30 hr.
- To maximize the usefulness of IDF for blanket module tests, modules should be replaceable after 10 to 60 hr in place.
- Therefore, turnaround times should be no longer than 60 hr.

These considerations impact the IDF design in two ways: (1) the blanket modules must be easily accessible, and (2) the vacuum seal design is a key design feature. The considerations resulted in a significant number of trade-offs in vacuum seal location, with a final design incorporating the features of seal locations both within and outside the test area. Overall design considerations made an external seal highly desirable (see Fig. 3-154). For blanket tests requiring rapid turnaround, a blanket port concept with a dual vacuum seal at the top was devised. This concept allows a test module to be inserted into a port through the dual seal without breaking the overall machine vacuum; however, it requires a first wall vacuum seal in the blanket

NUCLEAR TESTING REQUIREMENTS AND FACILITY CAPABILITIES PER RUN

Figure 3-154. Neutron fluence requirements and capabilities per run.

port itself. Because this extra wall is not desirable for some blanket tests, additional blanket modules will be designed to fit directly into the module test space with the port removed.

The resulting blanket module concept incorporates the following features:

- Two 360-deg module test areas, each with a useful width of about 1 m.
- Circumferential module use, including (1) one 360-deg module, (2) one 180-deg module and two smaller modules, and (3) three 120-deg modules.
- An optional port concept to allow rapid changeout for short-term tests.
- An optional first wall to allow protected and unprotected module testing.

Two additional figures indicate how TDF performance matches the design requirements of the five categories described earlier and compares with the capabilities of several other fusion facilities. Figure 3-154 illustrates the wall loading and fluence requirements per test for the five test categories and is based on the data in Table 3-61. The TDF capabilities assume a wall loading of $1.4\text{--}2.0\text{ MW/m}^2$. Figure 3-155 similarly plots wall loading and total fluence over the machine lifetime. The large boxes in each figure represent approximate limits to the requirements for each of the five test categories, with specific machine capabilities superimposed over them. As can be seen, TDF and TASKA are the only machines that closely match almost all of the requirements; however, structural materials damage testing for prototype qualification could be accomplished only with a higher wall loading (in the range $3\text{ to }4\text{ MW/m}^2$) or an extended lifetime machine with increased availability (up to 70%).

3.8.4. Key Technical Issues

Two key items remain: the development of actual test plans and schedules and the design of specific materials test configurations and blanket test modules. Both issues are, of course, closely related. Specifically, the test plans must be closely coordinated with the design capabilities of the blanket modules themselves and with the capabilities and operating schedule of TDF.

Figure 3-155. Total integrated neutron fluence requirements and capabilities.

To develop appropriate test plans, we must match the test-length factors discussed previously with the final TDF operating plan to ensure that tests are coordinated with expected machine availabilities and wall loading. This is also subject to the timing and sequence requirements of the tests themselves. In general, however, it should be possible to run many short-term tests (e.g., neutronics, thermal hydraulics) early in the TDF lifetime, when availabilities are expected to be low. In fact, a natural progression from many short-term tests and initial integrated blanket module tests to final long-term blanket qualification testing would coordinate well with the expected increase in availability over the lifetime of TDF.

Finally, specific blanket modules must be designed on the basis of several factors. First, machine and blanket designs desirable for testing in TDF must be selected. This will almost certainly change with time, and new designs may become desirable for testing during the lifetime of TDF. In addition, as early designs are tested in TDF, modifications or complete design changes may be indicated by test results. The requirements for each blanket concept must be considered in light of the various blanket test configurations available for TDF (90- to 360-deg circumferential module or blanket port). Further, since TDF is a small machine relative to a DEMO or prototype device, the issue of act-alike vs look-alike modules is important. Clearly, TDF cannot test full-sized (or "look-alike") blanket modules; therefore, to ensure a test module that closely represents the expected performance of a full-scale module under actual operating conditions, accurate scaling is extremely important. For example, since heating profiles and thermal hydraulics are important test considerations, the effects of geometrical scaling down of piping, flow channels, etc., must be carefully considered. This requires scaling flow velocities to preserve Reynolds and Prandtl numbers in steady-state operation. Simulation of transient conditions will require detailed consideration of many other factors.

As tests on preliminary blanket concepts are completed, final blanket concepts for long-term testing within TDF must be developed on the basis of a careful screening of earlier results. In this way, maximum use can be made of the TDF test facilities, with a minimal potential of damage to either the modules or the TDF facilities themselves.

3.9. VACUUM PUMPING SUBSYSTEM SUMMARY

The TDF vacuum pumping system is comprised of three types of pumps, each with a different function. (1) Deuterium, tritium, and impurities are pumped on LHe-cooled, LN_2 -shielded cryocondensing panels that are deployed within the main vacuum envelope of the end cells and the neutral beamlines. (2) The helium gas resulting from the D-T fusion reactions is pumped on LHe-cooled, LN_2 -shielded cryosorption panels that are contained in small vessels appended to the end cells with isolation valves. The sorption material is argon gas. (3) The initial pumpdown or vessel "rough pumping" is accomplished by an external mechanical pumping system that also pumps degassed effluent from the D-T and helium cryopumps to the external fuel cleanup system.

3.9.1. General Vacuum Requirements

The general vacuum requirements for TDF include:

- Neutral densities.
- Neutral beam drift spaces.
- End cell regions.
- Continuous operation.
- Minimization of the D-T inventory within the vacuum envelope.
- Delivery of pumped gases in a manner compatible with the fuel processing system.
- Compatibility with TDF environment and availability.
- Ease of maintenance.
- Installation/removal.
- Leak-checking/repair.

Because TDF is meant to run in the steady-state mode, the pumping system must also have that capability. The main cryocondensing (D-T pumps) panels are designed to be degassed in discrete, sequential groups while the remaining panels continue to operate unperturbed by the degassing process. Degassed effluent from the cryopanel passes through an internal (to the cryopanel) isolation valve, where it is collected on a much smaller LHe-cooled panel enclosed in a LN_2 -cooled gas-tight shroud. Then the isolation valve is closed, the collector panel is degassed, and the gas is discharged to the

external mechanical pump and into the fuel cleanup system. The appended helium pumps operate in a similar manner; half of them are open to the vessel and pumping while the other half are isolated from the vessel and degassing into the external, mechanical pumping system.

3.9.2. Discussion

For such a scheme to work, the installed pumping capacity must be greater than that needed to maintain the required system pressure. The excess capacity must degas in a continuous, sequential manner at a rate equal to the throughput of the reactor. Further, because the degassing flow rate must be equal to the gas input and only a fraction of the total pumping system is degassing at any time, the degassing process must take place at a pressure several orders of magnitude higher than the required system pressure.

The quantity of gas accumulated on the cryopanel and collector panels (degassing pumps) during steady-state operation (gas inventory in the vacuum vessel) is related to the gas throughput, degassing cycle time, excess capacity fraction, and gas leakage into the vessel during degassing. Assuming the degas leakage is negligible (pressure rise less than a factor of two), the gas inventory can be determined from

$$I = Qt_d (3/2\gamma - 1)/\gamma - 1 ,$$

where Q is the gas throughput, t_d is the degassing cycle time, and γ equals 1 plus the fraction of excess capacity.

A constraint on the regeneration of cryopanel for TDF (or any fusion reactor) is the amount of tritium allowed to accumulate on the panels. Although this amount has not been fixed for TDF, the objective is to make it the minimum practicable.

3.9.3. Design

The specific design requirements set by TDF physics for the end cell vacuum pumping system were to maintain the neutral gas pressure at less than 5×10^{-6} Torr at a gas flow rate of 124 Torr-liters/s for deuterium,

47.6 Torr-liters/s for tritium and 0.17 Torr-liters/s for helium under steady-state conditions. Table 3-66 shows the significant parameters of the end cell vacuum pumping system.

The TDF continuous cryopumping system is based on a regenerable design developed and tested at LLNL. It can be regenerated without reintroducing an unacceptable portion of the pumped gas back into the high-vacuum space. A photograph of the prototype tested at LLNL is shown in Fig. 3-156. The pump features a set of LHe-cooled panels for condensing the deuterium. Thermal shielding for the panels is provided by a series of "Z"-shaped double chevrons that, when open, allow deuterium to pass to the condensing panels from two sides. For regeneration, closed louvers isolate the condensing surfaces from the high-vacuum space. The liquid helium is then purged from the condensing panels, releasing the condensed deuterium. The deuterium flows through an isolation valve between the closed panel and the LHe-cooled collector pump, which can be periodically degassed to the exterior mechanical pumping system.

Figure 3-157 shows how this regenerable pumping technique is applied to TDF. The 2-by-4-m panel shown is one of 72, half of which are deployed in each end cell. A degassing unit is comprised of six panels, each having an isolation valve leading to a common collector pump shroud. The louver actuator is also common so that the six panels can be simultaneously degassed into their collector pump. A six-panel assembly is shown in Fig. 3-158. This assembly is designed as a self-contained unit with all of its service penetrations mounted on its vacuum cover plate. Thus, it can be installed, operated, and removed as a separate entity. Of the 12 six-panel units for pumping deuterium and tritium in the end cells, 10 are always pumping while 2 are degassing.

Figure 3-159 shows the helium-appendage pumping installation in the end cells. There are four separate appendage pairs (8 pumps), two pairs in each end cell. Each pump contains two argon-cryotrapping panels having a total pumping area of 7.1 m^2 . Each pair share a common flapper valve that alternately opens and closes the 1.3-m-diam opening to the appended vessels, allowing four to pump while the other four are degassing.

Figure 3-160 shows the details of the helium-argon cryotrapping panel. The louvers on these panels are fixed because the flapper valve serves to isolate them during the degassing cycle.

Table 3-66. End cell vacuum parameters.

Gas	Flow rate (Torr- liters/s)	Panel area pumping (Am^2)	Specific pumping speed (s-liters/ s-cm^2)	Total pumping speed (s-liters/s)	T-1 degassing fraction	Total panel area (A-m^2)	Pressure (Torr)
T	47.6	960	6.4	6.1×10^7	0.2	1152	7.8×10^{-7}
D	124	960	7.75	7.44×10^7	0.2	1152	1.7×10^{-6}
He	0.17	28.4 ^a	3.2 ^a	9×10^{5a}	1	56.8 ^a	----
		5.31 ^b	11.1 ^b	5.9×10^{5b}	1	10.62 ^b	2.9×10^{-7}

Total pressure - 2.77×10^{-6} .

^aCryopanel area.

^bAppended pump-entrance area.

Figure 3-156. Prototype of the continuous cryopumping system tested at LLNL.

Figure 3-157. Regenerable cryopumping panel concept for TDF.

Figure 3-158. One of 12 six-panel degassing assemblies (36 panels in each end cell for a total of 72).

Figure 3-159. Helium-appendage pumping installation in the end cells. There are four appendage pairs (8 pumps), two pair in each end cell.

Figure 3-160. Details of the helium-argon cryotrapping panel.

The LHe supply and return circuits for the entire cryopumping system, as well as for the LN₂ supply and return circuits, will operate by natural convection in the manner of MFTF-B.³⁷ In addition to the LHe supply and return circuits to the cryopanel, there will be a warm helium gas system for purging LHe from the panels during degassing.

The cryopumps for the neutral beam injectors will operate essentially the same as the end cell pumps. However, they will be configured differently to conform with neutral-beam injection design constraints (see Section 3.2).

The operational characteristics of the end cell continuous cryopump system can be analyzed as follows:

Q = reactor throughput $T_2 + D_2$ in Torr-liters/s.

P_a = average allowable reactor pressure in Torr.

$S = Q/P_a = sA$ - required pumping speed in liters/s, where s = the specific pumping speed in liters/s-cm² and A = pumping area required to yield s - cm².

γ = total installed pumping area in cm².

$A(\gamma - 1)$ = area (cm²) of pump available for degassing at any given time.

$\Gamma/\Gamma - 1$ = ratio of installed pump area to available degassing area.

T_c = total cycle time of an $A(\gamma - 1)$ - pump area (pumping time + degassing time) in seconds.

T_d = degassing time of an $A(\gamma - 1)$ in pump-area in seconds.

Because the gas flow into the reactor Q must equal the gas flow out,

$$Q = Q_{\text{out}} \text{ or } sAP_a = S_d P_d ,$$

where S_d is the degassing pump speed (liters/s) and P_d is the degassing pump pressure (Torr). Further, the amount of gas degassed during the time t_d must equal the amount of gas pumped during the time $(t_c - t_d)$ for a given $A(\gamma - 1)$ pump area and

$$S_d P_d t_d = sA(\gamma - 1) P_a (t_c - t_d), \text{ or}$$

$$\gamma = t_c / (t_c - t_d) .$$

Summing up all the gas in the panels at any given time (the pumping panels inventory) as a function of yields,

$$I_p = sAP_a/2 (t_c - t_d) \text{ or } I_p = Q/2 (t_c) .$$

Finally, since the inventory in the collector pumps for any $a(\gamma - 1)$ degassing unit would equal the gas collected on that unit during its pumping time:

$$I_c = Q(t_c - t_d)/(\gamma/\gamma - 1) - 1 = Qt_d .$$

Operation of the LLNL prototype, regenerable, continuous cryopump, shows that 10 minutes is a reasonable time for degassing a cryopump and returning it to service. Thus, if t_d is set at 600 s, the inventory of gas in the pumping system can be plotted as a function of the excess capacity $(\gamma - 1)$, where

$$I_{\text{tot}} + I_p + I_c = Qt_d (3/2\gamma - 1)/(\gamma - 1) .$$

Figure 3-161 shows a plot of tritium inventory against excess capacity for the TDF end-cell pumping system. The 20% excess capacity set for TDF gives an end cell inventory of 40.3 g for tritium. Because the tritium source originates in the central cell neutral beams and the source's gas efficiency is 30%, tritium inventory in the neutral beam cryopumps is twice that in

Figure 3-161. Plot of tritium inventory vs excess capacity for the TDF end cell pumping system.

the end cells. Thus, the total tritium inventory in the TDF vacuum envelope is $3 \times 40.3 = 121$ g.

The cryogen requirements for the end cells were estimated as follows:

- Liquid-helium-cooled heat load due thermal radiation:
 $A = 1152 \text{ m}^2$ of panels + 40% for auxiliary systems = 1612 m^2 .
 Effective emissivity between LN_2 cooled shield and LHe-cooled surfaces is ~ 0.4 , allowing for long-term degradation.

$$Q_R = A\sigma E (T_{\text{LN}_2}^4 - T_{\text{LHe}}^4)$$

$$\begin{aligned} Q_R &= (1612 \times 10^4) / (5.67 \times 10^{-12}) (0.4) (x4/80 - x4/4.5) \\ &= 1.5 \times 10^3 \text{ W} \\ &= 1.5 \text{ kW.} \end{aligned}$$

- Neutron heating of LHe-cooled structures:
 Volume of aluminum - $3.75 \times 10^6 \text{ cm}^3$. Neutron heating - $0.4 \times 10^{-3} \times 3.75 \times 10^6 = 1.5 \text{ kW}$.
- Liquid helium loss due to degassing purge gas:
 Estimated at $\sim 1.5 \text{ kW}$. $Q = Q_R + Q_N + Q_d = 4.5 \text{ kW} = 6500$ liters/hr.

Estimating LN_2 consumption, using a thermal radiation emissivity of 0.7 and the same specific neutron heating but no degassing losses, gives

$$Q_{\text{LN}_2} = 585 \text{ kW} - 11,700 \text{ liters/hr.}$$

The external vacuum pumping system will be used to evacuate the vacuum chamber and to transfer the gas from the collector pumps to the fuel cleanup system. Designed for tritium service, it will have an oil-free scroll-type pump, suitable manifolding, and valving to both "rough pump" the vessel and transfer the collector pump gas.

The scroll pump will have a capacity of 560 liters/s, with a base or blank-off pressure of less than 10^{-2} Torr. Assuming a vacuum chamber volume

of 6×10^6 liters, the pumpdown would be accomplished as follows: From atmosphere to 0.5 Torr, the scroll pump would take approximately 24 hr.

$$t = V/s(f) \text{ in seconds ,}$$

where V = is the vessel volume, s is the displacement speed of the pump, and f is the pumpdown factor from 760 to 0.5 Torr.

$$t = 8.1 (6 \times 10^6 / 580) = 86,786 \text{ s} = 24 \text{ hr.}$$

From 0.5 to 10^{-4} Torr, one or several helium cryosorption pumps could be made available.

$$t = V/s[\ln(0.5/10^{-4})] \text{ ,}$$

where $s = 10^5$ liters/s (the air speed of one helium cryosorption pump) and $V = 2 \times 10^6$ liters.

$$t = (6 \times 10^6 / 10^5) \ln 5000 = 500 \text{ s} = 0.14 \text{ hr.}$$

Thus, the pumpdown time would be about 25 hr.

Key Technical Issues. Two key technical issues require further investigation:

- Panel leakage during degassing.
- Degassing characteristics of argon adsorbent loaded with helium and D-T.

The leakage of the LLNL prototype panel appears to be on the order of 25 to 30% of the gas pumped. No attempt was made to optimize this test panel for leakage; the point was to determine operational characteristics and concept feasibility, both of which appear to be acceptable.

An acceptable leakage level can be set by noting that the allowable total pressure in the end cells (from physics) is 5×10^{-6} Torr and the design pressure is 2.77×10^{-6} Torr (7.8×10^{-7} T_2 , 1.7×10^{-6} D_2 , and 2.9×10^{-7} He). There will be no leakage during degassing of the helium pumps, but there will be D_2 and T_2 leakage during degassing of the six

panel units in the end cell D-T pumping system and in the NBI pumping system. Because we can increase the total pressure by $(5 \times 10^{-6} - 2.77 \times 10^{-6})$ or 2.23×10^{-6} to 5×10^{-6} , the sum of the partial pressure of T_2 and D_2 can be:

$$P_{T_2} + P_{D_2} = P_a - P_{He} = (5 \times 10^{-6} - 2.9 \times 10^{-7})$$

or

$$P_{T_2} + P_{D_2} = 4.7 \times 10^{-6} \text{ Torr.}$$

From this information, we can determine worst-case degassing leakage for the TDF end cell pumping system.

The total D-T gas flow to the end-cell pumps includes plasma fan flow, $Q_{D_{2P}}$ and $Q_{T_{2P}}$, plus the panel-degass leakage, $Q_{D_{2L}}$ and $Q_{T_{2L}}$.

$$Q_{D_2} + Q_{T_2} = Q_{D_{2P}} + Q_{D_{2L}} + Q_{T_{2P}} + Q_{T_{2L}},$$

and the total D-T pressure

$$P_{DT} = P_{D_2} + P_{T_2} = \frac{Q_{D_2}}{S_{D_2}} + \frac{Q_{T_2}}{S_{T_2}},$$

where S_{D_2} and S_{T_2} are total pumping speeds for deuterium and tritium, respectively. From Table 3-66, we know that

$$\frac{Q_{D_2}}{Q_{T_2}} = \frac{124}{47.6} = 2.6$$

and

$$\frac{S_{D_2}}{S_{T_2}} = 1.2,$$

and, from above,

$$P_{D_2} + P_{T_2} = 4.7 \times 10^{-6} \text{ Torr.}$$

Solving for Q_{D_2} and Q_{T_2} , we get

$$Q_{D_2} = 235.4 \text{ Torr-liters/s}$$

and

$$Q_{T_2} = 90.53 \text{ Torr-liters/s.}$$

The D_2 accumulated (W Torr-liters) on two six-panel pumping-degassing units:

$$W = \frac{2Q_{D_2}}{10} (T_c - T_d) + \left(\frac{2Q_{D_2} t_t}{10} \right) 5 .$$

The first term is the D_2 gas accumulated on two six-panel units in one pumping cycle because of the flow from the plasma fans. The second term is D_2 gas accumulated on the same two units as a result of degas leakage from the five pair of six-panel units that degas during the $(T_c - T_d)$ pumping time. The time to transfer the gas from the panels to the collector pump (during which time leakage occurs) is (t_t) , which was determined on the LLNL prototype pump. Thus,

$$W = 2 \left[\frac{124 (3600 - 600)}{10} \right] + \left[\frac{(2) (111.4) (30)}{10} \right] 5$$

and

$$W = 7.77 \times 10^4 \text{ Torr-liters } D_2 .$$

The leakage per pair of six-panel units, then, is

$$L = \frac{2}{10} Q_{D_2} t = \frac{2}{10} (111.4) (30) = 668.4 \text{ Torr-liters} .$$

The percent leakage to maintain the peak pressure at less than 5×10^{-6} Torr is

$$\frac{L}{W} = \frac{668.4}{77700} \approx 1\% .$$

While it may not be possible to achieve such a low leakage rate for a continuous regenerable pumping system, it can be closely approached by minimizing the leakage conductance. This can be done by close-fitting extruded louvers and maximizing the collector pumping speed. Further the

probability of the twelve 4-m x 2-m panels, in a pair of six-panel degassing units, transferring their gas load to the collector pumps simultaneously is very low. Considering this, the time-averaged peak pressure due to degas leakage would be much lower, probably 5 to 10%.

The second technical issue--the degassing characteristic of an argon-adsorbent helium pump--is simply a matter of gaining the necessary experience.

3.10. PARTICLE DUMPS

3.10.1. Overview

The particle dumps must absorb about 100 MW of power, which is generated by the ion sources. This power is absorbed by the apertures, the ion or neutral dumps, or the plasma end dumps. The beamline section of this report describes the actual location and configuration of the ion dumps and the apertures. This section discusses the basic particle dump design, including the neutral and plasma end dumps.

The neutral dumps have a tested configuration as a baseline design. It is shown by Fig. 3-162. A plate of Amzirc (OFHC copper, copper with 0.2% zirconium) has machined fins and a backing plate brazed onto the same side. This provides a channel for the water coolant. Nucleate boiling takes place between the fins with the coolant continuously mixing water at the fin tip. This process allows very high heat transfer. The basic design has been tested to 2 kW/cm^2 over a panel area $20 \times 20 \text{ cm}$. The design heat flux for TDF is 1 kW/cm^2 . The low flux is required to maintain a life of about 0.5 full-power years. The front surface is thicker than that tested, because material is removed by sputtering. Figure 3-163 shows panels of this design assembled onto a frame. This subassembly is inserted into the central cell.

The plasma end dumps use a much simpler and cheaper technology than that of the neutral dumps. Two designs are proposed. For heat flux below 100 W/cm^2 , the panel material is stainless steel. For heat flux above 100 to 300 W/cm^2 , the material required is TZM (made of titanium, zirconium, and molybdenum). As Fig. 3-164 shows, the power density at the center of the plasma fan is 350 W/cm^2 at a location 28 m from the machine's center. This power density falls off quickly further from the center. As Fig. 3-165 shows, the panel is constructed of a corrugated sheet welded onto a flat sheet. This forms several channels for the coolant. The surface facing the plasma has enough thickness to allow for sputtering.

Figure 3-162. Baseline panel design.

Figure 3-163. Dump panel assembly.

Figure 3-164. Maximum heat flux on plasma end dump surface at various dump locations.

Sec A-A

TRANVERSE
SUPPORTS
TYP 3 PL

DIMENSIONS IN INCHES

Figure 3-165. Basic end dump panel.

3.10.2. Requirements

The particle dumps must absorb the power generated by the ion sources either as an aperture, ion or neutral dump surface, or at the plasma end dump. The neutral dumps must absorb about 30 MW of deuterium or tritium particles with an average energy of 60 keV. We chose the angle of incidence of the incoming particles with respect to the pump surface to limit the power flux to 1 kW/cm^2 . This limit is due to the large amount of sputtering expected, which requires a thick surface on the dumps with a corresponding temperature increase at the front surface. The life of 0.5 full-power years is a requirement for both fatigue and sputtering life. The dump design should not require development, but should be within the state of the art for high heat flux absorption. We considered coolant requirements for minimizing the total system costs.

3.10.3. Neutral Beam Dump Design

Design. Our design is based on a study by McDonnell Douglas under contract to the Lawrence Berkeley Laboratory (LBL).³⁸ Several materials for a front surface were considered in this study: OFHC copper, Amzirc, tungsten, and molybdenum. Amzirc was chosen due to its higher conductivity and strength than OFHC copper at the expected operating temperature. A $20 \times 20 \text{ cm}$ panel was tested on a beamline at LBL and performed well for at least 600 shots at 2 kW/cm^2 . For IDF, the requirement for a long life has greatly affected thermal requirements. A General Dynamics study recommended that the curve (Fig. 3-166) of sputtering rate angle of incidence be used for life calculation. As shown, the sputtering enhancement increases faster than the decrease in incident power with an increase in angle of incidence. This is why, for a given input power density, the front surface temperature continues to increase with the angle of incidence.

The life of a given thickness of material can be expressed by:

$$L = \frac{hE_p K}{3600 P \cos \alpha Z S(\alpha)} \quad , \quad (3-85)$$

Figure 3-166. Sputtering rate enhancement vs angle of incidence from normal.

where

- L = life in hours,
- h = thickness allowed to be sputtered,
- E = average energy of particles,
- ρ = density,
- K = constant,
- P = power density,
- α = angle of incidence from normal,
- Z = atomic weight,
- S(α) = sputtering rate at angle α .

The front surface temperature of a given thickness of material can be expressed as

$$T_{F.S.} = T_w + \frac{\ell q}{kA}, \quad (3-86)$$

where

- $T_{F.S.}$ = front surface temperature,
- T_w = temperature of the surface next to the coolant,
- q/A = heat flux/unit area,
- ℓ = material thickness,
- k = thermal conductivity.

The power density on the neutral dump surface depends on the source characteristics, the distance from the source grid to the dump, the geometry of the four sources in the beamlines, and the absorption characteristics of the plasma. Figure 3-167 shows the calculated power density on the dump surface when one source is on and the plasma is at full density; Fig. 3-168 shows the power density when four are on at the same time. Note that the region of highest power density is a small area, within which the calculated power density on the angled dump surface is about 1.2 kW/cm^2 . The front surface temperature of the dump is about 875°C . At this temperature, the strength of the Amzirc drops and the induced stress exceeds the elastic limit of the material. To reduce this temperature, the front surface must be made

Figure 3-167. Calculated power density normal to the beam after passing through the plasma.

Figure 3-168. Calculated power density normal to the beam for four sources on the dump.

thinner than necessary for the 0.5 full-power-year requirement. Several considerations make this a reasonable deviation. The view factor from one panel to the other is about 60%. This means that most of the sputtered atoms are redeposited onto the opposite panel. In addition, the panels can be inspected at the end of each experimental phase. If the small region appears to be losing material at an unacceptable rate, the panel can be moved or exchanged. We expect the panels to require replacement only after three calendar years of operation.

The life of the panels is based on steady-state sputtering considerations. General Dynamics made a study to ensure that the panels could handle the high heat flux during startup, when the plasma density is low. They assumed the density buildup of the plasma to be linear, increasing from 0 to a maximum in 0.5 s. The four sources were turned on 0.125 s apart, as illustrated in Fig. 3-169. As the plasma builds up, the power density on the surface of the neutral dump goes down, but as each source comes on there is an overlap and the power density goes up. The results of this study are discussed in Section 3.10.4, but to summarize, the maximum front surface temperature reaches about 330 °C for the area where two sources overlap. This is below that expected for the steady-state case.

McDonnell Douglas conducted a thermal stress analysis on the panel's design using two cases. The first was for a front surface thickness of 1.8 cm of Amzirc, and the second was for a thickness of 1.0 cm. They found that, because of the high surface temperature, the stress near the front surface exceeded the yield strength of the Amzirc. In addition, there were some regions near the panel's back side that had high stresses induced by the particular configuration used in the analysis. The study thus indicated a redesign of this region. It also predicted a very short panel fatigue life; however, thick copper plates have been used as beam-dump surfaces in several operating beamlines with little evidence of fatigue cracks or other types of failure. Since the plate is very thick at the beginning of the experiments, its response is a thick-plate response, and the high strain should not cause failure. As the plate sputters, the thickness between the front surface and the cooling channels is reduced and the temperature of the front surface goes down. We know that the panel can absorb 2 kW/cm^2 for at least 600 shots with no evidence of degradation.

Figure 3-169. TDF source startup sequence for central cell neutral-beam injectors.

The power density on the plasma end dump is:

$$P_{\text{end}} = \frac{P_{\text{cc}}}{K} \frac{B_{\text{end}}}{B_{\text{cc}}} \quad (3-87)$$

where:

- P_{end} = power density on the end dump,
- P_{cc} = power density in the central cell on the same flux line,
- B_{end} = magnetic field strength at the end,
- B_{cc} = magnetic field strength on same flux line in the central cell,
- K = constant dependent on beta.

At 28 m from the machine's center, the calculated power density at the center of the plasma end dump is 350 W/cm^2 . The size of the fan footprint is about $2 \times 17 \text{ m}$ after passing through a halo scrapper. Because the power density on the plasma end dump decreases rapidly away from the center of the fan, we have designed two panels of different materials. Those we considered were: Al6061, Amzirc, SS304, and TZM. The properties we considered were sputtering rate, resistance to coolant corrosion, ease of fabrication, strength vs temperature, and thermal characteristics. The sputtering thickness for one full-power year vs heat flux for these materials is shown in Fig. 3-170. The maximum front surface temperature vs heat flux for these materials is shown in Fig 3-171. Based on these results and the calculated strength required for the coolant pressure, we chose TZM for the baseline design for a heat flux over 100 W/cm^2 . Below this value, a SS304 panel satisfies the requirements. Since it costs about 40 times less than TZM, we would use it wherever we expect low heat flux.

We calculated the water flow rate and pressure required using the Dittus-Boelter Nusselt number relation:

$$Nu = 0.023 (Re)^{0.8} (Pr)^{0.4} \quad (3-88)$$

where Re is the Reynolds number and Pr is the Prandtl number. As the heat flux increases, the local pressure required to suppress boiling also increases, as shown in Fig. 3-172. We assume an inlet temperature of 40°C

Figure 3-170. Sputtering thickness for 1 full-power year for various materials at increasing heat flux.

Figure 171. Maximum temperatures of the beam dump front surface.

Figure 3-172. Water pressure requirements as a function of impinging heat flux.

and allow a 15° rise in water temperature. The minimum coolant flow for this case for both plasma dumps is 10,340 gpm with an inlet pressure of 250 psi.

We determined the amount of material required on the front surface (Fig. 3-170) to allow for sputtering assuming that 60% of the incident flux is 20-keV deuterium and 40% was a 50/50 mixture of 27-keV D-T particles.

We then did a structural analysis of a panel constructed of the candidate material using the ASME boiler Code 2 as a requirement. Membrane and bending stresses were computed at various locations for a given pressure with a joint efficiency of 0.8 for the welded regions and an idealized configuration (Fig. 3-173). For a heat flux greater than 100 W/cm^2 , only Amzirc and TZM met the requirements for strength. Because of the higher sputtering rate of Amzirc, we chose TZM for the higher heat flux areas.

The individual panels are assembled onto a frame and placed inside the vessel. The assembled configuration is shown in Fig. 3-174. We added small box-like protrusion to the vacuum vessel to allow removal and replacement of four to five panels while protecting personnel from high radiation. These panels will be manifolded together to allow such maintenance. Additional details on the plasma end pump design can be found in Section 3.10.9.

Tritium. One-half of the beamlines in the central cell inject tritium particles into the plasma. Some of these particles diffuse through the dump surfaces into the cooling water. We performed an analysis to determine the amount that may have to be recovered from the coolant.

We assumed that the analysis applies to both the neutral and ion dumps. Figure 3-175 shows the configuration used. Because diffusion rates are a function of temperature, we calculated the temperature profile through the thickness of the dump surface using the equation shown in Fig. 3-175.

Figure 3-176 shows the initial concentration of tritium particles, calculated by the University of California-Los Angeles (UCLA). This information was input to a computer code that calculated the amount of tritium expected to enter into the cooling water. It was calculated at 21 g/full-power year. This is a significant amount and illustrates that a tritium recovery system must be used for the cooling water.

ACTUAL COIL GEOMETRY

IDEALIZED RECTANGULAR SECTION
WITH THE SAME FLOW AREA

Figure 3-173. Panel coil geometry.

Figure 3-174. TDF end cell configuration.

Figure 3-175. Tritium diffusion model.

NEUTRAL BEAM DEPOSITION IN METAL SURFACE

Figure 3-176. Neutral-beam deposition in metal surface.

Key Technical Issues

Particle Dumps. Although the baseline configuration for the particle dumps has been tested, the thicker required surface required has not been used as an actively cooled surface. If it acts as a true thick plate, experience shows that the design will function satisfactorily. Yielding at the hot thin layers at the surface has not been a problem for the large number of flat, thick, copper beam dumps used to date in experiments at LLNL, LBL, the Doublet III Experiment, and Oak Ridge National Laboratory (ORNL). Cracks that developed on the thinner surface do not appear to grow into the thick plate. Copper plates have been melted on the front surface and have continued to be useful dumps. Before a decision is made on a TDF configuration, several particle dumps will have been tested on beamlines now planned for long-pulse operation. These are the Mirror Fusion Source Test Facility (MFTF) at LLNL, the Neutral Beam Engineering Test Facility at the LBL, and the test facility at ORNL. Present plans for MFTF call for testing an Amzirc panel with the baseline design with a front plate 1.0 cm thick rather than the 1.8 cm required for TDF. It may be possible to reduce the required thickness of the panel due to the view factors between panels. As the plate gets thinner, the front surface temperature goes down, and the design becomes more conservative from fatigue life considerations.

The data we used to calculate the thickness of the panel front surface is not taken from direct measurement. It is available for low energy particles only. Since the data indicates a rapid increase of sputtering enhancement with increasing angle of incidence, the accuracy of such calculations becomes very important. Data for high-energy-deuterium particle sputtering as a function of incident angle is vitally needed, because the factor of a 9 times sputtering enhancement at 70° incident drives the panel design.

Although the stress analysis indicates very high stresses both on the front surface of the panel and close to the water channels, neither may be as serious as it appears. Based on what we have already discussed, the front surface high stress may not be a problem, and with a slightly different design we can significantly reduce the stresses near the channels.

If sputtering and stresses are used as an absolute determination of panel selection, either another material must be used or the Amzirc surface must be laminated with material such as tungsten. We considered several means of laminating, including diffusion welding, explosion bonding, and plasma spraying. The usual question of bond survivability make these laminated designs more risky than the baseline panel. Laminated surfaces are presently being developed as part of the High Heat Flux Component Development Program (HHFCDP).

One technical issue for the plasma end dump is the cost of moving the dump in relation to the machine's center. Experiments on the two baseline designs have not been carried out to determine their heat-transfer capability. Another is the amount of tritium that diffuses through the material selected, which again depends on the distance from the machine center. Although a panel made of TZM appears to be relatively easy to manufacture, we have not made one.

3.10.4. Neutral-Beam Dump--Startup Analysis

Problem. The startup method chosen for the neutral beam injectors and the plasma growth profile will greatly affect the beam dump's heat load. During startup, the surface heat flux on the dump may be much larger than during the normal dump operating mode. This is because it takes time for the plasma trapping fraction f_+ , to increase to the steady-state value. The baseline heat-transfer panels used as the dump surface are designed to operate under steady-state conditions. Thus, the temperature and thermal gradient experienced by the panel during the startup phase must be evaluated for determining if that is a worse case.

Assumptions. Figure 3-177 shows the Amzirc heat-transfer panel under consideration. The baseline panel is commercially available from MDAC. Small internal channels with turbulent water flow promote forced convection and nucleate-boiling heat transfer. For TDF, the original McDonnell Douglas design has been modified by increasing the front surface thickness to allow for a one-half full-power year of sputtering. At the beginning of life, the front plate thickness is 1.8 cm, while the end-life thickness is 0.2 cm a loss of 1.6 cm of material.

Figure 3-177. Internally-finned, water-cooled dump.

Each central cell neutral beam injector has four ion sources with beams that overlap during start-up to distribute the deposited energy spatially on the dump surface, as shown in Fig. 3-178. The TDF rectangular standard source is $12 \times 40 \text{ cm}^2$, has a divergence of 0.5 deg in both directions, has a current of 60 to 70 A, and is located 9.5 m from the front surface of the dump.

The neutral-beam source turn-on sequence is shown on Fig. 3-179. The beams are turned on at 0.125 s intervals in the following order: Source 1, Source 4, Source 3, and Source 2. In this manner, the heat flux overlap at the worst location is delayed.

Figure 3-180 shows the assumed f_+ vs time. At time zero, no plasma is present; therefore, f_+ is zero. At this time the full-beam intensity is incident on the dump. At 0.5 s the plasma concentration reaches its steady-state mode; an f_+ of 0.79 is present continually from this point on, and only 21% of the beam intensity strikes the dump.

Discussion. We simulated four startup cases: Two begin-life (front plate thickness of 1.8 cm) and two end-life (front plate thickness of 0.2 cm) scenarios. We had to check two worst-case locations, (1) the maximum heat flux location of source 1, because this transient flux occurs over the longest period of time, and (2) the maximum heat-flux location caused by the intersection of Sources 3 and 4, because this is the highest heat flux present over the longest period of time.

The heat flow to the dump at a given time is calculated as follows:

$$DHF(x,t) = [\sum ISHF(x,t)] [1 - PTF(t)] \sin \theta, \quad (3-89)$$

where:

- $DHF(x,t)$ = the resultant dump heat flux at point x at time t (kW/cm^2)
- $\sum ISHF(x,t)$ = the normal heat flux from source at time t at point x (kW/cm^2)
- $PTF(t)$ = the plasma trapping f_+ ,
- θ = angle of dump inclination i.e., angle between beam and dump surface, (20 deg for TDF).

Figure 3-178. The central cell neutral-beam energy overlap on a normal dump surface with no plasma trapping.

Figure 3-179. Source startup sequence for the central cell neutral-beam injectors.

Figure 3-180. TDF plasma trapping fraction increase at startup.

Figure 3-181 shows transient resultant heat fluxes on the dump surface for the two locations studied.

The transient temperature profile at the front plate of the dump was determined with a one-dimensional model using the Convair thermal-analyzer finite-difference computer program.³⁹ The model nodes are shown in Fig. 3-182.

The front plate of the panel assembly was divided into 36 nodes with the heat flux from the beam injectors impinging on the surface of node 1. We assumed the plate to be a semi-infinite solid for the begin-life cases. This is because the front plate is thick and the transient condition is short (0.5 s).

The plate was too thin in the end-life study to be considered a semi-infinite solid; therefore, we simulated cooling at the back surface. The McAdams forced-boiling correlation,⁴⁰ conservative in our interest range, was suggested by the LLNL to predict the heat flux to the water:

$$\frac{Q}{A} = 2.256 \times 10^{-7} (T_w - T_{SAT})^{3.86}, \quad (3-90)$$

where

Q = the heat flux (kW/cm^2),

T_w = the wall temperature ($^{\circ}\text{C}$),

T_{SAT} = the saturation temperature at local pressure ($^{\circ}\text{C}$).

We took the local pressure to be 150 psi. All nodes were initialized to 60 $^{\circ}\text{F}$ (15.6 $^{\circ}\text{C}$).

Results. Resulting profiles of the front surface of the beam dump are shown in Figs. 3-183 through 3-186. The maximum temperature the front surface experiences, is 270 $^{\circ}\text{C}$ for begin-life (Fig. 3-187) and 330 $^{\circ}\text{C}$ for the end-life (Fig. 3-188) cases. These temperatures are well below the 1083 $^{\circ}\text{C}$ melting temperature of copper. The maximum temperature, at the 0.2-cm front surface delta, is 15 $^{\circ}\text{C}$ for the begin-life and 90 $^{\circ}\text{C}$ for the end-life case. This front surface must maintain structural integrity.

Figure 3-181. The heat flux scenario for the reactor startup phase study of the neutral dump at various maximum flux locations. Shown are (a) location of source 1 and (b) locations of sources 3 and 4.

Figure 3-182. The thermal analyzer front plate nodalization used to predict thermal gradients in the front plate of the neutral-beam dump during system startup.

NODE NO. DISTANCE

100	0.000
1	.025
2	.075
3	.125
4	.175
5	.225
6	.275
7	.325
8	.375
9	.425
10	.475
11	.525
12	.575
13	.625
14	.675
15	.725
16	.775
17	.825
18	.875
19	.925
20	.975
21	1.025
22	1.075
23	1.125
24	1.175
25	1.225
26	1.275
27	1.325
28	1.375
29	1.425
30	1.475
31	1.525
32	1.575
33	1.625
34	1.675
35	1.725
36	1.775
300	1.800

Figure 3-183. The begin-life temperature profile of the beam dump's front surface during reactor startup at the peak heat flux location of source 1.

WIRE NO.	DISTANCE
100	0.000
1	.025
2	.075
3	.125
4	.175
5	.225
6	.275
7	.325
8	.375
9	.425
10	.475
11	.525
12	.575
13	.625
14	.675
15	.725
16	.775
17	.825
18	.875
19	.925
20	.975
21	1.025
22	1.075
23	1.125
24	1.175
25	1.225
26	1.275
27	1.325
28	1.375
29	1.425
30	1.475
31	1.525
32	1.575
33	1.625
34	1.675
35	1.725
36	1.775
200	1.800

Figure 3-184. The begin-life temperature profile of the beam dump's front surface during reactor startup at the peak heat flux at the intersection of sources 3 and 4.

NODE NO. DISTANCE

100	0.000
1	.003
2	.006
3	.014
4	.019
5	.025
6	.031
7	.036
8	.042
9	.047
10	.053
11	.058
12	.064
13	.069
14	.075
15	.081
16	.086
17	.092
18	.097
19	.103
20	.108
21	.114
22	.119
23	.125
24	.131
25	.136
26	.142
27	.147
28	.153
29	.159
30	.164
31	.169
32	.175
33	.181
34	.186
35	.192
36	.197
200	.200

Figure 3-185. The end-life temperature profile of the beam dump's front surface during reactor startup at the peak heat flux location of source 1.

NODE NO. DISTANCE

100	0.000
1	.003
2	.008
3	.014
4	.019
5	.025
6	.031
7	.036
8	.042
9	.047
10	.053
11	.058
12	.064
13	.069
14	.075
15	.081
16	.086
17	.092
18	.097
19	.103
20	.108
21	.114
22	.119
23	.125
24	.131
25	.136
26	.142
27	.147
28	.153
29	.158
30	.164
31	.169
32	.175
33	.181
34	.186
35	.192
36	.197
200	.200

Figure 3-186. The end-life temperature profile of the beam dump's front surface during reactor startup at the peak heat flux at the intersection of sources 3 and 4.

Figure 3-187. The begin-life maximum temperature of the beam dump's front surface during reactor startup.

Figure 3-188. The end-life maximum temperature of the beam dump's front surface during reactor startup.

Conclusions. The temperatures of the panel during reactor startup are well below melt temperatures even though the beam intensity is larger than steady-state during the 0.5 s. Stress due to these temperature gradients must be checked to assure the panel's integrity during startup.

3.10.5. Central Cell Beam Dumps--Trade Study and Off-Normal Conditions

Overview. We conducted a trade study comparing three orientations of the central-cell beam dump to the incident beams and evaluating heat load and thermal performance. The orientations were: flat-vertical (15 to 25 deg angles between surface and incident beams), contoured-vertical (with a constant 15 deg between surface and incident beams), and flat-horizontal (with ~60 deg between surface and incident beams).

The peak heat load on a flat-horizontal beam dump (normal to the beamline) is greater than 1.8 kW/cm^2 . The pumping power required is more than an order of magnitude higher than that required for the other two configurations. For the contoured-vertical, a single-phase water flow coolant system can be used to maintain the beam dump surface below its upper temperature limit.

We also considered effects of two off-normal conditions a beam offset from the center of the plasma and a loss of plasma.

Discussion. Beamline attenuation by the circular plasma in the central cell results in heat flux peaks on the dump near the edges of the plasma. We calculated heat flux distributions the three orientations, as shown in Fig. 3-189. Both vertically dumps resulted in heat loads less than the design criterion of 1 kW/cm^2 .

Our analysis assumed that the dump was a panel of AMZIRC, and that water flow was single phase. The resulting pumping power is shown as a function of system pressure in Fig. 3-190.

For the central cell beam heat flux calculations, we assumed that the beam centerlines coincided with the centerline of the plasma. This best matches the attenuation capability of the beam with the plasma heat distribution and results in the minimum possible heat flux to the dump. To determine the sensitivity of peak beam-dump heat flux to a misalignment

Figure 3-189. Peak heat fluxes on central cell beam dumps.

Figure 3-190. Pumping power for TDF central cell beam dumps.

between the centerlines of the beam and the plasma, we calculated the heat flux distribution for an offset of 1.0 cm. The resulting distribution is shown in Fig. 3-191. The 1.0 cm offset corresponds to only a 0.1 deg deviation of the beam angle and results in the 30% increase in peak heat flux to the dump indicated in the figure. This indicates a likely requirement for actively controlling the beam direction.

Another off-normal condition for the beam dump occurs when the plasma disappears and the beam is allowed to impinge on the dump surface without attenuation. In this case the surface temperature of the dump rises very rapidly. Resulting surface temperature rise is shown in Fig. 3-192 for several values of flux. Times needed to increase the surface temperature by 500 °C range from 0.02 to 0.10 s for the three beam dump orientations, which indicates the need for a very rapid response for beam shutdown.

3.10.6. Beam Dumps for the Transition Region

Overview. There are three ion beam dumps in each of the anchor cell regions, the anchor pumping beam, anchor sloshing beam and the transition pumping beam. Their locations are shown in Fig. 3-193.

Table 3-67 summarizes heat loads, associated orientations, dump size, and life.

For each dump, the objective was to minimize surface heat loads consistent with geometry and maintenance considerations. The maximum allowed heat flux is 1.0 kW/cm^2 for both the anchor sloshing beam dump and the transition pumping beam dump, but the heat loads at normal incidence to the incoming beam exceeded that; therefore, we angled the dumps. The surface thickness and the erosion rates are similar to those for the central cell beam dumps. Their life is approximately 0.4 full-power years. The maximum heat load is 260 W/cm^2 for the anchor pumping beam dump, and its life is significantly longer (~7 full-power years) than those of the other dumps because of the increased surface thickness allowed and the decreased erosion rate. The results shown in the table are for a maximum surface thickness of 1.0 cm at a heating rate of 1.0 kW/cm^2 . This is based on fatigue considerations for 5000 cycles. Erosion lives are based on erosion rates at 1.0 kW/cm^2 . (This is given as a function of incident angle in Section 3.10.1.)

Figure 3-191. Effect of misalignment between beam and plasma.

Figure 3-192. Effect of plasma loss on the beam dump surface.

Figure 3-193. Transition region beam dump locations.

Table 3-67. Summary of beam dump heat flux, size, and life.

	Anchor pumping	Anchor sloshing	Transition pumping
Peak heat flux $\sim W/cm^2$ (Angle)	260 (75°/53°)	1000 (60°/54°)	1000 (30°/60°)
Surface thickness (cm)	4.0	1.0	1.0
Erosion per FPY	0.49	1.6	1.8
Life ^a (FPY)	~ 7	0.4	4
Dump size (cm)	40 x 120	30 x 60	130 x 50

^a End of life thickness--0.3 cm

^b 5000 cycles

Neutral beam parameters we used to obtain the heat loads are provided in Section 3.3.

Discussion

Anchor Pumping Beam Dump. The anchor pumping beam dump lies along the edge of the plasma fan between 8.45 and 9.6 m. This location and the dump's resulting surface heat load distribution are shown in Fig. 3-194. Seventy percent of the 116 kW of beam power delivered to the plasma is transmitted through to the dump. The edges of the beam are scraped off as it leaves the beam line. The result is essentially zero heat flux at a point greater than 40 cm in diameter and normal to the beam at the dump. The beam is incident at 15 deg and 37 deg in the two sections of the dump shown. This change of angle results in the step change in heat load distribution.

Anchor Sloshing Beam Dump. This dump is attached to the wall opposite the sloshing beam. In the beamline these are redundant beams 25-cm apart. (This is accommodated by increasing the azimuthal dimension of the beam dump.)

The beam delivers a peak heat flux of 1.24 kW/cm^2 normal to the beam at the dump. To reduce this heat load, the dump is oriented at 54 deg off-normal to the beam. Figure 3-195 shows the resulting heat flux distribution. The required dump dimensions are about 30 cm x 60 cm and the predicted erosion life is about the same as that for the central-cell dumps 0.4 full-power years.

Transition Pumping Beam Dump. This dump is adjacent to the choke coil. This dump takes a total of 2.3 MW delivered by six overlapping beams. The heat flux distribution normal to the beam and the resulting peak heat load are shown in Fig. 3-196. The peak heat flux of 1.2 kW/cm^2 is reduced to the maximum of 1.0 kW/cm^2 by orienting it as shown. However, the dump and its shield plug must clear the coil support structure radially and are, therefore, a maximum of 80 cm in axial length. This requires that a portion of the dump be oriented in the radial direction.

The transition beam dump surface area is 130 cm x 50 cm. Based on erosion, its life is 0.4 full-power years, which is similar to that for the sloshing beam dump.

Figure 3-794. Surface heat load on the anchor pumping beam dump.

Figure 3-195. Surface heat load on the anchor sloshing beam dump.

Figure 3-196. Surface heat load on the transition pumping beam dump.

End Cell Cost Trade Study. We performed an end cell trade study to determine the sensitivity of end cell cost to end cell size. Because it occurred midway through the TDF effort, our study was based on the initial conical end cell configuration shown in Fig. 3-197. Four elements were considered: the vacuum vessel, shield, end dump, and building.

Results showed that the incremental cost of the device per meter added to the half length is approximately \$5 M. The vacuum vessel and building costs were the major ones; that of the end dump decreases with increasing end cell length. However, this component accounted for only about 1% of the total. Differential costs are shown in Fig. 3-198 and summarize the effect of increasing machine half-length to more than 27 m.

We calculated costs for each of the components for three end cell lengths. These corresponded to domes on the conical end cell at 27 m, 31 m, and 35 m from the half-length as shown in Fig. 3-199. Also shown is the vessel surface area as a function of length. The vertical lines at the three reference lengths represent the area of the end dome added to the areas for the cylindrical and conical portion of the shell. Calculated thicknesses for the vacuum vessel are based on stability requirements for a cylindrical shell subjected to a uniform external pressure. We used the maximum end cell radius for each of the reference end cell lengths. Resulting thicknesses are shown as a function of external pressure in Fig. 3-200. Relative costs of the vacuum vessel are based on scaling structural material volumes--the product of surface area (from Fig. 3-199) and shell thickness (Fig. 3-200) at 0.10 MPa (one atmosphere). Resulting volumes are conservative, but they do provide a basis for relative cost. The reference cost was the defined vessel cost at the time of the trade study: \$12 M at a length of 31 m. Relative material volumes were 0.48 and 1.75 at 27 and 35 m machine half-lengths, respectively, resulting in costs of \$5.8 M and \$21 M.

Shield cost was based on the volume of ordinary concrete at \$480/m³. The differences in volume required were based on the vacuum vessel surface areas shown in Fig. 3-199 and a constant 60-cm thickness of concrete. We chose the thickness based on neutronics estimates performed early in the program. Resulting cost increments over the 27 m length are \$274 K and \$610 K for 31 m and 35 m machine half-lengths, respectively.

Figure 3-197. End cell plan layout used in cost trade study.

Figure 3-198. Summary of end cell cost study.

Figure 3-199. Vacuum vessel surface area.

Figure 3-200. End cell vacuum vessel thicknesses used for trade study relative volumes.

We determined the end dump cost based on \$246 K, the value we initially defined as necessary for a machine half-length of 31 m. It was assumed that pumping cost at this length was equal to the panel cost. For shorter half-lengths, the heat loads on the dump increase, as shown in Fig. 3-201. From this data we determined that heat loads for the 27 and 31 m lengths were proportionally larger than the 150 W/cm^2 heat load at 35 m. We calculated required pumping power for each machine half-length and assumed that costs would be proportional.

Table 3-68 lists end dump parameters and panel and pumping power costs. Although the panel size and cost is reduced with decreasing machine half-length, the total cost increases because of higher pumping power; therefore, it shows up as a negative increment. The final cost is on the order of that for the shield.

We also determined reactor building costs for the three lengths, including appropriate increases in machine diameter. They were \$47.7 M, \$60.4 M, and \$74.9 M for 27, 31, and 35 m lengths respectively. The differential cost, as indicated in Fig. 3-198, was the largest contributor to the overall cost.

3.10.7. First Wall of the Central Cell

Overview. The first wall regions of the central cell near the beam dumps (Fig. 3-202) are subjected to surface heat loads of approximately 200 W/cm^2 . They will require a cooled surface that should be separate from the shield so that replacement is relatively easily. The cooled first wall unit must surround the beam dumps and accommodate other components, such as those required for fueling. Access is made available when test modules and their shield plugs are removed.

Analysis of an array of cooling tubes showed that the first wall can be cooled with single-phase flow, either axial or circumferential. Stainless steel tubes with 1.2-mm thick walls will have a maximum wall temperature of 400°C . Copper AMZIRC is more promising than stainless as a first wall material because the wall thicknesses can be more easily increased, allowing for repair of possible local erosion.

Figure 3-201. End dump surface heat loads based on magnetic field strength.

Table 3-68. End dump parameters and costs.

Half length (m)	27	31	35
Dump size (m)	1.4 x 17	1.7 x 20.5	2.0 x 24
Dump area (m ²)	24	35	48
Panel cost (\$)	169,000	246,000	337,000
q_{AVE}/q_{PEAK} (W/cm ²)	130/322	80/215	60/150
Pumping power (kw)	850	290	110
Pumping cost (\$)	721,000	246,000	93,000
Total cost (\$)	890,000	492,000	430,000
Δ cost	---	(398,000)	(460,000)

Figure 3-202. First wall of the central cell.

Discussion. We made heat transfer calculations for water coolant flowing in axial and circumferential directions, using a first wall region 1.0 m in diameter and 1.0 m long.

We used a "one-region model" in which the physical properties were taken to be constant, allowing us to treat the whole coolant passage as one region. Our equations were as follows:

For conduction in the solid wall, we solved

$$T_{w,out} = T_{w,max} - q_s \cdot t_w / k_w , \quad (3-91)$$

where

$T_{w,max}$ = outside surface temperature

$T_{w,out}$ = temperature at inner wall,

q_s = heat flux,

t_w = wall thickness,

k_w = thermal conductivity.

For the heat balance in coolant, we used

$$\rho U C_p A (T_{out} - T_{in}) = q_s \cdot S , \quad (3-92)$$

where

ρ = coolant density,

U = coolant velocity,

A = flow area,

S = surface area.

Heat transfer from the wall to coolant was

$$q_s = h(T_{w,out} - T_{out})$$

where

T_{out} = coolant exit temperature,

h = film coefficient,

$$h = 0.023 Re^{0.8} Pr^{0.4} \cdot k/d_e \quad (3-93)$$

where

Re = Reynolds number,

Pr = Prandtl number,

k = thermal conductivity,

d_e = diameter.

Pressure drop is

$$\Delta p = (1.5 + \lambda L/d_e) \rho U^2/2, \quad (3-94)$$

where

L = Length of pipe,

$$\lambda = 0.3164 Re^{-1/4}.$$

Pumping power is $W_p = AU\Delta p$.

We solved Eqs. (3-92) and (3-93) simultaneously, giving the coolant outlet temperature and coolant velocity.

We assumed the first wall consisted of an array of tubes with 2.0 cm outer diameters. The wall temperature was limited so that either the heated surface temperature did not exceed the maximum allowable temperature or the cooled surface temperature did not exceed the coolant saturation temperature.

Table 3-69 shows the results for circumferential coolant flow; those for axial flow are similar.

Not only are maximum temperatures of the coolant and structure limited by thermal stress; so is the temperature difference in the structure. We used differences of 150° K for 316SS, 100° K for copper, and 70° K for aluminum. This limited the SS316 wall thickness to be less than 1.2 mm.

Table 3-69. First wall parameters for circumferential coolant flow. The heat load was $2.0 \text{ MW}\cdot\text{m}^{-2}$.

Material	AMZIRC	Aluminum	SS316	
a (mm)	12.0	12.0	17.6	15.0
t_w (mm)	4.0	4.0	1.2	2.5
k_w ($\text{W}\cdot\text{m}^{-1}\text{K}^{-1}$)	320	200	16	16
$P_{\text{out}}(\text{MPa})/T_{\text{sat}}(^{\circ}\text{C})$	1.0/180	1.0/180	1.0/180	1.0/180
$T_{w,\text{allowable}}(^{\circ}\text{C})$	320	200	400	400
$T_{w,\text{max}}(^{\circ}\text{C})$	205	200	330	400
$T_{w,\text{out}}(^{\circ}\text{C})$	180	160	180	87.5
$\Delta T_w(^{\circ}\text{C})$	25	40	150	312.5
$T_{\text{in}}/T_{\text{out}}(^{\circ}\text{C})$	60/95.2	60/88.5	60/78.6	60/64.2
$U(\text{m}\cdot\text{s}^{-1})$	3.90	4.82	3.43	20.7
Re	1.57×10^5	1.94×10^5	2.02×10^5	1.04×10^6
Pressure drop (kPa)	26.1	38.7	16.5	523
Pumping power (kW)	1.15	2.11	1.37	192

3.10.8. Blistering of the Beam Dumps

Overview. The available data on blistering under hydrogen-species ion bombardment suggest that the TDF beam dump will not blister. At most, a few blister layers less than $0.5\ \mu\text{m}$ thick will form and spall early in operation. Continued operation will not result in further generations as the metal reaches an equilibrium gas content. A single blister layer will be about $0.4\ \mu\text{m}$ in copper and $0.3\ \mu\text{m}$ in molybdenum or tungsten.

The data we surveyed does not include the projected conditions for the beam dump, and verification experiments will be required. The very high particle flux, energy distribution in the beam, and device operating cycle need to be simulated to evaluate the expected operating performance.

1. Beam Conditions. The beam will be a 50:50 D-T mixture at 80 keV. The average energy per particle at the beam dump is 61 keV, and the design heat load on the beam dump is $1\ \text{kW}/\text{cm}^2$. This is equivalent to a current particle density of $1.02 \times 10^{17}\ \text{cm}^2\cdot\text{s}$.
2. Beam Dump Material. Candidate materials are copper AMZIRC, molybdenum, and tungsten. Water cooling can limit the surface temperature of a copper alloy beam dump to $320\ ^\circ\text{C}$. The water inlet temperature is $\sim 50\ ^\circ\text{C}$.

Background Information. Draft review articles by Langley⁴¹ and Behrisch⁴² summarize the available blistering data. We have summarized the material.

Experimental bombardment of copper, molybdenum, and tungsten with H⁺ or D⁺ in a range of conditions has caused blistering. Most of the experiments have been at or below $20\ ^\circ\text{C}$, with few data for higher temperatures. The blistering results from relatively low hydrogen solubility, strong hydrogen-defect interactions, and relatively high hydrogen diffusion rates. As a consequence, the blistering is a strong function of bombardment temperature and has a modest ion flux dependence.

The approximate maximum temperature at which blisters form in hydrogen bombarded copper is $75\ ^\circ\text{C}$, while for molybdenum it is $20\ ^\circ\text{C}$. The critical fluence for blisters to form ranges from 1 to $10 \times 10^{18}\ \text{particles}/\text{cm}^2$. The thickness of blister lids that exfoliate at the critical fluence approximately equals to the mean projected range of the particle. For 80 keV

D⁺, this distance is 0.43 μm for copper, 0.28 μm for molybdenum and 0.30 μm for tungsten (see Table 3-70). The range is less for 80-keV H⁺, and probably lightly greater for 80 keV T⁺.

Blistering is repetitive under helium bombardment for some experimental conditions, but there is no evidence for repetitive blistering under hydrogen isotope bombardment.

Blistering is usually reduced or eliminated by the following conditions:

- Higher temperatures.
- Bombardment with distributed energies, rather than monoenergetic ions.
- Achieving high fluences ($>10^{19}$ ions/cm²).

All of these conditions prevail for the TDF beam dump so any blistering should be a transient phenomenon. It is imperative to note, however, that the projected service fluences of 1×10^{17} particles/cm².s for long-term, continuous service are totally unexplored conditions. The fluence for 1000-hour operation would be $\sim 4 \times 10^{23}$ particles/cm², about three orders of magnitude beyond the available experimental data.

3.10.9. Plasma End Dump

Overview. In the tandem mirror fusion reactor configuration ions leak out along the anchor cell magnetic field lines into the end cell regions. This plasma fan must be caught by an energy collector, such as a heat exchanger. The heat exchanger for TDF is the plasma end dump. The plasma is dumped onto its surface and the waste heat is carried away by a cooling medium. Although there are methods of using the heat (e.g. direct electrical conversion), they are not considered because of high capital cost.

The overall configuration and spatial orientation of the plasma end dump is shown in Fig 3-174. Each dump is located 28 m axially from the center of the machine and must accommodate maximum plasma heat flux of 350 W/cm² and a total power of 27.25 MW per end cell. The dimensions of the plasma footprint at the 28-m point are approximately 2 by 17 m. The dump lifetime requirement is ≥ 0.5 full-power years.

Design Summary. The dump is an array of heat-exchanger panels joined in series to common inlet and outlet headers. The total area of the panel array is larger than the plasma fan footprint by approximately 1/4 to 1/2 m on all sides.

Table 3-70. Projected range of deuterium ions in beam-dump materials.^a

Material	Range for given energy			
	(μm)			
	80 keV		40 keV	27 keV
	Deuterium	Hydrogen	Deuterium	Deuterium
Copper	0.43 ^b	0.36	0.23	0.17
Molybdenum	0.28 ^c	0.24	0.15	0.11
Tungsten	0.30	0.25	0.16	0.11

^aData is taken from Stopping Powers and Range of Ions in Matter, 3, edited by H. H. Andersen & J. F. Ziegler (Pergamon Press, 1977).

^bThe standard deviation is 0.13 μm; the full width at half-maximum of distribution is 0.30 μm.

^cThe standard deviation is 0.3 μm; the full width at half-maximum is 0.20 μm.

Each panel consists of a corrugated sheet of metal welded to a flat sheet forming several channels through which water flows. Each panel has one inlet and one outlet and a manifold at the top and bottom so that the water flows past the plasma impingement zone once. The basic configuration is shown in Fig. 3-165.

Initially, this dump was located about 30 m from the machine's center. The heat deposition and fan size there required a 28-panel array spanning the 24 m of the plasma footprint. In a later effort to reduce the size of the machine, we moved the dump inward to $Z = 28$ m. Here, the heat flux is higher and low conductivity materials, such as stainless steel, are unsuitable. Thus, we chose TZM, a molybdenum alloy. Since TZM panels are not available commercially, they were designed by General Dynamics based on an available panel of stainless steel.⁴³

After analyzing the design, we added braces to provide structural rigidity and prevent warping caused by the thermal stresses present during operation. With this reinforcement, the panel's design life is one full-power year before needing replacement. The dump assembly is located in the end cell on the rear wall. Since we did not want to remove the end dome to access them, we added a small box-like protrusion to the end wall to house the assembly. Sections of this house (visible in Fig. 3-173) are removable to provide access. In designing the dump for accessibility we also considered that the heat flux is higher in the center of the plasma fan than at the edges. This means that the center panels of the assembly could need replacement, while the end panels were still usable. To solve this problem, the array would be made in four or five sections, individually replaceable.

Several factors dictated the overall size. First, the panel must extend beyond the 2 m width of the plasma fan. Second, the channel cross sectional area and the overall flow rate are dictated by cooling requirements. Since the panel has one inlet and one outlet, the manifold requirements limit the number of channels per panel. Commercially available materials determine the width of the panel. Rolled sheet stock is a maximum of 36 in. wide. Thus, we chose a panel width of 34 in. with 16 channels per panel. The panel length of 107 inches is not a problem. The main inlet/outlet manifold for the panel bank assembly must carry the full cooling water volume flow. The four- or five-section array allows for as many inlet and outlet headers, each carrying 1/4 or 1/5 the total flow. This gives a more complex supply/return water system outside the machine.

The manifolds run parallel to the plasma fan on both ends of the panel assembly and are set slightly back from the plane of the dump panel. Structural frame support is mounted on the manifold piping between inlets and outlets. Brackets are mounted on the inside wall of the housing on which the panels are hung.

The inlet and outlet piping, as well as support brackets, bolt onto flanges on the manifold piping. This facilitates changing individual panels. All the piping used in the water cycle of these dumps is commercially available, so little manufacturing is required beyond the panels.

Installation is simple. Sections on the side of the house are removed, and the array is lowered into place by an overhead crane and hung on the brackets. The only other installation involves attaching the header pipes to the main water supply and return lines.

System Requirements. System requirements for the plasma end dump are summarized in Table 3-71.

Design Analysis

Thermodynamic Characteristics of End Dumps. It is advantageous to move the end dump toward the center of the reactor to reduce the size and thus the cost of the end-cell region. However, as the beam dump toward the machine center, the maximum heat flux on the dump increases, and some practical factors limit the magnitude the dump can handle. At some location, a simple heat transfer panel will not accommodate the heat flux, and a high-cost, high-heat transfer panel must be incorporated. The maximum heat flux occurs along the reactor center line, therefore, we calculate it at various distances along the axis.

The following conservation of flux equation is applicable:

$$B_{END} A_{END} = A_{CC} B_{CC} (1 - \beta)^{1/2} , \quad (3-95)$$

where B_{CC} = the central cell magnetic field with out plasma (T) ,

$1 - \beta$ = correction of the central cell magnetic field to account for reduction of field when the plasma is introduced (dimensionless).

Table 3-71. Plasma end-dump system requirements.

Parameter	Value
Total power (both ends) (MW)	54.5
Species (%)/Energy flux (keV)	See Table 3-75
	See Table 3-75
Maximum power density at 28 m from center of reactor (W/cm^2)	350
Coolant required for (both ends) (gpm)	10,340
Maximum inlet water temperature ($^{\circ}\text{C}$)	40
Life (yr)	1
Water pressure (psi)	200-250

B_{END} = the magnetic field at the dump (T) ,

$A_{CC} \cdot A_{END}$ = the area of the plasma the dump in the end cell (cm^2).

From conservation of energy, it is also true that:

$$(P_{CC}) (A_{CC}) = (P_{END}) (A_{END}) \quad (3-96)$$

where P_{CC} and P_{END} (kW/cm^2) are the heat fluxes of the plasma at the central cell and at the beam dump location, respectively.

Noting the area ratio present in both Eqs. (3-95) and (3-96), one can arrive at the following heat flux relation:

$$P_{END} = P_{CC} \frac{B_{END}}{B_{CC} (1 - \beta)^{1/2}} \quad (3-97)$$

The maximum central cell flux of $162.9 \text{ kW}/\text{cm}^2$ occurs at the central axis of the reactor. The central cell field is 4.5 T, and the average field strength is 0.24 T. Using Eq. (3-97) we can calculate the maximum incident power flux on the panel anywhere along the center axial reactor line, provided the field is known at that location. Figure 3-164 shows the resultant heat flux map calculated from Eq. (3-97). Centerline magnetic field strength values calculated by LLNL were used.

As the heat flux increases, the local pressure required to suppress boiling also increases as depicted in Fig. 3-172. We assumed an inlet temperature of 40°C and allowed a minimum temperature rise of 15°C . Then we calculated the film-drop temperature of the turbulent water flow using the following Dittus-Bolter Nusselt number relation:

$$Nu = 0.023 (Re)^{0.8} (Pr)^{0.4} \quad (3-98)$$

where

Nu = Nusselt number,

Re = Reynolds number,

Pr = Prandtl number.

Sputtering of End-Dump Surfaces. Sputtering caused by plasma particles can limit the life of the plasma dumps. In this section we outline our procedure for calculating the surface thickness sputtered away from the end dumps without addressing the question of lifetime.

The surface thickness sputtered, d in cm, is given by the relation:

$$d = \frac{t}{n} \sum_j \frac{N(V_j)}{eV_j} \sum_k f_k S_k, \quad (3-99)$$

where

$N(V_j)$ is the particle energy flux (kw/cm^2) eV_j is incident particle energy, V_j is the particle voltage (kV), e is the electron charge (C), n is the atomic number density of the surface material, t is the exposure time (s). S_k is the sputter yield in atoms per incident particle, f_k is the fraction of particles at the particular energy level. The summation factor over the plasma species contains this sputter yield and fraction. Since the exact angle of incidence of the incident particles is not known, we assume normal incidence and use Eq. (3-99) without modification.

Table 3-72 contains some of the input quantities used to compute sputtered thickness. Sixty percent of the incident energy flux is composed of 20-kV D and 40% of a 50/50 mixture of 27 = kV D and T particles. Substitution of these quantities into Eq. (3-99) results in the expression

$$d = \frac{t}{n} \left\{ 1.15 \times 10^{17} (27 \text{ kV}) S_D(27 \text{ kV}) + S_T(27 \text{ kV}) + 3.12 \times 10^{17} (20 \text{ kV}) S_D(20 \text{ kV}) \right\}, \quad (3-100)$$

that can be directly applied to the end dumps. Explicit results for d require input values for the remaining unspecified quantities in Eq. (3-100). Listed in Table 3-73 are the surface number densities and sputter-yields of the more promising material candidates: beryllium, aluminum, iron, copper, molybdenum, and tungsten. Results for iron, copper, and molybdenum can be applied to stainless steel, AMZIRC, and TZM surfaces respectively.

Table 3-72. Particles incident on end dumps.

Percent of energy flux	Particle Voltage V (kV)	Particle k	Particle fraction f_k
60	20	D	1.0
40	27	D	0.5
		T	0.5

Table 3-73. Atomic number densities and sputter yields.

Quantity	Dump surface material					
	Be	Al	Fe	Cu	Mo	W
$n \text{ (cm}^3\text{)}$	1.24	6.02	8.47	8.47	5.65	6.32
$S_D \text{ (20 kV)}$	5.0	5.3	7.1	1.7	2.0	4.4
$S_D \text{ (27 kV)}$	4.1	4.2	5.8	1.4	1.7	3.6
$S_T \text{ (27 kV)}$	6.5	6.9	9.5	2.3	2.8	6.3

Figure 3-170 shows resultant sputtering thickness for one full power year of panel operation for various materials as a function of heat.

Structural Analysis, Panel Coil Thickness and Life Time Analysis. The plasma end-dump panel is subjected to coolant pressure and temperature gradients that depend on the magnitude of the impinging heat flux. The temperature gradient also depends on the panel thickness. To make a conservative analysis for the material selection and scoping, we assume the panel thickness as the final thickness after sputtering erosion. Tables 3-74 through 3-78 list the candidate materials, their mechanical properties at various temperatures, and 10,000 hour creep-rupture stress.

The panel coil flow area selected from thermal considerations has a trapezoidal cross section of uniform thickness with a flow area of approximately 0.31 in^2 . For analysis purposes, we assumed the two panel plates to have the same thickness. The structural design must meet the requirements of the ASME Boiler Code. In Ref. 44, 10 panel coils are analyzed by computer codes based on finite element modeling methods. As our current program is a feasibility study, we felt that we could make our analysis without resorting to a finite element program. References 44 and 45 address problems of a similar nature. The ASME Boiler Code does not discuss pressure vessels of trapezoidal cross section, however, analysis procedures are outlined for noncircular, specifically rectangular pressure vessels. Figure 3-173 shows the actual coil geometry and the idealized rectangular section having the same flow area, depth and the same trial thickness T . The idealized section also shows locations M, N, and Q, where the stresses are to be computed for internal pressure per the Boiler Code, Section VIII Div I UA 706. At these three locations we computed the membrane and bending stresses for a given pressure and assumed thickness, using a joint efficiency factor of 0.8.

Thermal stress analysis requires knowledge of the temperature gradient through the coil-wall thickness. We computed gradient for an assumed thickness based on heat flux and conductivity of the chosen material. For a more detailed and complete thermal stress analysis, we must use a computer code based on a finite element model of the panel.

To assess the magnitude of the thermal stresses for preliminary sizing of structural components, it is customary to compute the thermal stresses based

Table 3-74. Mechanical properties of candidate materials at 200° F.

MATERIAL	MODULUS OF ELASTICITY E_1 Msi	POISSON'S RATIO ν^*	COEFFICIENT OF THERMAL EXPANSION α in/in/°F	YIELD STRESS S_y , KSI	TENSILE ULTIMATE STRESS S_u , KSI	10,000 Hr. CREEP REPTURE STRESS σ_R , KSI
AL6061	9.8	0.3	13.0	35	39	35
AMZIRC (54% CW)	18.7	0.3	9.8	46	60	50
SS304	28.0	0.3	8.4	60	120	90
TZM	46.0	0.3	2.7	127	148	138*

*ESTIMATED VALUE

Table 3-75. Mechanical properties of candidate materials at 300° F.

MATERIAL	MODULUS OF ELASTICITY E_1 Msi	POISSON'S RATIO ν^*	COEFFICIENT OF THERMAL EXPANSION α in/in/°F	YIELD STRESS S_y , KSI	TENSILE ULTIMATE STRESS S_u , KSI	10,000 Hr. CREEP REPTURE STRESS σ_R , KSI
AL6061	9.6	0.3	13.3	30	34	20
AMZIRC (54% CW)	18.5	0.3	9.8	45	58	45
SS304	27.5	0.3	9.2	60	115	87
TZM	45.0	0.3	2.8	125	144	135*

*ESTIMATED VALUE

Table 3-76. Mechanical properties of candidate materials at 400⁰ F.

MATERIAL	MODULUS OF ELASTICITY E_1 Msi	POISSON'S RATIO ν^*	COEFFICIENT OF THERMAL EXPANSION α in/in/ ⁰ F	YIELD STRESS S_y , KSI	TENSILE ULTIMATE STRESS S_u , KSI	10,000 Hr. CREEP REPTURE STRESS σ_R , KSI
AL6061	9.0	0.3	13.6	15	17	7.5
AMZIRC (54% CW)	18.0	0.3	9.9	44	55	43
SS304	27.0	0.3	9.6	56	110	83
TZM	44.0	0.3	2.8	124	141	134*

*ESTIMATED VALUE

Table 3-77. Mechanical properties of candidate materials at 500° F.

MATERIAL	MODULUS OF ELASTICITY E_1 Msi	POISSON'S RATIO ν^*	COEFFICIENT OF THERMAL EXPANSION α in/in/°F	YIELD STRESS S_y , KSI	TENSILE ULTIMATE STRESS S_u , KSI	10,000 Hr. CREEP REPTURE STRESS σ_R , KSI
AL6061	7.8	0.3	13.9	6	6.5	3.5
AMZIRC (54% CW)	17.5	0.3	10.0	42	55	37
SS304	26.2	0.3	9.9	54	108	75
TZM	43.0	0.3	2.9	122	138	130*

*ESTIMATED VALUE

Table 3-78. Mechanical properties of candidate materials at 600° F.

MATERIAL	MODULUS OF ELASTICITY E_1 Msi	POISSON'S RATIO ν^*	COEFFICIENT OF THERMAL EXPANSION α in/in/°F	YIELD STRESS S_y , KSI	TENSILE ULTIMATE STRESS S_u , KSI	10,000 Hr. CREEP REPTURE STRESS σ_R , KSI
AL6061	7.0	0.3	14.2	2.7	3	1.5
AMZIRC (54% CW)	16.5	0.3	10.1	40	53	30
SS304	25.5	0.3	10.3	50	106	71
TZM	42.0	0.3	2.9	120	135	127*

*ESTIMATED VALUE

on complete constraint. As discussed in Ref. 46, the maximum compressive stress caused by a temperature gradient of ΔT is

$$Q(t) = \frac{\alpha E \Delta T}{1 - \nu} \quad , \quad (3-101)$$

where

$Q(t)$ = stress,
 α = the thermal coefficient of expansion,
 E = Young's modulus of elasticity,
 ν = Poisson's ratio.

The stresses allowable by the Boiler Code for each stress classification are usually given in terms of the stress intensity S_{mt} , which is calculated from the tensile and creep properties of the candidate materials. Table 3-79 gives the criteria for calculating it. The Boiler Code considers primary and secondary applicable for the structure under consideration. Table 3-80 lists maximum allowable stress intensities based on a conservative design approach as Refs. 44 and 45 suggest. Using Tables 3-75 through 3-80, we established various panel coil thicknesses for heat fluxes from 100 to 600 W/cm². These thicknesses are listed in Table 3-81. Figure 3-171 shows the maximum temperature of the dump surface sputtering for one full-power year.

Conclusions. We computed plasma end dump panel coil thicknesses for different heat fluxes. From the stress analysis point of view, AMZIRC and TZM could meet the Boiler Code requirements for the heat fluxes shown in Table 3-81.

Material Selection for Plasma End Dump. The candidate materials should have the following material properties:

- A low sputtering rate,
- High thermal conductivity,
- Adequate strength for the operating temperature,
- Resistance to corrosion caused by coolant flow, and
- Ease of fabrication.

Table 3-79. Panel-coil structural design criteria.

TIME-INDEPENDENT ALLOWABLE STRESS:	S_m	LOWEST VALUE OF	1/3 ULTIMATE STRESS (S_u) AT TEMPERATURE 1/3 ULTIMATE STRESS (S_u) AT ROOM TEMPERATURE 2/3 YIELD STRESS (S_y) AT TEMPERATURE
TIME-DEPENDENT ALLOWABLE STRESS:	S_t	LOWER VALUE OF	2/3 OF STRESS TO CAUSE CREEP RUPTURE IN TIME t MINIMUM STRESS TO PRODUCE 1% STRAIN IN TIME t
GENERAL PRIMARY- MEMBRANE ALLOWABLE STRESS:	S_{m_t}	LOWER OF S_m AND S_t	

Table 3-80. Stress categories and maximum allowable stress intensities.

CATEGORY	INCLUDED IN CATEGORY	MAXIMUM ALLOWABLE STRESS INTENSITY
S_1	P_m	S_{m_t}
S_2	$P_m + P_b$	$1.5 S_{m_t}$
S_3	$2P_m + P_b + Q(t)$	$3 S_{m_t}$

P_m = Primary Mean Stress

P_b = Primary Bending Stress

$Q(t)$ = Maximum Thermal Stress Based on Complete Constraint =

$$- \frac{\alpha E \Delta T}{1-\nu}$$

Table 5-81. Plasma end dump heat flux versus panel thickness.

HEAT FLUX W/cm^2	MAX. SURFACE TEMP. OF	WATER PRESSURE REQUIREMENT PSI	AL6061		AMZIRC 54% CW		SS304		THZ	
			ΔT OF	THICKNESS t IN	ΔT OF	THICKNESS t IN	ΔT OF	THICKNESS t IN	ΔT OF	THICKNESS t IN
600	543	1000	--	--	90.0	0.12	--	--	--	--
500	477	540	--	--	62.8	0.10	--	--	107	0.06
400	408	277	--	--	34.6	0.07	--	--	57	0.04
300	342	121	--	--	18.0	0.05	--	--	32	0.03
200	275	45	19.0	0.04	10.0	0.03	--	--	15	0.02
100	206	13	5.0	0.02	2.5	0.02	56.0	0.02	7	0.02

Table 3-80. Stress categories and maximum allowable stress intensities.

CATEGORY	INCLUDED IN CATEGORY	MAXIMUM ALLOWABLE STRESS INTENSITY
S_1	P_m	S_{m_t}
S_2	$P_m + P_b$	$1.5 S_{m_t}$
S_3	$2P_m + P_b + Q(t)$	$3 S_{m_t}$

P_m = Primary Mean Stress

P_b = Primary Bending Stress

$Q(t)$ = Maximum Thermal Stress Based on Complete Constraint =

$$- \frac{\alpha E \Delta T}{1 - \nu}$$

Table 3-81. Plasma end dump heat flux versus panel thickness.

HEAT FLUX W/cm^2	MAX. SURFACE TEMP. OF	WATER PRESSURE REQUIREMENT PSI	AL6061		AMZIRC 54% CW		SS304		TiZ	
			ΔT OF	THICKNESS t IN	ΔT OF	THICKNESS t IN	ΔT OF	THICKNESS t IN	ΔT OF	THICKNESS t IN
600	543	1000	--	--	90.0	0.12	--	--	--	--
500	477	540	--	--	62.8	0.10	--	--	107	0.06
400	408	277	--	--	34.6	0.07	--	--	57	0.04
300	342	121	--	--	18.0	0.05	--	--	32	0.03
200	275	45	19.0	0.04	10.0	0.03	--	--	15	0.02
100	206	13	5.0	0.02	2.5	0.02	56.0	0.02	7	0.02

No single material has all of these properties. Molybdenum has a low sputtering rate, and precipitation-strengthened alloys of copper like AMZIRC have high thermal conductivity, moderate strength at elevated temperatures, reasonable resistance to corrosion, and ease of fabrication. Thus, we considered a molybdenum front surface bonded to AMZIRC. The bonding methods considered were:

- Plasma arc deposition of molybdenum on copper
- Chemical vapor deposition of molybdenum on copper
- Explosive welding.

Molybdenum and copper have very different coefficients of thermal linear expansion: $\alpha = 4.9 \times 10^{-6}/^{\circ}\text{C}$ for molybdenum and $\alpha = 17.6 \times 10^{-6}/^{\circ}\text{C}$ for AMZIRC. In addition, we need a thick coating of 5 to 10 mm of molybdenum on copper. Both the plasma arc and chemical vapor processes deposit molybdenum on copper at about 400 to 500 $^{\circ}\text{C}$, and after cooling, we found that the surface did not have a strong bond. However a weak bond is expected between copper-molybdenum because they have different coefficients of thermal linear expansion. We have not tried the explosive welding process.

Molybdenum can be brazed to copper, but the brazing temperature is about 800 $^{\circ}\text{C}$ and the process would anneal the copper alloy to very low mechanical strength levels, e.g., to that of annealed copper.

Thus our choice reduces to pure materials. The candidates considered were:

- TZM (0.5 Ti, 0.25 Zr, balance molybdenum),
- AMZIRC (0.15 Zr, balance OFHC copper),
- Al 6061 T-6, and
- Stainless steel 304.

We found that stainless steel had too low a thermal conductivity, which produces large thermal gradients. Al 6061 had good thermal conductivity but very little mechanical strength at 300 to 400 $^{\circ}\text{C}$. That leaves the choice of AMZIRC or TZM.

TZM has both a low sputtering rate and low thermal conductivity, in direct contrast to AMZIRC. AMZIRC could have to be joined by low-temperature soldering, which would be undesirable. High-temperature brazing would soften the material. On the other hand, TZM or molybdenum can be electron-beam welded and have good fabricability. If thermal gradients can be tolerated in TZM, then it would be the preferred material for plasma end

pumps. TZM has the advantage of very low swelling caused by neutron irradiation below 500 °C (Ref. 47), but it does become embrittled by neutron irradiation. Copper swells most between 300 °C to 400 °C (Ref. 48).

Key technical issues. Two issues must be addressed. As Fig. 3-164 shows, the maximum heat flux on the panel increases as the dump moves toward the center of the reactor. Although the cost of the vacuum vessel decreases with a smaller end cell, the dump cost increases because a higher technology panel is required.

The magnitude of tritium diffusion into the cooling water for the baseline panel should be calculated accurately. Separation of tritium from water can be a very expensive procedure. Running the dump surface at higher temperatures aggravates the problem, since the diffusion rate increases with temperature. There are expensive methods to design around the problem, e.g., using two bonded materials with different diffusion rates for the front surface of the dump. A cost analysis on the trade might warrant the more complex panel design.

3.11. TRITIUM STUDIES

3.11.1. Summary

On the basis of fueling requirements and probable operating characteristics of the TDF reactor and its auxiliary components, which were obtained from various program participants, we calculated a material balance for deuterium and tritium in the TDF system. The tritium system capacity and performance requirements were based on the determined material balance for a 20-MW, full-power, continuous operation. The technological approach for the tritium subsystems was based primarily on the Tritium Systems Test Assembly (TSTA) development program at Los Alamos National Laboratory but also included results from the TFTR program at Princeton University, the Mound Laboratory tritium-treatment systems, and the design studies for research and commercial fusion systems, including the Fusion Energy Design Center.

Most of the effort in designing the TDF tritium systems went into evaluating the capabilities of subsystems for allowing safe operation of the TDF reactor and for safe handling of its estimated tritium inventories. Models were developed to evaluate the capability of the probable cleanup systems to timely clean up postulated facility accidents. This analysis indicates that the proposed designs for tritium systems will be adequate for maintaining tritium release values well within the design goals for credible accident conditions. The major uncertainty involves the tritium permeation rates through the beam dumps to the cooling water.

3.11.2. System Requirements

The purpose of the TDF tritium systems is to allow the tritium fuel for the fusion device to be received, stored, purified, and recycled but be sufficiently contained so that the operating personnel or local population in the vicinity of the facility are not subjected to potentially harmful exposures of tritium. The tritium-related subsystems for accomplishing this include the following:

- Tritium Receiving and Storage (TRS).
- Fuel Cleanup (FCU).
- Isotope Separations (ISS).
- Fuel Preparation (FPS).
- Tritium Waste Treatment (TWT).
- Tritiated Water Recovery (TWR).
- Facility Air Detritiation (FAD).
- Glovebox Gas Treatment (GGT).
- Tritium Systems Data Acquisition (TDA).

The tritium systems must maintain airborne concentrations of tritium below $5 \mu\text{Ci}/\text{m}^3$ in spaces occupied by unprotected operating personnel and below $500 \mu\text{Ci}/\text{m}^3$ for protected (in bubble suit) operating personnel. In addition, the tritium systems are designed to satisfy the following tritium release goals:

Normal tritium loss from facility	$\leq 10 \text{ Ci/d}$	$\leq 1000 \text{ Ci/y}$
Maximum off-normal loss from facility	$\leq 30 \text{ Ci/d}$	$\leq 5000 \text{ Ci/y}$
Maximum accident release	$< 10 \text{ g}$	$< 50,00 \text{ Ci}$

3.11.3. Tritium Material Balance

We calculated a tritium material balance to provide a basis for the design of the various tritium-related subsystems including tritium inventories. Calculations were based on the data of Table 3-82.

The calculated burn-up rates for deuterium and tritium under these conditions were 2.05 and 3.07 g/d, respectively, with the generation of 4.08 g/d of He-4. The calculated material balance is shown in Table 3-83. The design of the tritium systems is based on flow rates shown in the table, but these are only approximate values. In the mass balance calculations, the efficiencies of the neutral-beam injectors and the back pumping of the NBI vacuum pumps were considered. On the basis of calculations performed at TRW, about 24.6% of the NBI feed gas will be injected and 85.3% of the injected gas will enter the plasma. The close proximity of some of the NBI beam dumps to NBI ducts will result in some of the injected gas being removed from the reactor chamber by the NBI vacuum pumps. Conductance calculations by the

Table 3-82. Fuel and plasma stability requirements for a 2-MW full-power operation of the fusion device.

System	Species (T or D)	Current (A)
Central cell NBIs	T	540
Central cell NBIs	D	540
Anchor and transition NBIs	D	66.3
Central cell fuel injectors	D	800

Table 3-83. Reactor fueling rates and vacuum pump exhaust rates for full-power (20 MW) operation.

System	Species	Flow rates	
		(atom moles/s)	(g/d)
<u>Fueling rates</u>			
Central cell NBIs	T	2.677×10^{-2}	6976
Central cell NBIs	D	2.677×10^{-2}	4659
Anchor and transition NBIs	D	3.286×10^{-2}	5718
Central cell fuel injectors	D	8.291×10^{-3}	1443
<u>Exhaust rates^a</u>			
Central cell tritium			
NBI vacuum pumps	D	1.010×10^{-3}	176
	T	2.121×10^{-2}	5526
Central cell deuterium			
NBI vacuum pumps	D	2.121×10^{-2}	3690
	T	1.010×10^{-3}	263
Anchor and transition			
NBI vacuum pumps	D	2.480×10^{-2}	4316
	T	1.239×10^{-5}	3
Plasma end cell vacuum pumps	D	2.089×10^{-2}	3636
	T	4.531×10^{-3}	1181

^aAssumes 24.6 NBI efficiency and 85.3% plasma penetration of the injected neutral beam particles; assumes 10% (5% T and 5% D) of the injected gas will be drawn back up the NBI ducts by the NBI vacuum pumps because of the close proximity of the NBI beam dumps to some of the ducts and the relatively high conductance of these ducts. Only 0.1% of the injected gas is assumed to be drawn back through the anchor and transition NBI ducts because of the remote location of the beam dumps and the low duct conductance.

designer indicated that 10% of the gas injected in the central cell will be removed by the NBI vacuum pumps, approximately 5% D and 5% T. Because the NBI beam dumps in the anchor and transition cell are further removed from their injection ducts and because the duct conductances will be much smaller, we assume only 0.1% of the injected gas will be drawn back into the NBI vacuum pumps in the anchor and transition cells, again a 50% split of D and T.

A summary of the mass balance calculations and the distribution of various gas streams to the tritium systems is shown schematically in Fig. 3-203. The Isotope Separations System (ISS) separates the various hydrogen isotopes prior to recycling of the recovered fuel so that the fuel can be blended to the desired mix prior to reinjection. Because condensable contaminant gases collect in the ISS cryogenic distillation columns, we must maintain the contaminant gas levels to less than about 1 ppm (vol.) to minimize column cleanout. Thus, the contaminant gases are removed in the fuel cleanup system.

3.11.4. Fuel Cleanup System

When the fusion device reaches its maximum operating temperature, a number of contaminants, most of which will become gaseous, are expected to be liberated from the various materials of construction. These contaminant gases, along with trace amounts from in-leakage, are removed by the fuel cleanup (FCU) system prior to entering the ISS unit. Little information is available on the probable contaminant concentration from the fusion device. Personnel of ISTA surveyed fusion-device researchers to obtain a consensus of the most probable contaminant concentrations in order to estimate concentrations in the end dump vacuum pump gas streams of the TDF plasma (see Table 3-84).

A schematic flow diagram of the fuel cleanup system is shown in Fig. 3-204. The exhaust gas from the plasma end dump vacuum pumps is first passed through a catalytic reactor operated at about 450 K to convert free oxygen to an appropriate oxide. Then the gas is cooled and the contaminants are removed by cryogenic adsorption on molecular-sieve beds at a temperature of about 75 K. At this temperature all of the oxides, hydrocarbons, ammonia, and argon will condense and be adsorbed on the molecular-sieve beds. The only gaseous species that will not condense at these temperatures will be hydrogen isotopes and helium, which will eventually pass directly to the ISS unit, although there is some holdup of the hydrogen isotopes early in the cycle.

Figure 3-203. Tritium processing and treatment systems for TDF.

Table 3-84. Estimated contaminant-gas concentrations in the vacuum-pump exhaust for the plasma vessel of TDF.

Gaseous component	Atomic %	Flow rate (g/d)	Probable chemical species
He	0.04	4.1	He
Ar ^a	1.17	1221	Ar
C	0.02	6.3	C(D ₁ T ₁ H) ₄
N	0.10	36.7	N(D ₁ T ₁ H) ₃
O	0.50	209.7	(D ₁ T ₁ H) ₂ O, CO, CO ₂
H	1.00	26.4	DT, HD, HT ₁ DT ₂ ,
D	75.54	3989	T ₂ , and H ₂
T	21.63	1710	

^aArgon at about 30 times the He ash concentration is introduced in the vacuum pumps to pump the helium.

Figure 3-204. Fuel cleanup system for TDF.

The condensed containment gases are removed from the molecular-sieve beds by heating the beds to about 475 K and purging with dry nitrogen followed by D_2 . The purged contaminant gas is heated and passed through another catalytic reactor operated at about 800 K. A slight excess of oxygen is added to ensure complete decomposition of ammonia and hydrocarbons and to convert the hydrogen isotopes to water. The water is recovered in a freeze-out trap operated at about 160 K; the argon, carbon dioxide, oxygen, and nitrogen will pass through. These gases are directed to the tritiated waste treatment (TWT) system for trace tritium removal before being discharged. At 160 K, the vapor pressure of water is at least 10,000 times lower than that of the other species. The hydrogen isotopes, collected as water in the freeze-out trap, are heated to a vapor form and passed through a hot metal getter bed of activated uranium at a temperature of about 750 K. This strips the oxygen and allows the H, D, and T to pass through to the plasma end dump vacuum pump feed stream of the first catalytic reactor. Because there are two molecular-sieve adsorbent beds, two freeze-out traps, and at least two hot metal beds, these systems can be operated continuously.

The exhaust gases from the deuterium and tritium central cell neutral-beam vacuum pumps pass through a molecular-sieve adsorbent bed cooled to 75 K. This removes contaminant gases, including ammonia, argon, oxygen, and the hydrocarbons. These species are periodically removed and recycled to the primary fuel cleanup portion of the system. The recovered deuterium from the neutral-beam vacuum pumps of the anchor and transition cells bias only trace impurities, including tritium, so only a portion of this gas stream needs to be sent to the FCU system. Most of this gas stream can be returned directly to the fuel preparation system.

3.11.5. Isotope Separation System

The purified hydrogen isotopes from the FCU are sent to the middle of the first distillation column in the ISS unit, as shown in Fig. 3-205 and Table 3-85. The distillation columns are packed with commercial, stainless-steel packing material and are operated at about 20 K.

In Column 1, H and D are fractionated from T and are sent to Column 2 through a catalytic, room-temperature equilibrator. The equilibrators allow the reestablishment of equilibrium between the six hydrogen-isotope species

SAI-820P-86

Figure 3-205. Isotope separation system for TDF.

Table 3-85. The approximate dimensions of the ISS distillation columns of TDF.

<i>Column</i>	<i>Packed height (m)</i>	<i>i.d. (cm)</i>
1	4.1	8.2
2	4.1	5.6
3	3.2	7.0
4	4.1	11.1

(H^2 , D^2 , T^2 , HD, HT, and DT), which allows a cleaner separation. The protium specie is fractionated from D^2 and any remaining trace T species in Column 2 and sent to the TWT system for further cleanup and for discharge or disposal. The materials at the bottom of Column 2 are sent to the middle of Column 3 by means of another equilibrator. Here, the D^2 is fractionated and a portion of the product is pumped to the fuel preparation system (FPS) while the remainder recycled to the top of Column 1. At this point, the D^2 is about 99.96% pure.

The materials at the bottom of Column 3 contain trace contaminants of T and are returned to the middle of Column 1 for a recycle, again through an equilibrator. The tritium, which is concentrated in the bottom of Column 1, is directed to the middle of Column 4 where the tritium is purified to greater than 99% (isotopically pure) and is sent to the FPS. The overheads of Column 4, which contain some of the D species, are returned for recycle with the bottoms from Column 3.

The ISS is designed basically in accordance with the TSTA design concept and equipment. The probable flow rates for TDF, as determined in the material balance calculations, are also shown in Fig. 3-204.

3.11.6. Fuel Preparation System

The fuel preparation system consists of a series of transfer pumps, tanks, and fuel blenders. Feed gas for this system comes from the tritium storage beds, deuterium storage tanks, the ISS unit, and the anchor and transition neutral-beam vacuum pumps. Separate portions of this system are discussed in the various fueling systems for the neutral-beam injectors and the pellet injector.

3.11.7. Tritiated-Waste Treatment System

The tritiated waste treatment system collects the various gaseous waste streams in TDF that may be contaminated with tritium, separates the tritium and either recycles the tritium to the ISS unit (by way of the tritiated water recovery--TWR--system) for isotopic purification and reuse as fuel or solidifies the tritiated-containing compounds for solid waste disposal. The

potential tritium-contaminated-waste gases from the FCU, ISS, TWR, and the facility air detritiation (FAD) are collected with the use of a compressor and temporarily stored in a pressurized holding tank. The contaminated air is then treated with a cleanup system similar to that shown in Fig. 3-206. In this treatment scheme, the contaminated gas is heated and passed through a catalytic reactor at about 810 K to convert the hydrogen isotopes to water (oxygen is added as necessary). The gas is cooled to condense the water, and the resulting tritiated water, along with the nontritiated water, is adsorbed, at about ambient temperature on two beds of molecular-sieve zeolite in series. With heated catalyst reactors, oxide conversion factors of 10^5 are obtainable. For water removal, decontamination factors (DFs) (where the DF is defined as the inlet tritium concentration divided by the outlet tritium concentration) of $>10^6$ are also obtainable with adequately sized molecular sieve beds.

As an added measure of safety, steam injection is provided between the first and second molecular-sieve dryer beds. The objective is to add nontritiated water, which as a carrier will increase the removal of tritiated water in the second dryer bed. The steam injection may or may not be used depending on the tritiated-water concentration in the effluent of the second dryer bed. Two sets of dual molecular-sieve dryer beds are used in a parallel (rather than series) arrangement so that one of each set can be regenerated separately while the other is in service. Regeneration is accomplished by passing heated air in the reverse direction through the molecular-sieve dryer beds, recovering most of the water in the regeneration gas with a cooler-condenser, and removing the remaining water in a smaller molecular-sieve dryer. The beds can also be regenerated with a hot air recirculating system using the cooler-condenser to remove the water. The recovered tritium-contaminated water in the TWT system can either be sent to the TWR system for tritium removal or solidified for waste disposal. The TWT system has a gaseous treatment capacity of about $85 \text{ m}^3/\text{h}$ (50 cfm).

3.11.8. Tritiated-Water Recovery System

The primary purpose of the tritiated-water recovery system is to remove tritium from the plasma end dump and from the cooling of the tritium neutral-beam dump water. Tritium permeates through the beam dumps into the

Figure 3-206. Contaminated air distribution system for TDF.

cooling water at significant rates because of implantation energy and the relatively high temperature of the beam dump material. It has been estimated that for a full-power year of operation about 21 g of tritium will enter the cooling water through the neutral-beam dumps and about 98 g of tritium will enter through the plasma end dump.

Because leaks in the beam dump cooling system are virtually impossible to prevent, we must reduce the tritium in the coolant to a tolerable concentration. Experience with nuclear reactors and other processes using tritiated water indicates that a tolerable level of tritium coolant is about 1 Ci/liters or less. It can be shown that to maintain the tritium concentration in the beam dump cooling water below about 1 Ci/liter, tritiated water must be removed at a rate equivalent to the curies per liter of tritium entering the cooling water. Because the fusion device will only be operated with a maximum availability of about 50% full power per year, the TWR system should have a process capacity of about 1600 liters/d. With the TWR system in operation, about 50 days of full-power operation by the fusion device would be required before the tritium concentration in the cooling water reaches 0.9 Ci/liter.

The TWR system is shown schematically in Fig. 3-207. The process involves combined electrolysis and catalytic exchange technology. The tritiated water is injected into the middle of the exchange column that is filled with a catalyst material to promote the exchange of gaseous hydrogen isotopes with water. Because the equilibrium at the column operating temperature is near ambient, formation of tritiated water (HTO) is favored, and enriched HTO concentrates in the bottom of exchange column. A portion of this enriched HTO is removed and electrolyzed to form hydrogen isotopes and oxygen. Most of the electrolyzed hydrogen isotopes are returned to the bottom of the exchange column to further enrich the water in tritium, while the remaining portion of the gas is directed to a cryogenic distillation column for additional isotopic enrichment. The overheads from the distillation column are returned to the top (stripping portion) of the column for exchange with the tritium-depleted water and stripping of any remaining tritium from the gas. The tritium-free hydrogen gas, mixed with oxygen from the electrolyzer, then exits through the top of the exchange column, recombine to form water, and returns to the cooling-water system.

Figure 3-207. TDF tritiated-water recovery system.

SAI-820P-60

Because the tritiated water is enriched in the exchange column by a factor of about 5000, the feed to the cryogenic distillation column is relatively small. The deuterium in the cooling water will be enriched in a similar manner to tritium but to a lesser extent and will follow it through the process be partially mixed with the protium isotope.

3.11.9. Facility Air Detritiation System

The facility air detritiation system consists of three identical $8500 \text{ m}^3/\text{h}$ (5000 cfm) treatment systems for removing tritium-contaminated gaseous wastes from the reactor building, the tritium processing building, and the hot cell. Any one or all of the units can be directed to the cleanup of one or more of these spaces. The process flow sheet for each of these treatment systems is shown in Fig. 3-206. The removal process involves the oxidation in a catalytic reactor of all airborne tritium and its compounds to tritiated water and their removal on molecular-sieve dryer beds. Redundant dryer beds provide regeneration capabilities that allow continuous operation of all three of the $8500 \text{ m}^3/\text{h}$ systems. The regeneration procedures for the dryer beds are the same as described for the gaseous TWT treatment system.

3.11.10. Glovebox Gaseous Treatment System

For tritium systems in which large inventories of tritium are likely, portions are enclosed in a secondary containment system called the Glovebox Gaseous Treatment System. These include the TRS, FCU, ISS, FPS, and the TWR systems. Argon is in the secondary containment system at a slightly positive pressure at about 500 Pa or 2 in. of water. The total volume of the glovebox gaseous treatment (GGT) system is about 850 m^3 . The argon is constantly recirculated and constantly monitored for the presence of either hydrogen isotopes or oxygen that would indicate a leak in one or more of the enclosed units. Enough sampling ports are provided to localize a potential leak. A $85\text{-m}^3/\text{h}$ (50 cfm) gaseous treatment system will remove any hydrogen isotopes or oxygen that may leak into the secondary containment. The GGT system consists of essentially the same elements as the TWT and FAD systems (see Fig. 3-206). If for any reason the GGT becomes inoperative, the argon in the secondary containment can be processed with the TWT system and fresh argon replenished from storage.

Isolation valves will also be included in the system so that, should a leak develop in one of the components, the entire system will not become contaminated.

3.11.11. Tritium Systems Data Acquisition

Each individual tritium treatment system has redundant tritium monitoring instruments as part of the microprocessor-based operational control system. There are a number of other stack and area tritium and combustible gas monitors in various facility locations. The indicated presence of high tritium or combustible gas concentrations by these monitors would cause the automatic operation of one or more of the gas treatment systems or a venting operation. These comprise the tritium systems data acquisition (TDA) system which also provides backup control for the various processes and provides the communication link with the master computer control system for the facility.

3.11.12. Response of Tritium Systems to Accidents

The response time of the tritium cleanup systems for an accidental release clearly depends, among other variables, on the source term and treatment system capacity. Based on the calculated material balance and individual component design, we estimated the probable tritium inventories at various locations in the TDF (see Table 3-86). In addition, we analyzed accident scenarios for the most probable serious accident and the worst-case accident involving the release of the tritium inventory to their respective spaces. Models were developed for each scenario and the response of the various systems to a postulated accident was determined. The assumed tritium conditions for each space are summarized in Table 3-87.

Reactor Building. The most probable serious accidental release in the reactor building would be the loss of one of the tritium neutral-beam injector vacuum pumps with the subsequent release of its tritium inventory, about 100 g, into the reactor building. Figure 3-208 we show the calculated tritium concentration remaining as a function of time and of various FAD system cleanup rates. In these calculations we assumed a mixing efficiency of 70%, that is,

Table 3-86. Estimated tritium inventories at various locations in TDF.

Location/system	Tritium ^a	
	(g)	(Ci)
Plasma	<0.1	<30
Vessel walls and beam dumps	<10 ^b	<1 x 10 ^{5b}
Plasma end beam dump vacuum pumps	66	6.4 x 10 ⁵
Tritium NBI vacuum pumps	307	3.0 x 10 ⁶
Fuel preparation system	<100 ^b	<10 x 10 ^{6b}
Fuel cleanup system	100	10 x 10 ⁶
Isotope separation system	600	5.8 x 10 ⁶
Tritiated water recovery system	2	0.2 x 10 ⁴
Tritiated waste treatment system	20	2 x 10 ⁵

^aBased on full-power operation and represents maximum values.

^bVery uncertain estimates.

Table 3-87. Facility characteristics and assumed conditions for serious accident cases involving release of tritium inventories to their respective spaces.

Building/System	Building/system volume (m ³) (vol%/D)	Building/system leak rate	Cleanup rate (m ³ /d)	Source term	Initial Tritium concentration ^a (Ci/m ³)
Reactor building	82,500	1	825	100 g	11.8
Tritium processing building	18,500	1	185	300 g	158
Hot cell	28,000	1	280	100 Ci/d	0
Glovebox gaseous treatment	852	10 ⁻³	8.5 x 10 ⁻³	600 g	6.8 x 10 ³

^aAssumes instantaneous, complete mixing.

Figure 3-208. Tritium concentrations remaining in the reactor building as a function of time and of cleanup rates for an accidental release of 100 g of tritium.

the actual cleanup rate v_t was 30% less than that shown. This approach is not exact but should be a fairly close approximation. As seen from Fig. 3-208, little is gained by using a total cleanup rate greater than about 25,500 m^3/h (approximately 18,000 m^3/h actual). With the use of three 8500- m^3/h cleanup units, the time required to reduce the tritium concentration in the reactor building to 500 $\mu\text{Ci}/\text{m}^3$ will be about 46 h for the assumed conditions. About 67 h of continuous operation will be needed to reduce the concentration to about 5 $\mu\text{Ci}/\text{m}^3$. In these calculations, we assumed that any tritiated water formed in the reactor building atmosphere would not be adsorbed on the building walls to any appreciable extent and therefore would not change the removal rate. Calculations regarding the counterdiffusion of tritium through the building leakage paths indicated that loss of tritium through this mechanism is completely insignificant (<0.1 Ci) provided there was a negative pressure differential of several centimeters of water in the reactor building compared with the surrounding atmosphere. If this pressure differential were lost, namely through the loss of all cleanup systems, then the loss of tritium per day was estimated to be 0.21 Ci/d for a leak rate per day of 1 vol% and 1.1 Ci/d with a 10 vol% leak rate per day.

The loss of tritium through atmospheric discharge from the cleanup system was also determined to be trivial. Nearly all exhaust from the cleanup system is recirculated. Only the amount of gas needed to compensate for the in-leakage required to maintain a slight negative-pressure differential is discharged to the atmosphere. Even if a decontamination factor (DF) of only 10^4 is assumed for the tritium removal system, the amount of tritium lost through this path would only be about 0.13 Ci for a leak rate per day of 1 vol% and about 1.3 Ci for a 10 vol%/d leak rate with all three FAD systems operating. The DF for the tritium cleanup system will probably be in the range of 10^5 to 10^6 , depending on the actual design.

If for some reason the tritium inventory in all of the vacuum pumps in the reactor building were released at one time, the tritium cleanup times and building losses would be somewhat greater than those indicated.

Tritium Processing Building. Although the estimated total tritium inventory in the tritium processing building (about 720 g) is greater than for the reactor building (375 g), the probability for an accidental release is less

because of the extensive use of double containment. In addition, provisions have been made to recover the tritium in the event of a failure. For example, in the ISS a high pressure storage tank is provided to accept most of the column inventory in case of a sudden loss of cooling. In addition, spare powdered uranium-filled storage tanks are provided that will allow storage of the entire column inventory as a hydride. To lose any appreciable amount of tritium to the tritium processing building also requires a simultaneous major rupture in the GGT system enclosure with the failure of the primary system. It was concluded that the most probable serious accident would release no more than about 300 g of tritium to the tritium processing building.

The time required from one to three of the FAD units to cleanup the atmosphere of the tritium processing building after contamination with 300 g of tritium is presented in Fig. 3-209. With all three units in operation, the times required to reduce the tritium concentration to levels of 500 and $5 \mu\text{Ci}/\text{m}^3$ are about 13 and 18 h, respectively.

Hot Cell. Little information is available on the source term magnitudes for contaminated materials that may require maintenance or disposal. Source terms as high as 100 Ci/d can be handled in the hot cell if the airborne tritium concentration is maintained below $500 \mu\text{Ci}/\text{m}^3$ by the operation of two or more of the FAD units (see Fig. 3-210), assuming a constant source term. Assuming uniform hot cell mixing, the largest source term that can be handled while maintaining the average tritium concentration at no more than $500 \mu\text{Ci}/\text{m}^3$ is about 216 $\mu\text{Ci}/\text{d}$ when all three FAD units are in use. This calculation also assumes 70% mixing of the recirculated, cleanup system, exhaust gas. If the average airborne-tritium concentration is to be maintained below $5 \mu\text{Ci}/\text{m}^3$, the largest source term that can be handled in the hot cell is about 2.2 Ci/d.

Glovebox Gaseous Treatment System. For the GGT system, we assumed the entire tritium inventory of the ISS unit (about 600 g) could be lost to the argon gas. If this were the case, about 7 days would be required to reduce the tritium concentration in this system to $500 \mu\text{Ci}/\text{m}^3$ with the $85\text{-m}^3/\text{h}$ GGT cleanup system and about 9 days would be required to reduce it to $5 \mu\text{Ci}/\text{m}^3$. Because this system is small and can be tightly sealed, the leak rate is assumed to be less than 10^{-3} vol%/d or $8.5 \times 10^{-3} \text{ m}^3/\text{d}$.

Figure 3-209. Tritium concentration in tritium processing building as a function of time and of cleanup rate for an accidental release of 300 g of tritium.

Figure 3-210. Tritium concentration in hot cell as a function of time and of cleanup rate for a 100 Ci/d source term.

3.11.13. Conclusions

The capabilities of the facility cleanup systems for the assumed conditions and inventories are presented in Table 3-88. The estimated total tritium release for even the worst-case accident scenarios is well within the design goals. Provided the facility integrity is maintained and electrical power is not lost for more than a day or so, the tritium cleanup systems should keep the tritium release below the design goals for all postulated accident scenarios.

3.11.14. Uncertain Technical Issues

The major technical uncertainties that could significantly affect the design and operation of the TDF are the following:

- Rate of tritium permeation into the beam-dump cooling water.
- Presence of residual tritium in reactor components that require maintenance in the hot cell.
- Actual leak rates from the facility.

A large scale demonstration of the technology for removing tritium from large volumes of cooling water has not yet been demonstrated and will undoubtedly be very costly unless drastic technological breakthroughs occur. In addition, considerable uncertainty exists in the assumed tritium permeation rates through the beam dump materials under the operating conditions. If the estimates made with regard to both of these uncertainties turn out to be too low, the operation of the TDF and the cost of the tritium systems could be appreciably affected. However, the development of techniques to reduce the tritium permeation rates and the development of improved and less costly techniques for purification of tritiated water are considerably more likely than not.

The accumulation of tritium and its subsequent release during disassembly and maintenance of certain reactor components may not be a problem at all, but insufficient information was collected in this brief study to assure that such will be the case.

Table 3-88. Responses of facility cleanup systems to accidents and estimates of tritium losses to the atmosphere.

Building/System	Inventory released (g of T)	Assumed leak rate (vol%/d)	Time (h) to reduce airborne tritium concentration to:		Tritium lost to atmosphere	
			500 $\mu\text{Ci}/\text{m}^3$	5 $\mu\text{Ci}/\text{m}^3$	Through cleanup- system discharge ^a	Out-leakage loss of cleanup system (Ci/d) ^b
<u>Most probable serious accident</u>						
Reactor building	100	1	46	67	0.2	0.2
Tritium processing building	300	1	13	18	~0.1	0.8
Glovebox gaseous waste treatment	50	10 ⁻³	140	185	~0.1	~0.1 ^c
<u>Worst-case accident</u>						
Reactor building	375	10	52	73	7	4.0
Tritium processing building	600	10	14	19	2.5	1.7
Glovebox gaseous waste treatment	600	10 ⁻²	164	210	~0.1	0.15 ^c
<u>Earthquake</u>						
Reactor building ^d	100	10	46.6	67.7	~20	~2000 ^e

^aAssumes a cleanup system DF of 10⁴ and all three FAD units operating for reactor and for tritium processing buildings.

^bAssumes no pressurization.

^cLosses are to the tritium processing building and dp at 1-in. water gauge assumed.

^dAssumes instantaneous pressurization to 3 psig and a leak rate of 10 vol%/d at this pressure.

^eTakes 37 min to reduce pressure to ambient with all three FAD systems operating and discharging to the atmosphere.

Some uncertainty remains with respect to how well such a large facility can be sealed to minimize leak rates. It appears that inflatable seals for the large access doors can be designed to maintain the leak rates below the design goal. This is not a major issue as the cleanup systems as designed will handle considerably larger leakage rates while still meeting the tritium release goals of the design.

3.12. PLANT FACILITIES

3.12.1. Introduction and Summary

The plant for TDF includes features common to any large industrial facility as well as features unique to fusion. For example, the heat rejection system that uses natural draft cooling towers is generic to power plants and to many industrial facilities. On the other hand, hot cell facilities with capabilities for remote repair and maintenance functions and tritium-processing facilities with capabilities for separating large quantities of tritium from other gases are both unique to a fusion plant. The cost estimate of such special plant facilities (Section 5) shows their economic importance to the overall plant.

The various elements of the plant are designed and arranged to achieve a smoothly functioning and economic arrangement. Facilities that are functionally interdependent are located close to one another to provide operational ease and flexibility and to reduce cost.

Most of the conceptual design effort is focused on the facilities unique to fusion while a less detailed definition is given for the remaining facilities. These efforts have resulted in an estimate of approximate sizes and an identification of the general characteristics of the major facilities, the principal elements which are summarized in the following paragraphs.

Mechanical Systems. The heat transport system removes heat from the reactor and from plant auxiliary components and discharges this heat to the atmosphere by means of a mechanical draft cooling tower. Maximum continuous heat discharged is 258 MW.

The emergency cooling water system provides cooling water to the safety-related components (e.g., atmospheric tritium-cleanup units, diesel generators, control room) in the event of a loss of off-site power supply.

The handling system for activated structures provides for off-site disposal of structures activated as a result of reactor operation. The activated structures include spent test modules, beam dumps, and end cell dumps.

The radioactive-waste handling system provides for collecting, processing, and off-site disposal of radioactive solid, liquid, and gaseous wastes generated in the plant. These wastes include demineralizer resin slurry, effluent from equipment and building floor drains, and vented gases from equipment and storage tanks.

In addition, a number of other mechanical systems support safe operation and maintenance of the plant.

Alternating Current Power System. The ac power system consists of a heavy-duty and a light-duty substation, each receiving power from two independent utility lines and distributing the power through protected electrical feeders to all the electrical loads. It also includes standby diesel power generators and the noninterruptible battery inverter power supplies that provide backup power for essential loads if the utility power fails. The heavy-duty line provides the TDF operating power to the major loads listed in Table 3-89. If the heavy-duty power fails, the light-duty system would continue to provide power to the loads listed in Table 3-89 and to provide enough backup power to heavy-duty systems to provide for testing and maintenance operations. In general, the ac power system includes all electrical equipment that receives or generates ac power, transforms the power to voltage levels needed by TDF loads, and distributes the power through protected electrical feeders to the various TDF loads.

Buildings and Structures. The major buildings and structures that have interdependent functions, that require close proximity, and that are plant safety related are located on a common floor mat and on multiple levels. This arrangement is cost effective and does not affect operational flexibility. These buildings are the reactor building, the hot cell building, and the tritium building. The power supply and cryogenics building is not a safety-related building. To reduce cost, this building is structurally separate from the reactor building but is in close proximity and has a common wall with the reactor building. Although a major building, the control building is located approximately 100 m away from the reactor building to reduce magnetic field effects on the computers.

In addition to the above major buildings, other buildings and structures are provided to support safe operation and maintenance of the plant.

Table 3-89. Major electrical power loads.

	MW	MV•A
• Tritium neutral-beam power supplies	75	85
• Deuterium neutral-beam power supplies	95	105
• ECRH power supplies	5	5
• Water-cooled copper-coil power supplies	52	60
• Cryogenic system power	12	15
• Cooling system power	10	12
• Fueling, processing, and tritium-cleanup power	8	10
• Vacuum-system power	2	3
• Superconducting-coil power supplies	1	2
• Facilities (Table 2-94)	<u>15</u>	<u>18</u>
Total	275	315

Key Issues. The design of the hot cell building is a key facility design issue. The remote maintenance requirements of the reactor, though not yet adequately identified will determine the hot cell size and may significantly increase the building size.

3.12.2. Facility Requirements

The plant facilities requirements can be classified under four categories--functional, design, operation and maintenance, and safety. The design requirements derive from all the other requirements and specify the criteria to be used while designing a system or a component.

Functional Requirements. The functional requirements provide the support functions for one plant as a whole. Thus plant facilities must provide the following:

- Housing and structural support for all the equipment, components, and services of the plant.
- Protection from excessive release of radiation to the environment (allowable release limits are discussed in Section 4, Safety Assessment).
- Protection for safety of public and plant personnel from natural phenomena, including tornado and seismic events.
- Features for safe and efficient movement of material, personnel, and equipment as required for plant operation and maintenance.
- All facilities and services that will support safe functioning of the plant, e.g., supplies of cooling water, electricity, radioactive-waste handling facilities.

Operation and Maintenance Requirements. These requirements are discussed in Section 3.16 of this report (Maintenance).

Safety Requirements. These requirements specify the upper bounds on the consequences of operation of a system or a component in order to maintain safety of public and plant personnel. (See discussion in Section 4.)

Design Requirements. As already mentioned, the design requirements derive from all other requirements. Although many are required for a detailed design of plant facilities, only a few major requirements are considered for the present conceptual design effort.

- Neutron and gamma radiations--The walls, floors, and ceilings of the reactor building and hot cell building shall be adequately shielded (approximately 2-m-thick concrete) to limit release of these radiations.
- Tritium permeation--The internal surfaces of the reactor building (including radwaste and heat-exchanger areas), hot cell building, tritium building, and the cryogenics building, are lined with epoxy coating to limit tritium permeation into concrete.
- Building design leak rate--To limit leakage of radioactive gases (primarily tritium) from the reactor building, hot cell building, and tritium-processing building, we will assume a leak rate of less than 10% per day. The basis for this rate is discussed in Section 4, Safety Assessment.
- Plant normal and emergency cooling--During normal operation, a total of 258 MW of thermal power must be removed from the TDF device, its support systems, and miscellaneous plant facilities. In addition, several components and facilities require emergency cooling water in case the normal cooling water supply is disrupted because of a loss of off-site power supply. The total emergency cooling load is expected to be less than 1 MW. The components and facilities requiring emergency cooling water are the atmospheric tritium cleanup units, the diesel generators, and the control building.

Structural Design Criteria. These criteria govern the design of the buildings and structures for the TDF plant. In general, the design criteria comply with all appropriate federal regulations as well as with all industry codes and standards. State and local government regulations, codes, and standards must be added to these criteria after a site is selected.

The criteria specified herein are based largely on those established for the breeder plant and, as a result, are more detailed than usual for this TDF study. The information is readily available, and will provide a useful base

for future studies. It is implicitly assumed that most of the structural design criteria applicable to breeder and light-water-reactor plants will also apply to fusion plants.

Codes and Standards. Unless stated otherwise, the design of all buildings and structures is tentatively based on applicable portions of the following codes, specifications, industry standards, regulations, and other reference documents. Where conflict occurs between criteria, the more restrictive will apply. Where no date is shown, the latest issue will apply.

Governing Codes, Specifications, and Industry Standards.

- International Conference of Building Officials, "Uniform Building Code" (UBC).
- American Institute of Steel Construction (AISC), "Specification for the Design, Fabrication, and Erection of Structural Steel for Buildings," 8th Edition, 1978.
- American Welding Society (AWS), "Structural Welding Code", AWS D1.1 - 79; and "Reinforcing Steel Welding Code," AWS D12.1 - 75.
- American Concrete Institute (ACI), "Building Code Requirements for Reinforced Concrete" ACI 318 - 77 and Commentary; and "Code Requirements for Nuclear Safety Related Concrete Structures," - ACI 349 - 76.
- American National Standards Institute (ANSI), "Building Code Requirements for Minimum Design Loads in Buildings and Other Structures," ANSI A58.1.
- American Society for Testing and Materials (ASTM). Applicable standards of ASTM specifications are referred to in Section A.7. Construction Materials.

U.S. Nuclear Regulatory Commission (USNRC) Regulations and Standard Review Plans.

- Code of Federal Regulations (CFR), Title 10 - Atomic Energy, Part 50, "Licensing of Production and Utilization Facilities," including applicable Appendices A through M.

- Code of Federal Regulations (CFR), Title 10 - Atomic Energy, Part 100, "Reactor Site Criteria," including Appendix A thereto, "Seismic and Geologic Criteria for Nuclear Power Plants," adopted November 27, 1973.
- USNRC - Regulatory Guides as follows:
 - 1.59 Design Basis Floods for Nuclear Power Plants
 - 1.60 Design Response Spectra for Seismic Design of Nuclear Power Plants
 - 1.61 Damping Values for Seismic Design of Nuclear Power Plants
 - 1.76 Design Basis Tornado for Nuclear Power Plants
 - 1.92 Combination of Modes and Spatial Components in Seismic Response Analysis
- USNRC - Standard Review Plans
 - 3.8.4 Other Seismic Category I Structures

Site Characteristics. A hypothetical site for the TDF was selected for the purposes of this study. This hypothetical site is not only generally compatible with installation at an established government nuclear reservation, such as Idaho National Engineering Laboratory (INEL), but also is compatible with a significant number of potential U.S. sites. General information about the site is given in Table 3-90.

Seismic Design Classification of Structures. From the standpoint of their importance to safety, the plant structures, systems, and components are classified into three categories as follows:

- Seismic Category I Structure. Seismic Category I structures, systems and components are necessary to ensure the capability of preventing or mitigating the consequences of accidents that could result in potential off-site exposures comparable to the guideline exposures of DOE (see Section 4). These structures shall be designed to remain functional in the event of an earthquake of maximum intensity. The following are Seismic Category I Structures: reactor building (including heat exchanger and radwaste areas), hot cell building, tritium building, diesel-generator building, control building, control tunnel.

Table 3-90. Characteristics of hypothetical site for TDF.

Characteristic	Value assumed
<u>Geology/seismology</u>	
MIE ^a ground acceleration	- 0.25g
MPE ^a ground acceleration	- 0.13g
Ground response spectra	Use Regulatory Guide 1.60 assumptions
UBC ^a Seismic Zone 2	Zone 2
<u>Foundation material</u>	
Net allowable bearing capacity	
Static	500 kPa
Dynamic	700 kPa
Damping	Use Regulatory Guide 1.61 assumptions
"S" wave velocity	1200 m/s
Depth-to-bedrock	At surface
Location of the nearest surface capable of faulting	The site is located more than 8 km from a surface capable of faulting more than 305 m in length.
Snow and ice load	1 kPa
<u>Wind load for safety-related structures</u>	
Velocity at 9 m above ground	145 km/h
<u>Tornado loading</u>	
Region	Use regulatory guide 1.76, Region III, Design Basis Tornado
Characteristics	
Maximum wind speed	386 km/h
Rotational speed	306 km/h
Maximum rotational speed	80 km/h
Radius of maximum rotational speed	46 m
Pressure drop	10 kPa
Rate of pressure drop	4.1 kPa/s

^aAbbreviations: MIE, maximum intensity earthquake; MPE, most probable earthquake; and UBC, Uniform Building Code.

- Non-Seismic Category I Safety Related Structure. These structures are not necessary to ensure the capability of prevent or mitigate the consequences of accidents which could result in potential offsite exposures comparable to the DOE guideline exposures, but whose damage could affect the integrity of adjacent safety related systems, structures, and equipment. The following are the Non-Seismic Category I Safety Related Structures: power supply and cryogenics building, vent stack.
- Non-Seismic Category I Non-Safety Related Structures. These are structures whose damage could not affect the integrity of safety-related systems, structures, and equipment. All plant structures not listed in the previous two categories are in this group. Non-Seismic Category I Non-Safety Related Structures include but are not limited to the following: administration building, plant services building, cooling tower, demineralized-water storage tank, air-compressor station, water-treatment plant, pump house, raw-water storage tank, fire-water storage tank, off-site raw-water supply line, off-site power supply, guard house.

Plant Internal Loads. Dead loads include, first, the weight of all permanent structural elements and, second, the weight of all permanently attached equipment, piping, etc.

Live loads include, first, floor area loads (occupancy and laydown) and, second, loads from moveable equipment or structures.

Internal Pressure Load for the Design Basis Accident. For the design basis accident the internal pressure of the reactor building is 120 kPa. The positive pressure is the result of an accident involving cryogen release and helium pressurization.

Thermal Load. Accident temperatures are unspecified at this time.

Loads Not Included. For simplicity, the following loads are not included in the preconceptual structural design criteria: missiles (other than tornado), normal and accident pipe loads, dynamic earth pressures, and compartment pressurization.

Design Bases. In the following sections, factors of safety, codes, and loading combinations, which will be used for all structures, are established.

Factors of Safety. A minimum factor of safety shall be provided for all structures as shown in Table 3-91.

Seismic Category I Structures. Concrete structures in Seismic Category I are designed in accordance with ACI-349 and supplemented by ACI-318. The steel structures will be designed in accordance with the AISC code. The applicable loading combinations will be as specified in the NRC Standard Review Plan 3.8.4.

Non-Seismic Category I Safety-Related Structures. Reinforced concrete structures in this category are designed in accordance with ACI-318 for concrete structures and will be in accordance with the loading combinations in Chapter 9 of ACI-318. The steel structures are designed in accordance with the AISC code. Wind and seismic loadings are in accordance with ANSI A-58.1 and the Uniform Building Code (UBC), respectively.

In addition to the above loading combinations, Non-Seismic Category I Safety Related structures are checked and strengthened as required to assure they do not collapse on a Category I structure or system during a design basis tornado or maximum intensity earthquake (MIE) event.

For concrete structures under these conditions, the load factor for all loads shall be 1.0. For steel structures the allowable stresses shall be increased by 60% and the plastic section modulus of steel shapes shall be used. For these load combinations, only the main structural members are considered. The design criteria allows these structures to be damaged during these events; for example, permanent deformation of the structures may result from these extreme loadings.

Non-Seismic Category I Non-Safety Related Structures. These structures will be designed in accordance with ACI-318 for concrete structures. Steel structures will be designed in accordance with the AISC code, and wind and seismic loadings will be in accordance with ANSI A-58.1 and the UBC, respectively.

Table 3-91. Safety factors assumed for various loading conditions.

<u>Loading conditions</u>	<u>Minimum factors of safety</u>		
	Overturning	Sliding	Flotation
MPE ^{a,b}	1.5	1.5	--
Wind	1.5	1.5	--
MIE ^a	1.1	1.1	--
Tornado ^a	1.1	1.1	--

^a Loading condition does not apply to Non-Seismic category I non-Safety Related structures.

^b Load condition does not apply to non-Seismic I Safety Related structures.

Construction Materials. The principal construction materials for Seismic Category I and Non-Seismic Category I structures are concrete, reinforcing steel, structural steel, masonry, and metal decking and are noted below:

- Concrete design compressive strength are for all plant structures is 27 MPa, and for lean concrete backfill and for electrical duct encasement is 17 MPa. (Compressive strength (f_c') refers to compressive strength at 28 days if only cement is used and to compressive strength at 90 days if pozzolan replaces part of the cement.
- Reinforcing steel shall be deformed billet steel, conforming to ASTM Designation A-615 grade 60.
- Welded steel-wire fabric shall conform to ASTM A-185 (plain wire) or A-497 (deformed wire).
- All structural steel shall be ASTM A-36.
- Fasteners shall conform to ASTM A-307. High-strength bolts shall conform to ASTM A-325 or A-490.
- Anchor bolts shall conform to ASTM A-307, A-36, or A-449.
- Unless otherwise required by special conditions, such as the control room penetration areas, metal decking shall be used and considered in the design for forming concrete slabs.

AC Power. The ac power system must perform the following functions:

- Provide ample power for all electrical loads to TDF, such as those identified in Tables 3-91 and 3-92.
- Provide enough independent power to the facility to allow maintenance and test work if the main power system fails.
- Provide standby backup power to essential loads if all utility power is lost.
- Provide coordinated fault protection to the feeders at all voltage levels.

Electrical equipment will be designed for continuous operation with limited low-cost redundancy to improve the system availability. Based on the data of Tables 3-91 and 3-92, the ac power lines and backup power units will require the capacities and voltage regulations given in Table 3-93.

Table 3-92. Estimate of facility power loads.

Load description	Power	
	kW	kV·A
Building and ground lighting	500	500
HVAC ^a for all buildings	1500	3000
Fuel reprocessing	5000	6000
Shop and service equipment	2500	3000
Laboratory and office equipment	900	1000
Diagnostics	1500	1800
Data acquisition	90	100
Process C&I ^b	90	100
Emergency tritium removal ^{b,c} system (1)	1300	1400
system (2)	1300	1400
Emergency cooling ^b	500	600
Essential lights ^b	50	50
Uninterruptible power ^b	40	50
Backup power for testing and maintenance	8000	10000

^a Heating, ventilating, air conditioning.

^b Transferable to diesel generator plant.

^c Only one of the two systems needed for cleanup.

Table 3-93. Capacity requirements for ac power system.

Description	Power
<u>Two independent utility lines</u>	
Heavy-duty power line	400 MV·A, 230 kV
Light-duty facility power line	50 MV·A, 69 kV
<u>Heavy-duty line to provide</u>	
Voltage regulation	±10% daily variation
Power to major loads	300 MV·A, 13.8 kV
Backup power to facility line	20 MV·A, 13.8 kV
<u>Light-duty line to provide</u>	
Voltage regulation	±5% daily variation
	±1% normal transient
Facility power	15 MV·A, 13.8 kV
Backup power to heavy-duty line loads	15 MV·A, 13.8 kV
<u>Two standby diesel generators to provide</u>	
Backup power to essential loads	2500 kV·A, 2.4 kV
<u>Four noninterruptible power supplies to provide</u>	
Power to noninterruptible loads.	40 KV·A

3.12.3. Design Description

The systems, buildings, and structures of the plant facilities are designed with the plant operation and functional requirements in mind in order to achieve the most desirable relationship between the systems and components and to provide building locations, shapes, and sizes that are consistent with the space required by their functions and contained equipment. Arrangement of the overall site facilities is shown on the TDF Plot Plan (Fig. 3-211). All of the facilities, except the river water pump house and possibly an operator training facility, are enclosed within the exclusion area boundary fence. However, the administration, control, and security buildings are located just outside the radiation safety zone. The exclusion area is set at a radius of 500 m for public safety reasons (see Section 4). Brief design descriptions of the major individual systems, buildings, and structures are given in the following subsections.

Site Facilities. The following paragraphs and Fig. 3-211 pertain to the site facilities provided for the TDF plant.

Design Basis. Although the TDF plant is designed for a wide range of potential sites throughout the continental United States, the following assumptions are made for the purpose of this study:

- Location: A site with characteristics equivalent to the Idaho National Engineering Laboratory (INEL) site.
- Seismic zone: UBC Zone 2
- Tornado zone: Zone 3
- Exclusion Area Boundary: 500-m radius
- Water Supply: Assumed adequate to meet all normal requirements
- Electric Utility: Assumed high-voltage supply (>160 kV) available

Design Description. Principal site elements include buildings, structures, access roads, on-site roadways, security fencing, and surface and subsurface mechanical and electrical equipment and utilities. The mechanical draft cooling tower is located at the rear of the site consistent with the

Figure 3-211. Plot plan for TDF facilities.

prevalent wind direction of the site. All the site elements are located and arranged on the basis of safety, functional and operational flexibility, constructability, and economic considerations.

Administration, Control, and Security Buildings. All traffic to and from the site is controlled at the security building located just outside the exclusion area fence. All administrative and plant control personnel are housed in this building complex. Visitors are allowed to observe the operation of the reactor through closed-circuit TV from the control building. This arrangement restricts non-maintenance personnel and visitors to areas outside the radiation safety exclusion area.

Experimental Complex. The heart of the TDF plant is located in the building complex referred to as the "experimental complex" for convenience. This complex consists of the reactor building, (including heat exchanger and radwaste areas), hot cell building, tritium-processing building, and power supply and cryogenics building, including the diesel generator area.

Switchyard. The switchyard is a fenced area that contains all the electrical equipment necessary to accommodate incoming high-voltage power. It is located close to the power supply and cryogenics building and the transformer yard.

Other Site Facilities. Other major facilities provided include a river-water-intake pump house (which supplies all normal plant water requirements), raw water reservoir, raw water treatment equipment, fire water storage tank, and plant fire protection equipment.

Mechanical Systems.

Heat Transport System. The heat transport system (HTS) provides the equipment and facilities for supplying high-purity demineralized and deionized cooling water to remove heat from the reactor components and other auxiliary components of the plant during normal operation. The components served by the system and the corresponding heat loads are indicated in Table 3-94.

Table 3-94. Major plant cooling loads.

Systems and components:	Cooling load, MW
Neutral dump	16
Neutral-beam internal	65
End-cell dump	44
Subtotal	<u>125</u>
Reactor components:	
Copper coils	52
Test module 28	
Shield	4
Vacuum vessel	4
ECRH 5	
Fuel processing and cleanup	4
Subtotal	<u>97</u>
Facilities and other systems:	
Cryogenics system	12
Power conversion	16
Coolant pumping	--
HVAC and miscellaneous	8
Subtotal	<u>36</u>
Total	<u>258</u>

A schematic diagram of the heat-transport system is shown in Fig. 3-212. As seen in this figure, the HTS consists of three primary heat transport loops and a heat rejection loop. The heat rejection loop disposes of the heat to the atmosphere through a mechanical draft wet cooling tower. The reason for separating the primary heat transport into three loops is twofold. First, this separates the components operating in a tritium and neutron environment from those not operating in a similar environment. As seen in Fig. 3-212, the components in the reactor cooling and power absorption cooling loops operate in a tritium and neutron environment. Secondly, the components in the power absorption cooling loop require high coolant pressure to avoid boil-off of the coolant which may be caused by high-surface heat load. The peak coolant pressure in the power absorption cooling loop is 2.4 MPa while that in the reactor and facility cooling loops is 1.0 MPa. For all the three primary cooling loops, the hot and cold leg temperatures are 60 and 43 °C, respectively. The hot and cold leg temperatures in the heat rejection loop are 45 and 28 °C, respectively.

Emergency Cooling Water System. This system provides cooling water primarily to the atmospheric tritium cleanup units (ATCU), solid waste system, diesel generators, and control building heating, ventilating, and air conditioning (HVAC) during abnormal operating conditions. Operation of the system occurs, for example, during an incident when the reactor has been shut down and there is a large tritium leakage; the off-site power is unavailable, and the plant is using an on-site power source (diesel generator). The ATCUs are the major components requiring emergency cooling water, and it is assumed that no more than one of the ATCUs will be operating at full capacity during the abnormal incident. The largest ATCU cooling water load occurs when the reactor building ATCU is in the emergency operating mode. A schematic diagram of the emergency cooling water system is shown in Fig. 3-213. The system is a closed cooling loop with a forced-draft water-to-air heat exchanger for ejecting the heat to the atmosphere.

Activated-Structures Handling System. The periodic replacement (as a result of neutron activation) of the reactor components (viz., beam dumps, choke coil structure, test-module first wall, etc.) generate activated, solid,

Figure 3-212. Heat transport system.

3-549

Figure 3-213. Emergency-cooling water system.

radioactive wastes. A conceptual scheme for handling and disposing of these wastes is discussed here.

To put the magnitude (mass) and characteristics (activity and afterheat) of the wastes in perspective, the activated-structures' radioactive waste for three fusion reactor concepts are listed in Table 3-95. The information was obtained from Refs. 49 and 50. Similar information for the TDF reactor is not available at the present stage of design development; however, it is anticipated that the mass and afterheat of the TDF wastes will be much smaller, because TDF operates at a much lower thermal power level and does not have any blanket. Also there may be significant differences in waste characteristics, depending on the choice of structural materials and component design.

The processing and disposal of activated structures is strongly affected by the initial level and time-dependent behavior of induced activity and nuclear afterheat. The initial levels of activity and afterheat dictate the shielding and cooling requirements, respectively. The decay characteristics determine the storage required to reduce the levels of activity and afterheat prior to processing and off-site disposal. To determine the shielding, cooling, and storage requirements for the irradiated structures, it is necessary to generate decay curves for activity and afterheat. To date, adequate information has not been developed to generate these curves. Based on preliminary information,⁴⁹ order-of-magnitude decay characteristic curves were generated (Figs. 3-214 and 3-215) for the helium-cooled tandem-mirror hybrid reactor (TMHR-He). Similar curves for UWMAK-III reactor⁵⁰ are also included to provide some perspective on the decay characteristics of several structural materials. Similar decay characteristics are expected for TDF although the initial level of activity will be much less because of much lower neutron fluence.

Figure 3-216 is a schematic of a typical on-site process for the removal and preparatory work required for off-site disposal of activated structural materials. After the component requiring replacement is removed from the reactor, it is transferred to the hot cell where decontamination and disassembly operations are performed. Here the reusable (if any) portion of the component is separated from the nonreusable portion and held for assembly

Table 3-95. Amount and characteristics of radioactive waste for three reactor types. (Data obtained from Refs. 56 and 57.)^a

Reactor	Source	Constituent materials	Annual radwaste (10 ³ kg)	Specific activity ^b at shutdown (Ci/GM)	Total activity ^b at shutdown (Ci)	Specific afterheat ^b at shutdown (W _T /GM)	Total afterheat ^b at shutdown (V _T)
UNMAK-III (2000 MWE)	First wall Blanket	TZM	82	24	2×10^9	0.1	8×10^6
TMHR Helium cooled (400 MW)	First wall Blanket	Inconel 718 HT-9	12.6	80	1×10^9	0.3	4×10^6
TMHR molten-salt cooled (4000 MW)	Blanket	TZM	7.1	53	3.8×10^8	0.2	1.5×10^6

^aFor TDF, mass and afterheat are expected to be much less than figures shown here.

^bAfter one year of operation.

^cTMHR is Tandem Mirror Hybrid Reactor.

Figure 3-214. Activity as a function of time after reactor shutdown.

Figure 3-215. Decay heat vs time after reactor shutdown.

Figure 3-216. Processing of activated structure wastes.

Figure 3-206. Contaminated air distribution system for TDF.

cooling water at significant rates because of implantation energy and the relatively high temperature of the beam dump material. It has been estimated that for a full-power year of operation about 21 g of tritium will enter the cooling water through the neutral-beam dumps and about 98 g of tritium will enter through the plasma end dump.

Because leaks in the beam dump cooling system are virtually impossible to prevent, we must reduce the tritium in the coolant to a tolerable concentration. Experience with nuclear reactors and other processes using tritiated water indicates that a tolerable level of tritium coolant is about 1 Ci/liters or less. It can be shown that to maintain the tritium concentration in the beam dump cooling water below about 1 Ci/liter, tritiated water must be removed at a rate equivalent to the curies per liter of tritium entering the cooling water. Because the fusion device will only be operated with a maximum availability of about 50% full power per year, the TWR system should have a process capacity of about 1600 liters/d. With the TWR system in operation, about 50 days of full-power operation by the fusion device would be required before the tritium concentration in the cooling water reaches 0.9 Ci/liter.

The TWR system is shown schematically in Fig. 3-207. The process involves combined electrolysis and catalytic exchange technology. The tritiated water is injected into the middle of the exchange column that is filled with a catalyst material to promote the exchange of gaseous hydrogen isotopes with water. Because the equilibrium at the column operating temperature is near ambient, formation of tritiated water (HTO) is favored, and enriched HTO concentrates in the bottom of exchange column. A portion of this enriched HTO is removed and electrolyzed to form hydrogen isotopes and oxygen. Most of the electrolyzed hydrogen isotopes are returned to the bottom of the exchange column to further enrich the water in tritium, while the remaining portion of the gas is directed to a cryogenic distillation column for additional isotopic enrichment. The overheads from the distillation column are returned to the top (stripping portion) of the column for exchange with the tritium-depleted water and stripping of any remaining tritium from the gas. The tritium-free hydrogen gas, mixed with oxygen from the electrolyzer, then exits through the top of the exchange column, recombine to form water, and returns to the cooling-water system.

with new components. The assembled components pass through the decontamination process to remove loose contamination that may otherwise spread in the reactor building.

The need to store activated structures for decay heat removal depends on the waste shipping requirements, which include limiting the shipping container heat load, geometry of cask, Department of Transportation (DOT) packaging requirements, etc. The next step in the processing of activated structure, after it has decayed to the required level, is compacting for disposal. The compaction process generally involves cutting the structure into small pieces followed by separating the recyclable parts. The recyclable parts are shipped off-site for further processing, if needed. The unrecyclable parts are either pressed to flatten or are melted in a furnace if the pieces are highly irregular in shape. Finally, the compacted waste is loaded in canisters for shipment to the disposal site.

The hot cell facilities needed for the activated structures handling cannot be accurately determined until more is known about the overall process requirements. Most of the remote handling equipment needed is identified and discussed in Section 3.16, Maintenance. The equipment needed for the compaction process and subsequent preparation for off-site shipment includes:

- Hydraulic/pneumatic press for flattening.
- Furnace to melt the irregularly shaped pieces.
- Molds to cast the molten material into desired shapes.
- Remote manipulators to facilitate above operations and to load the compacted pieces into canisters.

For the process described above for handling activated structures and with the hot cell facilities provided for the purpose we assume that the largest component to be handled is the central cell magnet. If, in an unlikely event an activated component larger than the central cell magnet requires replacement and or disposal, special facilities will be required and will be provided when the need arises. However, long-term storage facilities for radioactive decay of these components are provided in the hot cell building as well as in the reactor building.

Radioactive-Waste Handling System. The radioactive waste handling system, or the radwaste system as it is commonly called, provides equipment

and facilities for collecting, storing, processing, monitoring, and disposing of liquid, solid, and gaseous radioactive wastes. No attempt has yet been made to quantify the amount of radwastes generated and, as a result, no estimate of total amount of radioactivity associated with these wastes is available. Thus only a scheme for handling radioactive wastes and the nature of the equipment and facilities needed are outlined here.

Liquid wastes originate from demineralizer wastes, valve and pump seal leaks, laboratory processing fluids, equipment drains, shower and laundry drains, decontamination and washdown operations. Gaseous wastes originate mainly from dissolved gases in the coolant, radiolytic decomposition of water, gases added to the plant such as helium, nitrogen, sulfur hexafluoride, and vent air circulated through storage tanks. Solid wastes arise from a variety of miscellaneous sources. *The three major sources are the spent resin from the various demineralizers, the used filter cartridges, and the prepared radioactive concrete.* In addition, there are such things as blotters, worn clothes, shoe covers, gloves, broken tools, decontamination materials, worn-out equipment, and other miscellaneous solid items.

A schematic diagram of the radwaste system is shown in Fig. 3-217. Gaseous wastes can be processed on a batch basis, rather than on a continuous basis, because of the anticipated small quantity of gases involved. Waste gases are collected, compressed, and stored in decay tanks for 30 to 45 days before their controlled release to the atmosphere. Radiation monitors measure gas activity before any release is made from the plant. When the gas in the decay tank reaches the desired level of activity, it is passed through high-efficiency filters. The gas is then diluted in the stack for control of *specific activity*.

Liquid waste handling has two goals: first, to release innocuous wastes to the environment after suitable monitoring and, second, to reduce the volume of waste by separating out the pure water and converting the concentrated active wastes in to a stable form for long-term storage before release from the facility. Wastes are classified as "clean" or "dirty", according to the chemical content of the liquid. The clean wastes have a much lower total dissolved and suspended solid than the dirty wastes. Wastes originating in the primary system are clean, but wastes originating in the showers or decontamination process are dirty. Liquid wastes are collected in holdup tanks

Figure 3-217. Radioactive waste handling system.

for batch processing (because of anticipated low waste volumes). Chemically clean wastes are treated for pH adjustment, while the dirty wastes are first passed through filters before they are mixed with the clean liquid stream. The mixed stream is then treated by evaporation and demineralization to separate out the pure water and to reduce the volume of radioactive liquid. The active concentrate, along with the solid radwastes from the plant, is then mixed with concrete in drums and shipped off-site for disposal. To meet DOT shipping requirements, the activity of the wastes is monitored before shipping off-site.

Other Mechanical Systems. In addition to the major systems described above, a large number of other mechanical systems are needed to support the operation of the device. Although these systems are not described in this report, the cost of providing these systems is included in the plant cost. Some of these systems are:

- Demineralized water system
- Service water system
- Potable water system
- Raw water system
- Fire water system
- Fuel oil system
- HVAC (heating, ventilating, and air conditioning) system
- Compressed air system
- Cover gas (inert) supply system
- Helium and nitrogen gas supply system
- SF6 supply system
- Radiation monitoring system

A facility interface diagram (Fig. 3-218) shows all the mechanical systems and their interfaces with the reactor and with its auxiliary systems.

Buildings and Structures.

Building Arrangement. The general arrangement of the major buildings and structures are presented in Figs. 3-219 through 3-226.

LEGEND

- LIQUID
 --- GAS
 --- A.C. POWER

3-559

Figure 3-218. Interface of facility systems.

Figure 3-219. Reactor and support buildings (plan below el. 130 m).

Figure 3-220. Reactor and support buildings (plan below ± 1.113 m).

3-562

Figure 3-221. Reactor and support buildings (plan below el. 106 m).

Figure 3-222. Reactor and support buildings (plan below el. 98 m).

Figure 3-223. Reactor building (el. 100 m) in more detail (Section A-A from Fig. 3-219).

Figure 3-224. Reactor complex (Section B-B from Fig. 3-219).

Figure 3-225. Cryogenics building of reactor complex (Section C-C from Fig. 3-219).

Figure 3-226. Tritium and hot-cell buildings (Section D-D from Fig. 3-219).

The structural design approach encompasses functional arrangement, safety considerations, and construction economies. In addition, the construction and circulation plan of the building envelope follows appropriate building code standards and industry practices. All major buildings are required to be developed in accordance with the load combinations, allowable stresses, and safety factors specified by DOE and/or NRC building design criteria. The reactor building pressure boundary design is in accordance with Section III of the ASME Boiler and Pressure Vessel Code. Other building designs, in general, follow the codes and standards of the American Concrete Institute (ACI) and the American Institute of Steel Construction (AISC).

Reactor Building. The major requirements of the reactor building are outlined in Section 3.12.2. The inside dimensions of the building are 80 m long, 28 m wide and 36 m high. The building has a central pit that is 28 m long, 26 m wide, and 10 m deep. The dimensions of the building are determined on the following basis:

- The building length is set by the overall length of the device and allowing for maintenance access from the ends, for lay down space, and for the transport tunnel that connects the reactor building with the hot cell building.
- The building width is set by the overall width of the device, and allowing for maintenance of the central cell neutral-beam injectors. This maintenance allowance has a major economic impact on the reactor building. Thus, much effort was placed to keep the building width to a minimum. However, detailed analyses of the maintenance requirements may necessitate a wider building.
- The height of the building is established by the device end cell diameter, end cell support requirement from the floor, the device disassembly and maintenance requirements, crane bridge and trolley size, and the size of the trusses needed to support the static and dynamic loading of the building roof. A 200 metric-ton crane is used to handle the heaviest single component, the central cell magnet, which weighs approximately 170 metric tons. The overall height needed for the crane, including hook sling height, is 7 m. The depth of trusses is estimated to be approximately 5 m.

- The central pit is needed to accommodate the central cell neutral-beam injectors and their maintenance.
- The thickness (2 m) of the floor and ceiling is needed to provide shielding against neutron and gamma radiation during operation and maintenance. No detailed analyses were made to identify more accurate shielding requirements. These analyses, if made, are expected to identify less stringent requirements.

Several other considerations that have affected the design of the reactor building stem from the safety and maintenance functions of this building.

- Protection from a seismic event requires a 3 m thick basemat to prevent overturning and vertical displacement of the building. Tornado and missile protection is also considered in the building design.
- The overpressure requirement (120 kPa overpressure from cryogen release) leads to a consideration for stiffening the corners of the building and providing stiffening ribs to the side walls.
- Reducing the leakage of radioactive gases, primarily tritium, from the reactor building leads to considerations for making the building leaktight. Since the primary source of leakage is penetrations, it has been assumed that the building can be designed for a leak rate of less than 10% per day (by volume) by providing adequate seals at the penetrations. Leak rates of light-water reactor buildings range from 0.1% to 1% per day.
- To provide for maintenance of the overhead crane and some of the remote manipulators, an extension of the reactor building is provided at the north end of the building.
- A local operating and viewing gallery is provided along the north wall of the reactor building to oversee the maintenance operation of the reactor.
- Access to the reactor building is provided through the heat exchanger area and through the radwaste area, which are located below the reactor building.
- The ventilation system provided for the building maintains a subatmospheric pressure (~ 0.5 Torr below atmospheric) to prevent out-leakage from the building. The ventilation system also maintains pressure differentials with the adjacent buildings to prevent any in-leakage of contamination from these buildings.

- All internal surfaces (floor, walls, and ceiling) of the building are coated with epoxy paint to minimize permeation of tritium into concrete. While very little experimental data is available regarding effectiveness of epoxy paint, Tritium Systems Test Assembly (TSTA) at Los Alamos is using this type of paint in its buildings. A test conducted at Los Alamos where an epoxy coated surface was exposed to highly concentrated (much higher than expected in a reactor building), elemental tritium gas for several days, showed that the paint is highly impervious to tritium gas and is easy to decontaminate. The major advantages of an epoxy paint over a steel liner are that it is inexpensive and does not enhance formation of oxides. Stainless steel works as a catalyst to enhance the formation of oxides making it difficult to decontaminate the surface.
- A transport tunnel is provided at the reactor building floor level to transport components for maintenance to and from the hot cell building. This tunnel also serves as an airlock between the two buildings and provides shielded storage area for the remote manipulators during reactor operation. For better sealing, all transfer operations in and out of the tunnel are made through hatches provided in the roof slab of the tunnel.

Hot Cell Building. The basic function of the hot cell facility is to support the maintenance of TDF. Major components are removed from the device and taken into the hot cell facility for repair and adjustment. The hot cells must provide adequate working space and a controlled environment to support the maintenance of the major components.

The TDF is expected to have an availability on the order of 25 to 50%. At this level of availability, many maintenance activities may be performed in the reactor building without significantly impacting availability. Very quick repair of failed components is not expected to be as important as it might be for power reactors with goals of higher availability. Thus, general purpose, as opposed to highly specialized, hot cells are currently being suggested for the hot cell facility. Large areas are provided to allow disassembly of large components while smaller cells provide repair space for smaller components and

disassembled pieces. In addition to general purpose areas, the following are required: an anechoic chamber (without echoes) to test and adjust ECRH components; a decontamination area to allow removal of surface contamination from components to be repaired; a bakeout area for outgassing the components before assembly; a test chamber for neutral-beam injectors; a laboratory for examining the test modules; a long-term storage area for radioactive decay; and a packaging room to prepare for final off-site disposal.

The design of the hot cell is determined by the requirements and by applying engineering judgment to estimate an appropriate configuration. While the identification of specific maintenance requirements is still in an embryonic stage, the general philosophies of maintenance procedure will be incorporated in the hot cell building design.

A number of design options are considered. One option is to use a number of special purpose cells within the hot cell building. Special purpose cells would be more responsive to quick repair times needed to support high availability. Each special purpose cell could be equipped to handle and repair only one component type. While repair time could be greatly decreased with special purpose cells, the cost of building and equipping such cells would greatly increase the cost of the hot cell facility.

At the other extreme, another design option is the use of the reactor building for maintenance so that no hot cell facility would be needed. While this would eliminate the need for one facility, it would significantly increase the size and cost of the reactor building. The repair and maintenance in the reactor building of all radioactive components would greatly increase repair times because of increased restraints on working conditions and on maintenance equipment, which in turn would decrease plant availability.

The selected design is a compromise between the two extremes. A hot cell facility is provided with general purpose areas for maintaining the major radioactive components. Some component maintenance within the reactor building is still expected. The compromise allows a reasonable chance of attaining the desired availability without greatly expanding the cost of the plant. Several unique features of the hot cell building are described in the following paragraphs.

A decontamination cell equipped to measure radioactivity and contamination, is positioned so that all contaminated components moving in and out of the hot cell must pass through a control station where each item can be checked for contamination and cleaned if necessary. The purpose of this is to prevent contamination from passing between the hot cell and other buildings.

The maintenance of the hot cell facility influences the hot cell configuration. Two types of work stations in the hot cell area are considered. One provides several individual cells to allow personnel entry for hands-on or glovebox procedures. The individual cells are shielded and isolated from each other so that radioactive component maintenance could continue in adjacent cells while other cell maintenance is being performed. A cell must be decontaminated before personnel entry. The most desirable feature of this approach is that hands-on (or bubble-suit) maintenance is perceived to be faster and more efficient than the alternative approach.

The alternative is to have all the work stations in one large area and perform all the cell maintenance operations remotely. Tools and equipment would be removed, decontaminated, and repaired in a separate glovebox type cell, but no personnel entry would occur in the hot cell area. The advantages of this approach are that the work stations do not need to be decontaminated for maintenance operations, and the reduced shielding requirements may reduce the cost and complexity of the facility.

The first approach has many disadvantages. Decontamination of hot cells for personnel entry is very difficult and time-consuming. Thus, the advantage of personnel entry is largely negated. Movement of cranes and components through contaminated areas after decontamination would likely lead to recontamination of the component and the work stations, even though they are isolated most of the time.

Totally remote maintenance of hot cells has been demonstrated in a number of facilities. Thus, while not easy, the use of the non-entry cell is feasible and is the approach selected for TDF. Unfortunately, the size of the TDF hot cell and of the components to be repaired are substantially larger than most of those already using totally remote hot cell maintenance. Thus, the hot cell for TDF represents a significant extrapolation of current technology.

Maintenance of the overhead crane is of particular concern and is likely to be a major difficulty in any totally remote design. However, the crane is high enough above the sources of contamination to allow glovebox type maintenance of the components. A shielded area is provided for this purpose.

The ventilation system plays a key role in the control of contamination in hot cells. The most successful flow pattern appears to be one in which the fresh air enters near the top of the cell and flows out through gratings in the floor. Each work station has a section of the outlet grating within its boundary. This flow pattern tends to pin the contamination to the floor and sweep it to the filters beneath the floor without contaminating other areas of the hot cell.

The use of an inert gas atmosphere for the hot cell area has been suggested by some. The advantages of an inert gas atmosphere are that fire hazards are greatly reduced, and the formation of ozone is prevented. Ozone is formed from the oxygen in the air by high levels of radiation. Ozone is highly corrosive to metals and attacks many of the plastics that are desirable for use in hot cells. Additional advantages of an inert atmosphere include the possibility of performing cutting and welding operations without special inert gas sources and the possible easier removal of any tritium released to the hot cell atmosphere. These advantages tend to offset the disadvantages of additional cost and complexity of maintaining an inert gas atmosphere. However, as long as a non-entry hot cell area is employed, the use of an inert gas has little influence on the hot cell design and the impact on overall cost is negligible.

Tritium Building. The tritium facility based on TSTA prototype is a full scale processing plant with high reliability. It is designed to be completely separate from the reactor building, but it is located nearby to minimize the length of the tritium feedlines, thus reducing the tritium-accident potential. The building is designed for contact maintenance; it has an air environment and is free of gamma and neutron radiation. All tritium-handling units are under remote, independent, hard-wire, computer control; the computer is equipped with an uninterruptible power supply to ensure that all tritium systems can be shut down in the event of a power failure. The tritium storage area is in the tritium facility and is a shielded, barricaded vault holding an

inert cover gas for fire protection. Because of space and safety benefits, the majority of the tritium is stored as UTX, but some low-pressure gaseous storage in tanks is also provided.

The tritium building is 30 by 50 by 16 m high; within it a slightly negative pressure is maintained by a self-contained heating, ventilation, air conditioning system (HVAC) interfaced to the atmospheric tritium cleanup (ATC) system to reduce tritium out-leakage.

Two personnel airlocks go into the tritium building; the one designed for routine use connects the health physics area with the lower level of the building. The other is intended for emergency use. Within the tritium building, space is provided for the ATC units and the interfaced HVAC system. The placement of all units is designed to provide good accessibility. The tritiated waste recovery unit, the tritiated waste treatment unit, and the solid waste disposal unit all of which generate and/or process tritiated waste, are located for easy waste removal. The isotope separation units are high; the 4-m-high vacuum shell around each unit is removed vertically and downward. (Space is provided in the basement but not shown.) Storage space is provided for nontritiated gaseous deuterium and hydrogen as well as for helium and nitrogen. Within the tritium building are three service areas. The first is an analytical laboratory for doing routine analyses on catalysts, molecular sieves, etc; the second is an electronics laboratory in which nonactivated tritium monitors are serviced and tested; the third is a maintenance area where tritium units are repaired.

In the tritium building, which is designed for contact maintenance, certain procedures are followed to reduce worker contact with tritium. Atmospheric levels are maintained at $<5 \mu\text{Ci}/\text{m}_3$. Normal fire prevention practices for hydrogen are exercised.

Heat Exchanger and Radwaste Areas. The area directly beneath the TDF reactor floor is devoted to the reactor support systems. These support systems include the heat transport system, radioactive waste handling system, and facilities for access control to the reactor building. For the location of equipment containing radioactive fluids, full advantage is taken of the proximity of the area to the reactor.

All penetrations and openings between the reactor building and the areas below it are equipped with seals to prevent diffusion of tritium and activated air. As a further precaution, the HVAC system maintains varied negative-pressure differentials between these areas and other adjacent buildings. A separate partitioned room in the radwaste handling area contains a safety check area for personnel. Tritium proof sealed doors and airlocks permit access to the reactor building from this area.

The heat exchanger area contains equipment for cooling various reactor components and auxiliary facilities. This equipment includes circulating pumps, heat exchangers, surge tanks, water chillers, and treatment equipment. Since the cooling water in these components may become radioactive after circulating through the reactor during operation, closed loop cooling systems minimize environmental and personnel hazards by restricting activated flow to the heat exchanger area. Activation is shortlived, and access to this area is prohibited during reactor operation. Some of the components in this area may require local shielding where more intense and persistent radiation exists. This local shielding will be put in place at a later date after actual measurements of radioactivity have been taken.

The radioactive waste area mainly houses the radwaste system components of the three waste-processing facilities:

- Liquid waste processing facility
 - Decanting equipment
 - Collection tanks
 - Neutralizer
 - Evaporator and demineralizer
 - Filters, pumps, radiation monitoring equipment, etc.
- Solid waste processing facility
 - Cement filling station
 - Drum processing station
 - Drum storage area
 - Radiation monitoring equipment
- Gaseous waste processing facility
 - Collection tank
 - Compressor and dryer
 - Decay tank
 - Particulate filter
 - Radiation monitoring equipment

No equipment arrangement has been developed for the radioactive waste area, but the space provided is considered adequate. Because the radioactive materials are handled by the components, the radwaste area is located in an adequately shielded building. Because this area (also the heat exchanger area) is located in the basement of the reactor building, it is provided with 2-m thick walls, floor, and roof to conform to structural design requirements. To provide as low as reasonably achievable (ALARA) radiation protection, equipment will be arranged in a manner that isolates the operating and maintenance areas from the processing equipment.

The heat exchanger area is 28 m by 22 m by 6 m high. The radwaste area, including the personnel access control area, is 28 m by 28 m by 6 m high.

Power Supply Building. The power supply building contains the equipment for ECRH power conversion and supply, Nbl power conversion and supply, and magnet power conversion and supply. No detailed equipment arrangement has been developed for this building; only general areas are identified in the layout drawings. The building has two floors. The first floor contains the ECRH power generators (gyrotrons), the compensation and transient suppression networks and modulator/regulator modules which supply high-voltage dc power for the neutral-beam ion sources. The second floor contains the magnet power conversion and supplies, consisting of rectifiers and switches, local controls and interface to central controls, and HVAC for the power supply building. All transformers, capacitors, and auxiliary substation equipment are located outdoors.

Because of high electrical potential and the possibility of arcing relative to the transient suppression networks, each unit is housed in a separate enclosure on the first floor. Each of the enclosures has a grounded metal lining to further protect equipment and personnel.

The power supply building measures 80 m by 16 m by 14 m high. The building has a wall common with the reactor building, which helps to reduce the length of the high-voltage dc power cables to the ion source supplies.

Cryogenics Building. This building is the vertical extension of the power supply building and consists of one floor only, measuring 80 m by 16 m by 7 m high. The helium and nitrogen cryogenic supply equipment is located in

this building. No detailed equipment arrangement has been developed for this building; only general areas are identified in the layout drawings. These areas are the helium compressor and oil removal unit area; nitrogen compressor and oil removal unit area; storage dewar, cold box, and other equipment area; and control console area. The high-pressure gaseous helium and nitrogen storage tanks are located outdoors for convenience of off-site supply of these gases.

Control Building. The central control building is remotely located from the experimental complex. The computers, used both for the TDF systems and for handling experimental data, are housed in the same building.

The control building is outside the radiation safety exclusion area. The TDF device is remotely controlled, and personnel are subjected only to the lower radiation dose outside the exclusion area. The extra path length of signal carriers is not significant because all control and experimental data acquisition signals are transmitted through optical data links. A spinal cord concept carries all services to and from the TDF, and the data links are carried in a utility tunnel. Safety-related signals and interlocks are carried in electrical cables, which are also routed through the utility tunnel.

A gallery is envisaged above the control building in anticipation of the many visitors expected at TDF. Visitors, who will not need to wear film badges, may observe central control room operations without disturbance and can see inside the experimental area on closed-circuit TV.

Structural Design. A preconceptual structural design is presented here on the most important TDF buildings and structures. For a few key areas of the TDF structures, a very limited structural analysis is performed in accordance with the structural criteria discussed in Section 3.12.2. Possible alternate designs for some key areas and recommended future studies are also discussed.

Reactor Building Complex. The reactor building (including heat exchanger and radwaste areas), hot cell building, and tritium building have been combined into one large complex sharing a common basemat. For the most part, these buildings are massive reinforced-concrete structures that use structural

steel members only to support concrete weight during forming, thereby eliminating expensive and time-consuming shearing and form removal operations. The most significant exceptions to this are the roofs of the reactor and hot cell buildings, which use steel trusses to carry both dead weight and seismic loads. Concrete roof and floor slabs are designed to act as a diaphragm to transfer lateral loads to the concrete walls that are designed as shear-resisting elements. The foundation mat consists of slabs on an elastic foundation. The thickness of walls and slabs surrounding radioactive areas is generally governed by shielding requirements.

The most significant structural features of the complex are the 2-m-thick walls, floors, and roofs required for shielding in the reactor building and hot cell. These, combined with the large distances over which these structural elements must span, constitute a very significant cost factor. The large thickness required for shielding increases structural mass, which results in high gravity and seismic loads.

Areas of high sensitivity to these loads are the roofs of the reactor building and of the hot cell, which have large clear spans. The design selected is a deep steel truss system with rolled beams for roof purline supporting a steel deck. This design is selected because of its relative ease of construction: First, the trusses are placed on the walls. Then the secondary framing and steel decking are erected on the trusses. Next, the reinforced-concrete roof slab is placed.

The steel truss concept is selected over the following two alternate methods:

The first uses post tensioned T-beams with a reinforced concrete slab placed on top. The main disadvantage is the large crane capacity needed and the time required to lift the heavy T-beams into position. This lifting problem is most critical on the high reactor building as opposed to the hot cell roof. Even with this disadvantage, the post-tensioned T-beam concept is the more attractive of the two alternate concepts considered because of its low fabrication cost. A future cost-comparison study (T-beam vs steel truss) is recommended.

The second alternate concept is the barrel vault (arch-shaped roof). Because the large concrete thickness is required for shielding, not for strength, its thickness must be held constant. Thus the usual economies of a barrel vault cannot be utilized. Also, because of curvature of the vault,

more concrete is required, compared to the flat roof system. Moreover, the barrel vault requires a steel truss for form support.

Reactor Support. The reactor has three support regions: north end cell support, central cell support, and south end cell support.

The north and south end cell supports are attached directly to the operating floor (el. 100 m) of the reactor building, which has been reinforced by walls directly below the supports, as shown in Fig. 3-223. Horizontal loads are taken by the operating floor acting as a diaphragm.

For the purposes of this study, it has been assumed that the south end cell support provides north-south, east-west, and vertical restraints. The north end cell support provides only east-west and vertical restraints, and the central cell support provides only vertical restraint. This arrangement allows machine thermal growth.

The central cell support is the most critical from a structural viewpoint, because it must be small in plan to permit maintenance of the neutral-beam injectors, and yet must be 13 m high. This design concept relies on the structural strength of the reactor so that the central cell and anchor cells transmit their full north-south, and a part of the east-west, seismic loads to the south end cell support. Note that since the south end cell support is assumed fixed, the central and anchor cells must also transmit the north-south seismic load of the north end cell to the south end cell support. It is not known at this time if this is feasible. If the central cell support also requires east-west restraint, then the single cantilevered column support will most likely not be acceptable because it is probably too flexible horizontally compared to the stiffness of the central cell and anchor cell structures with respect to east-west loading. In this case, in future studies we should investigate the following two alternate concepts:

- A concrete column (see Fig. 3-223) with a horizontal reinforced-concrete east-west strut to the west wall at the 100-m elevation and an optional similar strut in the north-south direction to the floor at 100-m elevation. This will horizontally stiffen the center anchor support significantly.
- The same structural configuration described above except that each column and strut(s) is made of four wide flange steel members laced together to form box sections.

Power-Supply Complex. This complex consists of power supply, cryogenics, and diesel generator buildings. This is a structurally separate building from the reactor building complex. The power supply complex has no radiation shielding requirement. Tornado protection requires much thinner walls than the 2-m walls of the adjacent reactor building complex. If the two structures were joined into one large structure, the power supply complex would require significant strengthening to take the large soil pressure reaction which would be induced in the basemat of the power supply complex. As a separate building it can be an economical light steel structure with relatively thin reinforced-concrete floors and walls and basemat. The piping and electrical connections between the two complexes, however, must withstand relative seismic and differential settlement displacements.

AC Power System. The proposed heavy-duty substation employs three single phase transformers, each having two windings, as shown in Figure 3-227. A spare transformer, located on a standby pad at the substation, is recommended so that full-power service can be restored rapidly if a transformer failure occurs. An alternate approach is to use two large three-phase transformers with high impedance windings to reduce the short-circuit fault currents and the cost of the 13.8-kV circuit breakers. The spare transformer, if provided, would be more expensive than a single-phase high-impedance transformer. To reduce the cost of the substation, no bank circuit breakers are provided in either 13.8 kV circuit. For this purpose, differential current measurements between the primary and secondary of the transformers are used to trip the high-voltage circuit breakers connected to the utility line, which is assumed to be 230 kV for cost estimating purposes. Service disconnect switches are provided to isolate the 230 kV and 13.8 kV circuits for maintenance and backup-power operation.

Figure 3-228 is a one-line diagram of the 13.8-kV substation distribution feeders, which identify the major loads. Mnemonics used in the diagram are identified in Table 3-96. The two 13.8 kV high-impedance windings were selected in lieu of one 13.8-kV winding to reduce the short-circuit fault current and the cost of the distribution circuit breakers. The numbers in parentheses refer to the number of 13.8-kV circuit breakers associated with a given load. The nominal load size is given by the number inside the circles.

Figure 3-227. High-voltage side of heavy-duty substation with single-phase transformers.

Figure 3-228. Heavy-duty substation 13.8-kV power-distribution system.

Table 3-96. Power system nomenclature.

TNBX-CPS	Tritium neutral-beam line No. X central power supply, X = 1, 2, 3, or 4.
DNBX-CPS	Deuterium neutral- beam line No. X central cell power supply, X = 1, 2, 3, or 4.
DNBX-TPS	Deuterium neutral-beam line No. X transition cell power supplies, X = 5 and 6.
DNBX-APS	Deuterium neutral-beam line anchor-cell power supplies, X = 7 through 10.
M1A-PS, end A, M1B-PS, end B,	M-1 choke coil power supply; otherwise like M-1A-PS.
SCLC	Load center for superconducting-coil power supplies.
FUEL-P	Fuel process and tritium cleanup systems.
CRYO-M	Cryogenic system power, predominately ac power to motors.
WATER M	Water-cooling system power, predominately ac power to pump motors and cooling tower fans.
VAC	Vacuum system ac power to mechanical pumps.

As an example, there are four tritium neutral-beam lines (TNB1 through TNB4) and four deuterium neutral-beam lines (DNB1 through DNB4). Each neutral-beam line has three sources operating simultaneously and each source has separate filament, arc, and grid power supplies that receive 13.8-kV power from a dedicated circuit breaker. Therefore, there are three circuit breakers per beam line, one for each source. All 13.8-kV circuit breakers have the maximum capacity for carrying 1200 A. Current trip levels can be set at any current from 100 A to 1200 A.

The 13.8-kV circuit breakers 18 and 19 provide power to many smaller loads at lower voltage levels, shown in the more detailed one-line diagram of Fig. 3-229. Transformers reduce the voltage from 13.8 kV to 2.4 kV for operation of the cooling-pump motors, PM1 through PM4, and for the cryogen compressor motors, CM1 through CM4. The 2.4-kV power is further reduced to 480 V for the superconducting coil power supplies, PS1 through PS9. Double-ended load centers provide power to the motors and superconducting coil power supplies.

Referring again to Fig. 3-228, we see circuit breaker 27 provides power to the light-duty substation if its independent power line should fail. Circuit breakers 16 and 17 provide power to the water-cooled choke coil power supplies by means of voltage-controlled tap-changing auto transformers described here under the Magnet section, 3.2.

The light-duty substation (Fig. 3-230) provides power to loads requiring better voltage regulation. Heavy-duty pulsed loads such as neutral-beam power supplies are not operated off this line, except for test and maintenance of one source at a time when the heavy-duty line is out of service. The light-duty substation provides power to the normal and transferrable loads identified in Table 3-90 above and provides backup power by means of circuit breaker 23 if needed when the heavy-duty system is out of service. The 2.4-kV system distributes power among the TDF buildings through load centers where the voltage is further reduced to 480 V and 120 V. These facility load centers and their distribution power cables were not identified and included in a detailed cost estimate, but an allowance was made in the building cost to cover the cost of the low-voltage power distribution system.

Referring to Fig. 3-230, we note two diesel generators are sized to provide power to the essential loads needed after the machine is shut down. These loads are identified in Table 3-92. Either diesel generator can supply

Figure 3-229. Motor-control and load-control centers for the superconductor coil power supplies.

Figure 3-230. Light-duty substation and essential power-distribution system.

enough power to meet the minimum required. If all utility power is lost, the diesel generators start up automatically. Some manual assistance may be needed to connect the essential loads or a few normal facility loads if both diesel generators are on-line.

Figure 3-231 is a one-line diagram of the battery inverter system that can supply very limited power to noninterruptible power loads until the batteries are discharged. The diesel generators should be on-line, charging the batteries long before they are discharged. This system provides power to safety related protection equipment.

3.12.4 Key Issues

The key issues associated with the design of plant facilities have two major characteristics: first, they are primarily economic (as opposed to technical) and, second, they are not unique to TDF but are generic to currently envisioned neutron producing fusion devices. The major key issues involve the design requirements for:

- The hot cell facility.
- The reactor building.
- The tritium processing building.

Because the cost of the plant facilities is a significant part of the overall TDF cost, these key issues deserve some attention. Uncertainties in these design requirements leave the cost of plant facilities indefinite at best.

The design of the hot cell facility has not yet received adequate attention. The maintenance requirements of the TDF device will greatly influence this design. The major uncertainties that will significantly affect the cost of the hot cell facility include:

- The number, nature, frequency, and sequence of maintenance operations will dictate the size of the facility.
- The use of a number of special purpose cells rather than one large cell will affect the complexity and cost of the facility.
- The use of metal plates rather than epoxy paint for cell wall lining will also influence the facility cost.

The other major uncertainties that will have significant impact on the cost of the buildings housing the hot cell, the reactor, and tritium processing include:

Figure 3-231. No-break control power for the superconductor-coil protection and fuel reprocessing systems.

- Shielding requirements--Currently the wall, floor, and roof thicknesses are assumed to be two meters, based on neutron- and gamma-shielding requirements for the hot cell and reactor buildings. No definite calculations exist to validate these assumptions. Any reduction in the thicknesses save building costs.
- Safety criteria for facilities handling large amounts of tritium have yet to be established. These criteria will dictate the designability, constructability, and cost of these three buildings with respect to leak tightness, internal pressure, and seismic and tornado loading requirements.
- Hands-on and remote maintenance requirements inside the reactor building will influence the size of this building.

The following structural design issues relate to heavy weights of walls and doors:

- One area of high sensitivity to the heavy building weight caused by the required shielding thickness is the limited span that can be tolerated by the basemat because of soil pressure. This requires that the basemat be stiffened by internal walls (and the first floor where possible) to keep basemat thickness and reinforcement at reasonable levels. In future studies, we should investigate the trade off between incorporating such walls into the building layout as opposed to eliminating some of them and providing more basemat thickness and/or more reinforcement.
- Another area of high sensitivity to the required shielding thickness are the seismic loads induced on the walls of the reactor building. Most of these heavy walls span large distances in the absence of stiffening from adjacent floors or walls. This configuration appears feasible, even though, the reinforcement levels required for seismic loads are very high in the longer walls. Future studies should consider alternate designs in which these walls are stiffened with pilasters.
- The weight of the shielding doors to crane maintenance bays in the reactor and hot cell buildings should be minimized. To reduce the door mass, the door opening should only be large enough to allow the crane bridge to pass, with a larger opening provided at one location

for accomodating the crane trolley. Also, the door should be constructed of several sections (vertical joints) to further reduce the lifted weight. The same relatively small lifting mechanism could then move from section to section and slowly lift the entire door by sections.

No key technical issues are associated with the ac power system since reliable equipment is commercially available. The usual electrical safety practices allowing for leakage of hydrogen isotope gases that may cause explosive mixtures in local zones must to be observed.

An economic issue involves the amount of redundant equipment needed. Is it possible to operate the TDF without a better voltage-regulated light-duty power substation? Are the diesel generators needed and if so, how many?

In this mirror-experiment study, we followed the guidelines established for tokamak studies, assuming these guidelines also apply to mirror machines fueled with tritium.

To summarize, we note that the major facilities are currently being designed on the basis of very optimistic assumptions. The cost estimate of these facilities may increase when a detailed evaluation of the key issues is undertaken.

13. INSTRUMENTATION AND CONTROL SYSTEM

3.13.1. Summary

The instrumentation and control (I&C) system is a distributed, hierarchical computer control and instrumentation system, organized functionally into three major divisions:

- The Supervisory Control and Diagnostic System (SCDS). Responsible for the overall control and direction of the I&C system, it acquires and stores both diagnostic and facility data, and provides the operator/machine interface. The SCDS consists of nine 32-bit minicomputers with peripherals, operator consoles, and interfaces to the Local Control and Instrumentation System.
- The Local Control and Instrumentation System (LCIS). Provides the interface between the SCDS and TDF equipment. The LCIS consists of about 65 subsystems, each with a local control computer (micro-processor) linked by fiber optics to a multicrate Computer-Automated Measurement and Control (CAMAC) system which interfaces directly to the facility hardware.
- The Safety Interlock System (SIS). Continuously monitors all safety critical input/output points. The SIS consists of two programmable controllers with backup hardware, sensors, and alarms. Normally, the system operates independently of the SCDS and the LCIS, but data is available to the SCDS (via the LCIS) for display or storage.

3.13.2. Requirements

The I&C system is responsible for monitoring and controlling TDF. All operations of the facility's hardware must be initiated through the I&C. Besides monitoring all TDF parameters, the system must also allow for displaying and storing data. The design is influenced by the following considerations:

- The scope of the system; the enormous number of input/output points to be monitored and/or controlled. Based on MFTF, there will be about 3000 control points, and about 7000 that will need monitoring.

- The extended layout of equipment. Due to the remote location of the control center, there may be distances of up to several hundred meters between the controlling computers and the facility hardware.
- The need for flexibility. The design must be able to continually accommodate new sensors and functions, or even entire systems.
- The need for redundancy and system reliability. It must be possible to switch over readily to backup hardware, should equipment failures occur.
- Modularization is highly desirable. Extensive use should be made of common (interchangeable) hardware and software.

3.13.3. Discussion

The current design is based on the I&C system for the MFTF, with modifications as necessary. This approach was taken, rather than design "from the bottom up", due to the similarities of the I&C requirements for the two systems and to the natural unavailability at this stage of quantitative, detailed signal and control point lists for many of TDF's subsystems. Because there are many similarities between the two facilities, an I&C design done in this manner should be accurate with respect to system structure and should also provide a reliable estimate of the scope and size of the system.

The next three sections describe the systems.

The Supervisory Control and Diagnostic System (SCDS). This distributed, hierarchical computer control and instrumentation system will consist of nine Perkin Elmer 3230/3250 minicomputer systems (or their equivalent), each controlling one or more major TDF systems (e.g., the vacuum system, or the plasma streaming guns). All interprocessor communications are accomplished via a shared or common memory. The nine minicomputers are logically arranged around the shared memory in a star configuration, as shown in Fig. 3-232. Although each computer is dedicated to controlling a particular set of hardware, access to other data and control functions (if permitted) will be possible via interprocessor communication. The functions for the nine SCDS computers are:

Figure 3-232. The supervisory control and diagnostics system (SCDS).

- System Supervisor. This computer controls all the other SCDS computers. Its console would theoretically be manned by the chief operator, who would have access to all the lower-tier computers and would thus have complete control over all TDF operations.
- Neutral Beam Injector Supervisor. Coordinates the operation of the next-lower tier of SCDS computers, such as the neutral-beam subsystems.
- Plasma Streaming Supervisor. Coordinates operation of the plasma streaming guns, pellet injection, and monitoring of the beam dumps.
- Central Cell Neutral Beam Supervisor. Coordinates operation of the central cell neutral-beam injector sources. Supervisor controls 32 microprocessor-based subsystems, one for each neutral beam source (four sources per beam line for eight beamlines).
- End Cell Neutral Beam Supervisor. Coordinates operation of the end cell neutral-beam sources. One subsystem per source is used, for a total of 12 subsystems: four each for the anchor pumping beams, the anchor sloshing beams, and the transition pumping beams.
- The Vessel Supervisor. Responsible for the vacuum and cryogenics systems.
- The Facilities Supervisor. Responsible for the ac power, the safety interlocks, and the magnet systems.
- The Diagnostic Data Processor. Controls operation of all the plasma diagnostics.
- The Data Base Manager. Updates and maintains a data base of current values for all the TDF's parameters. Recent (but superseded) data remains on disk for a certain time, after which it is stored on magnetic tape and is available by request.

The six SCDS subsystems used for facility control have multiple CRT displays, touch-sensitive panels for operator/computer interaction, and disk storage units. The diagnostic data processor and data base manager, which will be used for software development in addition to their regular duties, will have a full set of computer peripherals. These will include a magnetic tape drive, line printers, graphics terminals, and large disk storage units.

LCIS Interface. Six of the SCDS computers communicate with the local control and instrumentation system. The LCIS consists of about 65 microprocessor-based subsystems, which interface directly to the facility's hardware. (The LCIS is described in more detail later in this section.) There are two types of LCIS/SCDS-computer interfaces. Commands and most of the data are transmitted by standard 9600 baud, bi-directional, RS-232 serial interfaces. Certain diagnostic instruments transfer data at a higher rate, and this is done by a direct memory access.

Diagnostics. The diagnostic data processor is responsible for controlling all of TDF's diagnostics. Since a large and continually growing set of diagnostic tools will assuredly be needed, the present I&C design includes the ability to handle diagnostics in general. The specific instruments or devices to be used will be defined later.

The diagnostic system is patterned after MFTF. Instead of using one computer for diagnostics and the other for data base management, it might be better (for reasons of system reliability) to divide the diagnostic channels between them, and provide each computer with access to the data base. This arrangement, an alternative to a dedicated diagnostic data processor and data base manager, is pictured in Fig. 3-233. Note that the use of 13 diagnostic systems has been assumed. The interfaces between the SCDS and LCIS computers mentioned previously are also shown. The line communication multiplexer provides a bi-directional RS-232 modem for several diagnostic systems, and a quad synchronous adapter provides a uni-directional, high-speed, direct-memory access for four diagnostics. Although not specifically defined at this time, some possible diagnostics (as used on the MFTF) are:

- Background gas pressure. An assortment of vacuum gauges for measuring the neutral particle pressure.
- Charged particle flux. A Faraday-cup collector for measuring ion current, and a disk-shaped probe for measuring the net electronic current.
- Electromagnetic fluctuations. A high-impedance electrostatic probe and a group of nested single-turn loops for measuring fluctuations.
- Far infra-red laser interferometry. To measure the phase shift of radiation with wavelengths of from 0.1 to 0.5 mm, as they pass through the plasma cross section.
- Ion spectroscopy. Similar in diagnostic function to the laser but operates at wavelengths of from 1 to 4 mm.

Figure 3-233. The plasma diagnostics data processing and display system.

- Neutral-beam transmission. A spatial array of secondary electron detectors that are used to deduce the radial profile of plasma line density.
- Neutron counters. Liquid, organic, and thin plastic scintillators, or fast-fission proportional counters, depending on the range of fluxes to be covered.
- Neutral-flux spectroscopy. Surface barrier detectors that are used for measuring the energy distributions of neutral (deuterium) particles emanating from various plasma regions.
- Plasma-current sensor. An assortment of diamagnetic loops, pickup coils, and pickup loops that respond to the changing magnetic flux linkages caused by plasma currents.
- Sloshing-ion sensors. Individual secondary electron detectors that are used for measuring the angular distribution of neutral particle flux from thermal barrier region.
- Ultraviolet spectroscopy. A self-scan image sensor for measuring the absolute intensity of the ultraviolet flux emitted by a plasma.
- X-ray spectroscopy. High-purity germanium detectors and/or inorganic scintillators (such as NaI or C_6F_6) used for measuring the x-ray energy spectrum, from which plasma electron temperatures can be deduced.

One of the key design requirements is protection against single-point failures. Accordingly, each SCDS computer has a designated backup. In other words, one of the other computers can perform the identical functions. The system and injector supervisors back up one another, as do the facilities and vessel supervisors, and the data base manager and diagnostic processors. The LCIS microprocessors (LCCs) that are connected to a particular SCDS computer are also connected to the back-up computer. If an SCDS computer fails, control of the LCCs that are affected switches to the backup computer. This can be done automatically (by computer control) or manually. Furthermore, the data base manager and the diagnostic data processor computers can each be switched to assume the duties of the other. In particular, the disk containing the data base can be switched to the functioning computer, so that new data can continue to be stored. If the disk itself fails, data can still be written directly on magnetic tape by one of two tape drives.

Therefore, the facility can continue operating despite the loss of any single SCDS computer, although perhaps with some performance degradation.

Operator-Machine Interface. The chief design goals for the operator's consoles are the operator's convenience, and flexibility for future change. The maximum possible use will be made of current technology, such as using touch-sensitive panels for command and data entry. Several CRT color monitors will be used per console. Color will be used to convey a maximum amount of information per display, and to highlight data and/or requests for action by priority level. For example, alarms could be displayed as flashing red.

All operations will be "soft-key" controlled, so that all an operator has to do to change one display to another is to press keys. A hierarchical system is employed whereby the operator can assume control of any lower tier system. However, access will be key-coded: only certain individuals will be allowed to have control over any given system. Key coding could be by badge number, or by a similar means, and all accesses to the system (plus any actions that are taken) will become part of the historical record.

All consoles will have the appropriate software support for any selectable function. At any time, it will only be necessary to update the software to add new functions, rather than having to make expensive hardware reconfigurations.

Exception Handling. A protocol for exception handling will be developed. For all the monitored signals, information such as high- and low-tolerance limits and criticality will be stored. A violation of these limits (exceptions) will initiate actions by the SCDS, which will depend on what category the exception is in:

- o Category 1. Requires only that the operator be notified and the exception recorded. Notification might be done by a flashing display screen. No action by the operator is required.
- o Category 2. Requires a response from the operator. Some action by the operator might be necessary, followed by an acknowledgement by the operator that the problem has been corrected or that it is alright to proceed.
- o Category 3. Requires that the SCDS take an immediate corrective action, such as aborting an operation, or shutting something down.

In addition, some exceptions can change their category. For instance, in combination with other exceptions, they can become more serious. One of the advantages of a computer-based exception handling system is that it can easily be programmed to look for such correlations.

Local Control and Instrumentation System. The local control and instrumentation system (LCIS) is responsible for acquiring data from, and controlling, all the hardware in the TDF. The LCIS is an array of microprocessor-based subsystems, which together form the interface between the SCDS computers and the facility's equipment. Each subsystem has several elements in common: a local control computer (an LSI-11 microprocessor), fiber-optic links, and either a CAMAC system or Modicon programmable controller. Most of the subsystems utilize the CAMAC system, which is described later in this section. the Modicon programmable controllers are used in the safety interlock system, and also are described later.

There will be about 65 LCIS subsystems (scaling from the MFTF, and assuming the use of 13 diagnostic systems), one for each major piece of TDF equipment. (For example, the central cell neutral-beam SCDS computer will control 32 LCIS subsystems, one for each central cell neutral-beam source. The end cell neutral-beam SCDS computer will control 12 LCIS subsystems: four anchor sloshing beams, four anchor pumping beams, and four transition pumping beam sources.)

Although there are a large number of subsystems, they perform many common tasks. If the hardware is developed in functional modules, common tasks will naturally lead to the use of common hardware. Thus, different subsystems will require only using different combinations of the same modules. The results are greater cost savings, the availability of more backup hardware, and better system reliability.

The LCIS Computer. The basic configuration of an LCIS subsystem is shown in Fig. 3-234. The Local Control Computer (LCC) is assumed to be the LSI-11/2, a 16-bit microcomputer made by the Digital Equipment Corporation (DEC). This was the LCIS computer selected for the MFTF. At that time it represented current technology, but it is now somewhat dated. Presumably, some newer microcomputer would be selected for the TDF, one which would have up-to-date technology at the time of the final design.

The LSI-11 includes 32 kbytes of random access memory (RAM), with no disk storage. Programs are down-loaded from the SCDS computers. The interface to the SCDS computer is the DLVLL-E, also made by DEC. This is a standard RS-232 serial-line interface (a 9600-baud modem line). The serial-highway driver provides the interface to the CAMAC serial highway. It supports programmed input/output operation, as well as block data transfer directly to memory (DMA). Optical transmitters and receivers convert our message format to the bi-phase manchester code used for serial highway transmissions.

Fiber optics. Due to the extended layout of the I&C system, fiber-optic links are required. The LCC's will be located in the central control room, which is assumed to be in a separate building from that housing the facility's hardware. The CAMAC crates are located adjacent to the power supplies, or in a specially screened room, in the facility building. The separation involved may be 300 meters, well within the capability of current fiber-optics technology. This technology is developing rapidly and is becoming increasingly more reliable, both at higher data rates and over greater transmission distances. Optical transmissions have low attenuations and large bandwidths, are immune to electromagnetic interference, and allow electrical and ground isolation between the control room and the facility's hardware buildings. Using fiber-optics communication networks in facilities such as this has become commonplace.

Computer-Automated Measurement and Control (CAMAC). CAMAC is a standardized (IEEE STD 583) data acquisition and control system used extensively in nuclear and particle physics research facilities, and in industrial applications. The hardware configuration of a CAMAC system is shown in Fig. 3-234. The fundamental unit, the CAMAC crate, is basically an input/output-card cage, which houses and provides power for plug-in CAMAC modules, each of which performs some specific operation (usually an input/output). Extrapolating from MFTF, we estimate that approximately 220 CAMAC crates will be needed. In general, 2 to 4 crates will be used for control systems and 7 to 10 crates for diagnostic systems.

The U-port module consists of an optical transmitter/receiver which converts the bi-phase manchester code (used for serial highway transmission) to the D-port format (for use in the CAMAC crate). The serial crate

Figure 3-234. The local control and instrumentation system (LCIS) subsystem.

controller provides the interface to the CAMAC I/O modules and controls all operations within the crate. The plug-in modules are subsystem specific, but provide combinations of standard functions, such as digital and analog input/output. Thus, an appropriately selected small number of different modules can be used for a wide variety of applications. Because of the high degree of standardization and interchangeability, any component can be readily replaced, thus minimizing downtime due to equipment failure. For the most part hardware is commercially available from a number of vendors. However, depending on the application, some modules may have to be designed and built, either in-house or by a vendor.

Software. A single purpose data acquisition and control program, such as the MFTF's plasma diagnostic and local control executive, will be used in all of the LCCs. The command language is based on a small number of primitives, *each one causing some simple operation to be performed.* Different sets of commands run different subsystems, but all the commands are made up of the same primitives and are run by the same executive program.

Hardware interface details are buried in a device table, which contains the information needed for a particular operation. The software set also includes a series of input/output routines, one for each defined function. These routines are invoked by the executive *when a command is executed.* The software is thus readily adaptable to hardware reconfiguration. Adding a new input/output point requires only that a new entry be made in the device table. Only if a new function is introduced would a new input/output routine be required.

Test/calibration. There is a need to operate the TDF's subsystems and diagnostics independently of the SCDS, for debugging and calibrating. An LSI-11 computer, located in the control room area, will be used to emulate the SCDS. When connected to this emulator by fiber optics, one or more special test computers (or terminals) can check or calibrate the TDF's hardware from anywhere in the facility.

Safety Interlock System (SIS). This system will monitor all personnel safety and equipment protection signals. The SIS consists of:

- Two programmable controllers with hardware backup.
- Various sensors for detecting the status of interlocks and other safety indicators, such as critical temperatures and pressures, radiation, and other hazards.
- Alarm indicators, which include audio/visual warning devices, such as horns, beepers, and warning lights.

Use of the Modicon 584 programmable controller, the choice for MFTF, has been assumed for the present design. (The final design would, presumably, make use of the latest technology, and would thus use another, newer controller.) This unit is similar to the CAMAC crate already discussed, in that it functions as an input/output card cage. However, unlike the CAMAC crate, the intelligence is built-in rather than being supplied by an external computer. Plug-in modules provide a given number of input/output channels per module, and allow flexibility in configuring the mainframe. The system configuration is shown in Fig. 3-235. The programmable controllers are linked by fiber optics and modems to an LCIS computer, and hence to the SCDS facilities computer. Some representative inputs and outputs are shown. (Note that many of the input sensors monitoring the TDF's subsystem equipment are not themselves part of the SIS. Even though they provide information to the SIS, they are considered part of their respective subsystems.) The outputs include permissives and status signals to other systems, lights indicating go/no-go conditions, and the expected assorted bells and whistles for audio/visual warnings. Programmable controllers have nonvolatile memories, but no local disk storage. Programs have to be supplied via the down-link from the LCIS/SCDS computers, or from a special programming terminal located in the control room area. During normal operations, the programmable controllers will function independently from the LCIS/SCDS, although data will be sent to the SCDS. The program stored in memory is in complete control, with no provision for remote control by the LCIS/SCDS. Specifically, this means that any SIS-generated interlocks or system shutdowns cannot be bypassed by the higher-level computer systems.

Programmable controllers have certain advantages over both computer-controlled and hardwired systems. Their primary advantage over a

Figure 3-235. The safety interlock system (SIS).

totally computer-controlled system is speed. Computer processing time is naturally proportional to the total number of input/output points. For very large systems, depending on the other duties and consequent availability of the computer, the time between consecutive servicings of any given input/output point (and therefore, the response time) may be degraded to an unacceptable level. One solution is a computer dedicated to do just this task. However, if only relatively simple logic is required, a programmable controller provides faster response time with a lower cost. A programmable controller can be reconfigured simply by plugging in new boards and changing its software. The engineering and installation costs are less. These costs are estimated at \$50 per input/output point (including programming), which contrasts with an estimated \$100 per input/output point for designing and installing hardwire relays. Furthermore, hardwire relays would (most likely) be useless after any significant reconfiguration, and would have to be replaced from scratch.

The proposed system has extensive built-in test capabilities and is extensively protected against single point failures. This includes dedicated inputs, which can be switched in to test critical outputs on-line, and dedicated outputs for testing critical input channels. Hardware backup relays ensure that power will be removed from critical outputs in case of an input/output module failure, or in case of an improper controller operation. Checking is built into the software to test for invalid program sequences. A watchdog timer is also used to pinpoint response time delays and outright failures. The system's integrity is protected by keylocked cabinets, which restrict access to the controller's hardware and the programming terminal. Keylocked switches will also be used to protect the program's memory. Without using these keys, the controller cannot be reprogrammed.

3.14. CRYOPLANT

3.14.1. Summary

A cryogenic system will be required to satisfy TDF's helium and nitrogen refrigeration and liquefaction demands. A preliminary requirements survey and analysis has shown that a cryogenic system very similar to that proposed for the MFTF-B project will meet TDF's cryogenic demands.

The cryoplant study investigated five major elements in a conceptual design:

- The LHe and LN₂ requirements.
- The plant size requirements.
- The utility requirements.
- The cost of major components.
- The incremental operating and capital costs.

The information presented here for all these areas, except for the LHe and LN₂ requirements, is based on an MFTF-B plant design proposal supplied by Koch Process Systems.

Several TDF team organizations were consulted during the requirements survey to assess the cryoplant demands as accurately as possible. These included: the Fusion Engineering Design Center (FEDC), LLNL, TRW, and Science Applications, Inc. (SAI).

3.14.2. Requirement Survey and Analysis

There are five major TDF systems that require LHe and LN₂ refrigeration/liquefaction. These demands, which must be met with a reasonable safety margin, are summarized in Table 3-97. The design must also provide for 8 hours of operation (from storage dewars) in a standby mode (such as during refrigerator maintenance).

Magnet Cryogenic Requirements. The superconducting magnets for the TDF require LHe and LN₂ during all their operational modes: cooldown, steady-state, standby, warmup, and emergency shutdown (the magnet goes normal and quenching follows). The FEDC currently has responsibility for the anchor

Table 3-97. Estimated cryogen requirements.

Subsystem/team member	Helium (W at 4.4 K/liters/hr)	Nitrogen (W at 78 K)
Superconducting magnets/GD,FEDC	2500/253	21,600
Vacuum system and cryopanel/LLNL	2050	579,000
Beam lines/TRW	900	12,500
Tritium processing/SAI	400	4,500
Pellet injection/FEDC	<u>2</u>	<u>10</u>
Total estimate	6 kW/253 liters/hr	618 kW

cell coils, while General Dynamics was assigned the design of the central and barrier cells. Table 3-98 summarizes the anchor cell requirements,⁵¹ and the LHe and LN₂ heat loads, as supplied by the FEDC. Table 3-99 summarizes the requirements for the solenoid coils (S-0, S-1). These are in-house estimates, based on MFTF-B solenoid heat-load analyses. Table 3-99 also lists the scaling factors used to assess the solenoid heat loads, as derived from the MFTF-B final design review and analysis. The LHe scaling factors were based on heat loads obtained from a detailed thermal analysis of the MFTF-B solenoid. The heat load is normalized to the parameters that have the most effect on the heat load. For example, the conduction heat load for an MFTF-B solenoid is proportional to the total cold mass (magnet pack and insulation) weight; i.e., the greater the weight, the larger and more numerous are the struts that support the magnet. Similarly, the magnitude of the centerline magnetic field directly affects the size and number of interconnecting struts that support the loads induced by the magnetic field in a fault mode, and the number and size of these struts directly affect the heat loads. A typical MFTF-B solenoid weighs 30,000 lbs, produces a magnetic field of 1 T, and gains 13.07 W by conduction.⁵² Therefore, the conduction scaling factor, normalized to weight and field would be:

$$\frac{13.07 \text{ W}}{30000 \text{ lb}} \times 1 \text{ T} = 0.4356 \frac{\text{MW}}{\text{lb} \cdot \text{T}}$$

The remaining LHe scaling factors are derived by similar methods. These scaling factors are:

- Radiation from the LN₂ shield to the LHe-temperature magnet case.
- Conduction by the instrumentation wires.
- Conduction and radiation losses by the fluid transfer lines.
- I²R losses at splices.
- Neutronic heating.

All solenoid magnet LN₂ radiation loads are based on 83.9 W/m² of magnet shield surface area and are calculated accordingly.

These magnet cryogen heat loads are only preliminary estimates, and must be updated as the design progresses. The calculated total LHe and LN₂ magnet heat loads are estimated as being 2596 W and 21,614 W, respectively for nine superconducting coils.

Table 3-98. Anchor cell region cryogen requirements as provided by the FEDC cryoplant study.

Component heat loads	LHe (W)	LN ₂ (W)
Stack and vapor cooled leads (conduction, vaporization)	96	
Splices (I ² R heating)	12	
LN ₂ shield to LHe magnet case (Radiation)	54	
Neutron heating	106	
Struts/supports (conduction)	20	
Vacuum vessel to LN ₂ shield		9000
LHe requirement (vapor cooled leads at 0.35 liters/hr and 100 A)	136	
Total estimate (all six coils)	288 + 136 liters/hr	9000

Table 3-99. Cryogenic requirements as determined from heat load estimates for solenoid coils S-0 and S-1.^a

Components	S-0 solenoid		S-1 solenoid	
	LHe (W)	LN ₂ (W)	LHe (W)	LN ₂ (W)
Struts (conduction)	408		639	
Radiation	50	3356	58	4615
Instrumentation (cable conduction)	9		10	
Fluid line transfer				
(radiation/conduction)	7		7	
Splice (I ² R heating)	4		5	
Neutronic heating (TRW analysis)	78	8	107	10
Liquid helium requirement for vapor cooled leads	31.7		27.0	
Total estimate per coil	556 + 31.7 l/hr	3364	826 + 27 l/hr	4625

^a Symbols used are W for weight (lbs x 10⁻³), B for magnetic field at centerline (T), and D for 2 π x the nominal radius x the width (n²).

Vacuum Pumping System. The 36 cryopanel in the end cell region maintain the TDF at a vacuum of 5×10^{-6} Torr and are the second largest user of LHe and LN_2 . LLNL is responsible for the cryopanel design and analysis. Cryogen requirements for the unirradiated system were obtained from Ref.⁵³. The LHe and LN_2 loads reported were 1,500 and 575,000 W, respectively.

Total additional heat deposition from neutron irradiation was calculated assuming a volumetric neutron heat flux of 4 mW/cm^3 at the plasma beam center in the vicinity of the cryopanel⁵⁴, along with an "e folding distance" of 6.8 cm.⁵⁵ It was also assumed that the LHe and LN_2 portions of the cryopanel consisted of $3.2 \times 10^6 \text{ cm}^3$ and $2.33 \times 10^7 \text{ cm}^3$ of non-transparent material, respectively.⁵⁴

Based on the preceding assumptions, neutronic heat integration analysis was completed. The total neutron heat deposition rates for the LHe and LN_2 portions are 554 W and 4032 W, respectively. These additional loads increase the total LHe and LN_2 vacuum system cryopanel loads to 2054 W and 579,032 W, respectively.

Main-Vessel Beamlines. The eight main-vessel beamlines are being designed by TRW. They contain small superconducting magnets, internal beam dumps, neutralizer ducts, and cryopanel that require LHe and LN_2 . Information regarding cryogenic requirements⁵⁶ gives LHe and LN_2 requirements of 896 W and 12,500 W, respectively. These estimates include neutron heating contributions.

Tritium Systems/Processing. The TDF tritium systems are being designed by SAI, and represent a relatively minor load on the cryopant; 400 W of LHe and 4480 W of LN_2 cryogens are required for the separation, cleanup, and processing of deuterium and tritium within the TDF.⁵⁷ In addition, 30 liters/hr of LHe liquefaction is required.

Pellet Injection. The pellet injector system requires very little LHe or LN_2 refrigeration. The FEDC is designing the pellet injector, and supplied the following cryogen requirements⁵⁸: 2 W of LHe and 10 W of LN_2 .

3.14.3. Cryoplant System Concepts

Helium System. The system requirements for the latest TDF configuration are 5848 W of LHe refrigeration at 64 kW, and 253 liters/hr of LHe liquefaction. A helium system experiences two types of loads from TDF.

- A liquid-helium refrigeration load--Defined as a cycle in which the helium is returned to the refrigerator as a saturated vapor at 4.35K, and only needs cooling (at a constant temperature) to the saturated liquid state at 4.35 K.
- A liquid-helium liquefaction load--Defined as a cycle in which the helium is either returned to the liquefiers (or is lost completely) as a superheated vapor at a temperature greater than 4.35 K. Thus, it must first be cooled to the saturated vapor temperature (4.35 K) and then cooled at a constant temperature (as with refrigeration) to the saturated liquid state at 4.35 K.

A typical liquefaction load would result from the LHe boiling off through a vapor-cooled magnet current lead, which might or might not be returned to the system. If it is not returned, it still represents a liquefaction load since gaseous helium at room temperature must be liquefied to make up the loss. A helium system therefore receives two ratings. One rating describes the available refrigeration and liquefaction capacity while the system is operating in a combined mode; the other rating is the system's capacity when it is used only as a liquefier.

The requirements just described for LHe are substantially below those of the MFTF-8 cryoplant system, with its capacity of 9,000 W of LHe refrigeration at 4.35 K, and 530 liters/hr of LHe liquefaction in the combined cycle mode. These capacities allow a 50% safety margin for the refrigeration load and 109% for liquefaction. It is not overly conservative to design a cryoplant with such margins, since the actual demands are generally at least 30 to 40% greater than expected, due to unforeseen heat losses in the system.

Cold Box (Refrigerator). The cold box is the heart of the LHe system. It contains several heat exchangers, and Joule-Thomson type throttling valves. It utilizes LN₂ precooling, which loads the LN₂ refrigerator/liquefier.

Compressors. There are a total of six oil-injected-screw type compressors in the primary compression system. The first stage consists of four, and the second stage of two, units.

Helium Storage and Supply Dewars. The helium system must include provisions for supplying LHe during a cryoplant shutdown period of eight hours. In addition, the system must be capable of storing all the gaseous helium boiled off during this time by the major TDF systems. When the cryoplant is once again operational, a recovery system must pressurize and store this helium at intermediate pressures, before it is refrigerated and liquefied.

The system's liquid helium storage capacity is 65,000 liters, which is sufficient to supply all of the TDF's systems with their LHe cryogenic requirements for the 8-hour standby period during cryoplant maintenance. A large gas bag will hold all the gaseous helium boiled off by equipment during the standby, for pressurization, refrigeration, and liquefaction at a later time.

Nitrogen System. The nitrogen reliquefier/subcooler provides cooling for the TDF systems' LN_2 shields, and also precools the helium gas before it is refrigerated and liquefied. It has a cooling capacity of 500 kW, and includes a 150,000 liter liquid storage dewar, which is capable of providing at least 8 hours of service during a cryoplant standby for maintenance.

Building/Utility Requirements. The floor space required for the TDF cryoplant will be comparable to the MFTF-B cryoplant, but it will have a slightly different arrangement due to differences in reactor dimensions. This study assumed that the TDF's cryoplant will be completely separate from the reactor building (which housed part of MFTF-B's cryoplant). It was assumed that the entire TDF cryoplant (including the cold box, and all the storage tanks and dewars not housed in the MFTF-B configuration) could be placed in a building with twice the floor space of that proposed for MFTF-B. Assuming an MFTF-B building size of 6300 ft^2 , the TDF building will therefore require approximately 12,600 ft^2 . Since certain components of a cryoplant (such as storage tanks) do not require housing, it is probable that the actual building requirement for TDF may be only slightly larger than the MFTF-B building requirement. This cannot be accurately assessed until more detailed TDF designs have been completed.

The estimated electrical utility requirements for a cryoplant of this size are: LHe 5400 kW, LN₂ 6000 kW. The total power requirement is 11.4 MW. These power requirements may be used to illustrate the relative utility cost for LHe and LN₂ refrigeration. The LHe system, capable of removing 9000 W of heat at liquid helium temperature (4.35 K), requires 5400 kW to operate. Therefore, this "costs:"

5400 kW or 600 W of electric power .
 9 kW watt of heat removed at 4.35 K

Similarly, for every watt of heat removed at LN₂ temperature (78 K) it "costs:"

6000 kW or 12 W of electric power .
 500 kW watt of heat removed at 78K

3.14.4. Incremental Capital and Operation Costs

Given the TDF/MFTF-B cryoplant capital and operating costs, it is possible to derive the incremental cost data. These data can be very valuable for system comparisons, pre-design analysis, and rough cost estimates. The operating costs reflect the electric power and water utility requirements. The liquid helium incremental operating costs are \$37.50/kW-hr. The LHe capital costs include building, installing, preoperational testing, and original equipment costs. The incremental capital costs for LHe are \$1333/W at 4.35 K. At 78 K, the operating and capital costs for liquid nitrogen are \$0.75/kW-hr and \$15.90/W, respectively.

3.15. MAGNET POWER CONVERSION AND PROTECTION

3.15.1. Summary

All the magnet coils, except for the M-1 choke coils, are superconducting. They are charged slowly and operate in a steady state for a long time. During a normal shutdown, the coils are discharged slowly (slow dump). However, if the coil protection system detects an imminent quench, the coils are all discharged rapidly (fast dump). The fast discharge transfers most of the energy stored in the coils to dump resistors, so that the maximum temperature rise of a coil is <300 K for credible faults, accidents, or operator errors. The coil protection system obtains its power from the noninterruptable supplies to ensure its continued operation should the utility power fail and to prevent inadvertent fast shutdowns from occurring.

Each superconducting coil has a low-voltage, high-current power supply for charging the coils and supplying bussing and cable losses during steady-state operation. The choke coil magnets are normal resistance, watercooled copper coils connected in series to ensure that the same current flows through each of them during normal and faulted operation. Medium-voltage power supplies are needed to supply their relatively large power losses. If the water flow to the M-1 coils or the liquid level in their pressurized water storage vessel falls below a safe level, a fast discharge of all coils is initiated, which turns off the power to the magnet power supplies.

Thyristors in the primary circuits of the rectifier transformers of the superconducting coils control the coil currents. Voltage-controlled tapped auto transformers control the choke coil currents.

3.15.2. Requirements

The data for the magnet field-coil power supply and protection system requirements are given in Table 3-100. The charging time should not exceed 4 hours, and the power supplies must be able to provide the steady-state currents shown in the table with sufficient potentials to overcome the bus, cable, and terminal voltage drops, and the $L(di/dt)$ needed for startup. The M-1 choke coils are water-cooled copper coils, and their power supplies must also provide additional voltage to overcome the voltage drops across these coils during steady-state operation.

Table 3-100. The TDF's coil power conversion and protection requirements.

Parameter	Value
Maximum coil charge time (hr)	4
Maximum coil discharge time constant	
• Fast discharge (s)	200
• Slow discharge (hr)	2
Maximum discharge voltages (Pool boiling SC coils):	
• Coil to ground (kV)	2
• Coil terminal-to-terminal (kV)	2
Steady-state coil currents and stored energy (kV/MJ)	Current/energy
• Coil (kA/MJ)	
S-0	9.75/406
S-1	7.84/896
M-1	14.22/11.8
M-2	6.55/17.0
M-3	6.55/13.9
T-1	5.71/13.3
Maximum temperature rise during a coil quench (K)	<300

The central cell solenoidal coils, S-0 and S-1, have a large amount of stored energy in their magnetic fields. Because these coils use pool boiling helium to maintain cryogenic temperatures, the voltage across the coils and from coil to ground are limited to about 2 kV. To prevent magnet damage due to a quench, it is necessary to transfer a large part of the stored energy to dump resistors in a short time. This requires that the fast discharge time constant be no greater than some upper limit yet to be determined. Based on previous analysis, this limit is estimated to be 200 s.

3.15.3. Design description

Design data for the magnet power supplies are given in Table 3-110. Compared to the other coils, M-2, M-3, and T-1 have a low amount of stored energy. Because of their low inductance, very little additional voltage is needed to meet their startup requirements. Previous experience has shown that 12 V is adequate for covering the resistive circuit voltage drops and the small additional voltage needed for charging these superconducting coils. The central cell coils have a larger amount of stored energy and associated inductances. To overcome their higher inductance voltages during startup, their power supply terminal voltages must be several volts higher than for the other coils. The ac input voltages and amperages depend somewhat on the power supply design. The data in Table 3-101 are typical for a good design, and provide the basic requirements for designing the ac power and coil protection load center for the superconducting coils.

From Table 3-101, it is evident that the resistive choke coils require a total of 52 MW for steady-state operation. Most of the voltage is needed to cover the coil voltage drop. The circuit voltage drop and inductive startup voltages are small in comparison. The large power load for these coils requires that their ac power be taken directly from 13.8 kV feeders. A generic one-line diagram, applicable to all the superconducting coil power supplies, is shown in Fig. 3-236. The coil current control is achieved with silicon controlled rectifiers (SCRs) in the primary of the rectifier transformer. The voltages and currents in the coils are controlled by varying the firing angles to the SCRs (thyristors). Due to the large currents involved, the power supply losses are appreciable and require water cooling. The delta and wye 6-pulse bridges provide 12-pulse dc power to the coils. The free-wheeling diodes provide a low impedance drop for a very slow shutdown, when the ac breakers are open.

Table 3-101. Magnet power supply design data.

Coil ID	S-0	S-1	M-1	M-2	M-3	T-1
Type of coil	SC ^a	SC ^a	NWC ^b	SC ^a	SC ^a	SC ^a
Number of power supplies	1	2	2	2	2	2

Power supply ratings

● AC input

V	480	480	13,800	480	480	480
A	216	216	1,250	120	120	110
kVA	180	180	30,000	100	100	90

● DC output

V	15	18	1,800	12	12	12
A	9,800	8,000	15,000	6,800	6,800	6000
kW	147	144	26,000	82	82	72

^a Superconducting coil.

^b Normal water-cooled coil.

Figure. 3-236. A typical primary power supply for the superconducting coils: consisting of a silicon-controlled rectifier with low voltage and high current.

Figure 3-237 is a one-line diagram of the choke coil power supplies. Two 13.8 kV feeders supply the motor driven tapped auto transformers to provide a nominal 6.9-kV power to the rectifier transformers. The auto transformer taps provide voltage control for startups, and for establishing the desired operating current. The secondaries of the rectifier transformer are connected in a delta and wye configuration, as shown in Fig. 3-236, for 12-pulse operation. The rectifier symbols in the squares of Fig. 3-237 represent six-pulse bridge rectifier circuits. The choke coil currents are about twice those of the superconducting coils, so a choke coil requires two 12-pulse units connected in parallel. The choke coils must be connected in series, so that the same amount of current will flow through each one under both normal and faulted operation. The coils could be interleaved among the power supplies, which would require four 15 kA busses; or, they can be connected as shown in Fig. 3-237 to reduce the bussing cost.

The superconducting coil protection equipment requires dump resistors, switches, and circuits such as those shown in Fig. 3-238. When an imminent quench condition is detected, a fast energy dump is initiated by opening the circuit breakers to interrupt the current flow from the power supplies. The inductive voltage rises and drives the current through the dump resistors. Their maximum resistance (and hence the coil discharge time) is limited by the permissible voltage drop between the coils and ground, and across the coils' terminals. A slow energy dump can be initiated by opening pneumatically controlled switches to dissipate the coil energy much more slowly through the 0.0015-ohm slow-dump resistors.

Each superconducting coil will have all the coil protection equipment shown in Fig. 3-238, and listed, with their ratings, in Table 3-102. The discharge voltages for the anchor cell and the central cell coils will be limited to 500 and 1000 V, respectively, which is sufficient to transfer most of the stored energy to the dump resistors.

Figure 3-239 shows a conceptual plan view for the electrical section of the building, and Fig. 3-240 is a conceptual elevation view for the magnet electrical support equipment in this section. The large transformers and medium-voltage circuit breakers are located outside, but close to, the building. The power supplies (PS), switches (SW), and circuit breakers (BR) are located in cabinets, conveniently grouped inside the electrical building,

Figure 3-237. The magnet power conversion system for the series-connected choke coils, M-1.

Figure 3-238. The magnet power supply and coil protection system for the M-2 and M-3 pair of superconducting coils.

Table 3-102. Design data for the magnet protection system.

Parameter	Coil					
	S-0	S-1	M-1	M-2	M-3	T-1
Number of coils	1	2	2	2	2	2
Operating stored energy/coil (MJ)	406	896	11.8	17.0	13.9	13.3
Operating current (kA)	9.75	7.85	14.22	6.55	6.55	5.71
Equivalent inductance (H)	8.5	29.1	0.12	0.79	0.65	0.82
Quench discharge voltage (kV)	1.0	1.2	N/A	0.5	0.5	0.5
Quench decay time constant (s)	85	194	0.91	11.3	9.3	11.7
<u>DC circuit breakers</u>						
Number/coil	2	2	N/A	2	2	2
Volts (kV)	1.0	1.2		0.5	0.5	0.5
Current (kA)	10.0	8.0		7.0	7.0	6.0
<u>Fast-Dump Resistors</u>						
Number/coil	8	16		2	2	2
Resistance (ohms)	0.20	0.60		0.035	0.035	0.035
Peak power (kW)	1200	600		1500	1500	1150
Energy (MJ)	50	56		17.0	13.9	13.3
<u>Slow-Dump Resistors</u>						
Number/coil	1	1		1	1	1
Resistance (ohms)	0.0015	0.0045		0.00015	0.00015	0.00015
Peak power (kW)	150	300		6.5	6.5	5
<u>Ground Resistors</u>						
Number/coil	4	8		1	1	1
Resistance (ohms)	50	50		50	50	50
Power (kW)	1.0	1.0		1.0	1.0	1.0

Figure 3-239. A conceptual plan view of the power conversion and magnet protection equipment.

Figure 3-240. Section A-A elevation view of the power conversion and coil protection equipment, from Figure 3-239.

in close proximity to their associated coils in the reactor cell. Each group also contains one ac power transition cabinet (AC), which connects to the load centers, the instrumentation and control cabinets (IC), and the water cooling system (WC) cabinets. Cabinets are arranged in a "U"-configuration so that the back side is accessible for installation and maintenance. Cabinets can be disconnected from their electrical cables and busses, and their water cooling lines (located under the center raised floor), and jacked by fork-lift trucks into the 9-ft aisle for removal and replacement. Front panels along the main corridor of the building are the local control centers.

The many large dump resistors are located on top of the electrical building. Vertical busses connect them to horizontal busses that pass under the raised floor and penetrate the reactor building through cylindrical steel pipes. An insulating epoxy-type filler separates the busses from each other and the large pipe, which is secured by the concrete wall. The pipe also serves as a magnetic shield. Electrical cable conduit is also expoxied into the large pipes. The conduit cables are also secured with epoxy, so that any tritium that should leak into the reactor building will not readily escape through the electrical wall penetrations.

The two load control centers, LCCA and LCCB, provide ac power and circuit protection for the superconducting-coil power supplies. Figure 3-241 shows the one-line diagrams of these load centers, which also supply 2.4-kV power to the coolant pump and cryogen compressor motors. Additional 480-V power from the load center supplies smaller motors and the building's lighting needs.

3.15.4 Key issues.

Magnet power supplies and their load centers can be designed with proven, highly reliable commercial components. The coil protection system developed for the MFTF-B is also applicable to the TDF. The usual electrical safety practices need to be incorporated into the design. No technical problems exist.

Figure 3-241. The Motor and load control centers for the Superconducting coil power supplies.

3.16. MAINTENANCE

3.16.1. Overview

Maintenance and disassembly of the major systems were prime drivers in developing the TDF configuration. Wherever it was appropriate, these considerations influenced component designs with regard to access, handling, and location. Early in the project, we established a maintenance philosophy by which to develop the baseline configuration and its components. The major aspects are:

- Contacting maintenance operations at the shield boundary 24 hours after device shutdown.
- Being able to accomplish all maintenance and disassembly operations remotely under emergency conditions.
- Having modularized component installations.
- Using proven remote-maintenance equipment technology.

Establishing a philosophy for maintaining a D-T fusion device requires taking several considerations into account. These include:

- Prior to any disassembly, personnel should be able to routinely do hands-on inspections, perform maintenance equipment setups, and supervise maintenance activities in the reactor building.
- Since activation levels within the shield boundary are too high for hands-on maintenance, remote operations are required.
- Because of these requirements, the bulk shield is designed to limit activation at its outer surface to a level that will permit contact maintenance in the reactor building (outside the device shield boundary) after a suitable cooldown period.
- In addition, the shield is designed to specifically protect certain components (e.g., the superconducting coils) from material damage and excessive neutron heating.

In light of these requirements, TDF is designed to have a maintenance approach which maximizes the flexibility of maintenance operations. Personnel access into the reactor building is permitted after a reasonable shutdown period, within the limits discussed in Section 3.16.2.

3.16.2. Requirements and Performance

The maintenance approach is three-fold, considering the requirements for contact and remote maintenance, component disassembly, and the technology available for remote handling equipment.

Contact and Remote Maintenance. Generally, beginning one day after shutdown, all areas outside the device shield are maintained by contact operations. This assumes that the plasma chamber is unopened, and that all vessel penetrations are properly shielded. In addition, these contact tasks must also be remotely accomplishable in emergency situations where personnel would be prohibited from entering the reactor building.

Component Disassembly. Wherever practical, subsystems and components are designed for replacement as individual modules having minimal impact on adjacent systems. This concept is particularly applicable to components known to need more than one replacement during the device's lifetime. During the baseline's development, ease of access and simplicity of handling were configuration requirements.

Remote Maintenance Equipment Technology. Because TDF is a near-term device, component maintenance and replacements planning specifically assumes the use of existing remote maintenance equipment and technology in the manipulator, viewing, and transport systems. Technological developments are considered to be near-term, if they are expected to occur within several years.

In summary, the requirements and limitations for both contact and remote maintenance are:

- Permissible dose rate. The surface dose rate at the shield boundary will be 2.5 mr/h. At this level, during a 40-hour work week next to the device, the exposure to personnel will not exceed the federal limitation of 5 rems/yr. (It might be possible to increase the dose rate limit by reducing the shield thickness, if it were assumed that there are no limits on the numbers of available maintenance personnel or that contact operations will not require extended

periods of downtime. Neither of these assumptions was used.) Furthermore, a dose rate limit of 2.5 mr/h is consistent with other recent reactor studies.

- Shutdown time. The shield's thickness was designed to allow personnel to have access to the shield boundary 24 hours after shutdown. Because the short-lived radioisotopes in the shield material will decay to permissible levels, this waiting period will reduce the surface dose rate to 2.5 mr/h. This time can also be used by personnel for such general maintenance preparations as de-energizing the coils, draining coolant, and preparing equipment.
- Contact maintenance boundary. Contact maintenance will be limited to operations outside the device shield boundary, provided that the shield is intact, and that the major penetrations are shielded. The contact maintenance boundary is shown in Fig. 3-242. (Generally speaking, contact maintenance operations are defined as operations done without a protective suit. Therefore, disassembly procedures in the reactor building include provisions for the decontamination of tritium and activated particulate matter. A design goal for TDF is to have contact operations done without suits. However, if contamination levels exceed the maximum permissible concentrations, the use of suits with portable life-support systems is an acceptable mode for hands-on operations.) The shield materials and thicknesses that meet the biological requirement for a 2.5 mr/h exposure rate are also shown in Fig. 3-242. The central cell consists of 115 cm of stainless steel, with 5 cm of lead cladding on the outer surface. The superconducting solenoid coils require 90 cm of stainless steel between their bores and the plasma. The anchor cell is shielded with 60 cm of borated (heavy) concrete, and the end cell shield is 60 cm of ordinary concrete.
- Miscellaneous. Several other operational constraints must also be considered in a maintenance context. Personnel and maintenance equipment will not be permitted in the reactor building during the device's operation. The biological hazard for personnel is obvious. For equipment, this policy will avoid neutron-induced activation, which would result in having to remotely maintain the

Figure 3-242. The contact maintenance boundary and shield thicknesses.

maintenance equipment. This is why the overhead crane systems, which must remain in the reactor building, are stored behind a shielded enclosure at one end of the facility.

The magnetic coils must be de-energized during both remote and contact maintenance operations. This is primarily a safety consideration for personnel, maintenance equipment, and the magnets. The concern relates to two factors:

- The energy stored in the larger coils is significant, being measured in the megajoule (MJ) range.
- The attractive force of the magnets is significant, and can affect the operation of electric motors in the manipulator systems, or turn small magnetizable tools into projectiles. (A plot of the magnetic flux around the device is in Section 3.2.

However, the coils can be kept at their operating temperature if the maintenance operations do not involve disturbing the magnet systems. A test module replacement is an example where the coils would be de-energized, but could still be kept at liquid helium temperature.

3.16.3. Maintenance Equipment

The remote maintenance equipment basically consists of manipulators, viewing systems, and transporters. Because of the near-term time frame for TDF, only presently available equipment is being considered. Manipulator systems are used to accomplish the functions of a human. In general, these systems consist of closed-circuit TVs (CCTVs), available in color or black and white with two- or three- dimensional imagery, coupled to a manipulator system. The transport systems include the overhead cranes and other transport devices in the reactor building used for removing and installing components on the device, and for moving components to the hot cell. A preliminary listing of special-purpose, remote handling equipment is presented in Table 3-103.

3.16.4. Scheduled and Unscheduled Operations

Most of the major components will require scheduled maintenance or replacement during the device's lifetime, and all of them may require

Table 3-103. Preliminary list of special-purpose remote handling equipment.

Particle pump handling device
Helium dump handling device
Cryopanel assembly handling device
Beamline handling device
 (Anchor pumping, transition pumping, and sloshing)
beamline handling service (central cell)
Overhead handling fixtures for:
 Cryopanel
 Sloshing beamlines
 Choke coil
 Transition and anchor coil assembly
 Test module
 Pellet injector
Test module handling device
Beam dump handling device

unscheduled replacement. Provisions for these unscheduled replacements has been considered in developing the baseline configuration, although the emphasis has been placed on scheduled maintenance operations. A number of components have been identified as requiring periodic replacement. The design of these components, as well as their position on the device, was very much influenced by maintenance considerations, such as accessibility and modularity. Table 3-104 is a list of these components, along with pertinent comments, that compiles the best estimates of their expected lifetimes. Readers are cautioned against using these estimates as absolute values, since they represent only a first cut. The value of this table lies in the making of relative comparisons for identifying maintenance drivers, and for assessing maintenance equipment needs.

Using the lifetime data from Table 3-104, a chart of scheduled component replacements can be developed. Figure 3-243 is just such a chart. It shows the first replacement times for various components plotted against calendar years and equivalent full-power operating years (FPY). For reference, the availability distribution is also shown. The first components requiring replacement will be the ion sources, with an average expected lifetime of approximately 0.2 FPY, or 1.5 calendar years. The superconducting coils are lifetime components, and are therefore shown as having no scheduled replacements, even after 15 calendar years. The choke coil has an expected lifetime of 0.25 FPY, and it is assumed that its failure will be dependent on neutron-induced degradation. Therefore, noting that the first year of device operation is with hydrogen only, the accumulated operating time to failure will begin after the first calendar year. This is assumed to be true for most of the components. Note that the expected lifetimes are given in FPY, which is directly related to actual operating time for the device. For example, based on the assumed availability distribution for TDF, the 15-year calendar life of the device is equivalent to 5.4 FPY.

Two important points can be made by using Fig. 3-243. The first is that maintenance procedures and equipment must be in place early in the operating phase of the program. Slightly more than 1-1/2 years after startup, the first use of remote-handling equipment occurs, implying that maintenance considerations must be factored into the designs at an early stage. The second is that it behooves the component designer to seek extended lifetimes,

Table 3-104. Expected lifetimes of components in the central cell, anchor cells, and end cells.

Component	Expected Lifetime (full power years)	Comments
<u>Central Cell</u>		
S-0 coil	5.4	Full lifetime
S-1 coil	5.4	Full lifetime
Test modules	0.1 - 1	Weekly, monthly, yearly
CC beamlines	0.2	
Sources	<0.1 ^b	
Dumps	0.2 ^a	System driver
Cryopanel	0.5	
Magnets	5.4	Full lifetime
CC beam dumps	0.2	
Choke coil	1	Design objective; 0.25 may be actual
Pellet injector	0.3	
<u>Anchor Cell</u>		
Transition coil	5.4	Full lifetime
Anchor coils	5.4	Full lifetime
S beamline	0.5	
Sources	0.2 ^b	
Dumps	0.5 ^a	System driver
Cryopanel	1.0	
Magnets	2.0	
TCP beam dump	<0.2	
S beam dump	<0.2	
ACP beam dump	<0.2	

Table 3-104. Cont'd.

<u>End Cell</u>		
Particle dump	0.5	
Cryopanel assemblies	1.1	
Helium pumps	1.7	
TCP beamline	0.2	
Sources	<0.1 ^b	
Dumps	0.2 ^a	System driver
Cryopanel	1.0	
Magnets	2.0	
ACP beamline	0.5	
Sources	0.2 ^b	
Dumps	0.5 ^a	System driver
Cryopanel	1.0	
Magnets	2.0	

^a The system driver is that component with the shortest life, which requires the entire system (module) to be removed for maintenance.

^c Beamline sources are not system drivers, even though their expected lifetimes are the shortest in the beamlines, because they are designed to be replaced as independent submodules within the beamline system.

Figure 3-243. An estimate of first-time replacements for selected components.

where they can be realistically achieved. For instance, Fig. 3-243 shows that the first choke coil changeout will occur after six calendar years instead of 2.5, if the choke coil's operating lifetime can be extended from 0.25 to 1 FPY.

The chart in Fig. 3-244 examines the scheduled replacement history of one component over the device lifetime. Only the choke coil is shown, but a similar chart can be developed for any component. For a choke coil with an operating lifetime of 0.25 FPY, there are 21 scheduled replacements. During the last phase of the device's operation, with a 50% availability, the changeouts will occur approximately twice each year.

3.16.5. Component Replacements

Here are abbreviated descriptions of some of the component replacement scenarios that were considered during the configuration development. In each case, it is assumed that hands-on maintenance operations will be done whenever possible. Therefore, maintenance personnel are involved as long as the device is unopened. They are responsible for setting up equipment such as scaffolding, manipulators, and fixtures, and they provide the on-site inspection prior to starting replacement operations.

Test Module. Figure 3-245 shows the removal of a test module, which is located at the upper centerline of the central cell. Note that this module includes the test specimen as an integral part of the shield plug. After removing the service connections and unfastening the vacuum flange interface between the module and the vacuum boundary of the central cell, a lifting fixture is assembled onto the module flange for overhead lifting. All of the operations done before the test module is lifted out are manually assisted. The module's weight is estimated as being about 50 to 70 tons, with overall dimensions of 1.2 x 1.6 x 4.3 m. Reinstalling the replacement module requires that an alignment fixture be indexed to and mounted onto the central cell. The re-installation process is totally remote, until the module is again seated on the vacuum flange.

Choke Coil. Figure 3-246 shows a choke coil being removed. This operation is essentially the same as that for the test module, because of the coil's upper

OPERATING LIFE OF A CHOKE COIL = 0.25 FPY

ORNL-DWG 82-3330 FED

Figure 3-244. The choke coil replacements scheduled for the device's lifetime.

Figure 3-245. Test module removal.

Figure 3-246. Choke coil and beam dump removal.

centerline location. The choke coil is expected to be one of the most frequently replaced components, so it was located for an easy overhead access. Before this module (the shield plug and magnet) is lifted out, all operations are contact. The choke coil module weighs approximately 20 tons, and it also requires an alignment fixture for reinstallation.

Central Cell Beam Dump. Figure 3-246 also shows the orientation and removal of a beam dump. To remove these components, handling equipment must be used that can accomplish a motion away from the vertical, which precludes using the overhead crane. This type of maintenance equipment is shown in subsequent figures.

Anchor-Sloshing Beamline. Figure 3-247 illustrates removing the beamline components as a rail-mounted pallet assembly, enabling the entire system to be handled as one large module. This approach is taken because of the limited access available for the beamlines. The ion source replacement is the one component which can be independently changed after removing its service connections and the shield cover. A rail-mounted extraction device is required to pull the pallet after the vacuum flange interface is disconnected. Figure 3-247 is also typical for the anchor and transition pumping beamlines. The weight of the largest pallet for these beamlines is estimated to be less than 50 tons.

Central Cell Beamline. The disassembly procedure for the central cell beamlines is more complex than that for the other beamlines. This results from minimizing the clearance between to the reactor building's walls. Figure 3-248 shows the arrangement for their disassembly. The first component to be removed is the module consisting of the nuclear shield and the rear superconducting magnetic shield. This first module interfaces with the beamline at the rear vacuum bulkhead, where the sources are mounted. (This module is also removed to gain access to the ion sources, when they need replacement.) The second module that must be removed is the rear half of the nuclear shield, which provides ample access to the pallet-mounted beamline components. The nuclear and magnetic shields that remain are essentially fixed in place.

Figure 3-247. Beamline removal: typical for anchor sloshing, transition pumping, or anchor pumping.

Figure 3-248. Central cell beamline removal.

The adjustable support platform is used to assist an extraction device (not shown), which handles the pallet assembly prior to its being lifted by the overhead crane. The extraction device for the central cell beamline would be mounted to the building wall in a manner similar to that shown for the cryopanel removal in Fig. 3-249.

Cryopanel. Each end cell consists of thirty-six 2 x 4 m cryopanel pumps, which are arranged in modular assemblies of three panels each, as shown in Fig. 3-249. This module is a manageable size that will not change the building wall dimensions, which were established by the central cell beamline removal.

This figure shows one of the six lower panel assemblies being removed. First, the cryopanel handling device removes the shielded access cover to gain entrance into the end cell. It then pulls the track-mounted assembly out onto an inclined plane. It is possible to adjust this handling device vertically and horizontally by using the jack-pads and wheels of the trolley bed, respectively. Finally, the overhead crane is used to deliver the component to the hot cell entrance.

Particle Dump. The particle dump consists of a number of panel modules, which are connected to common inlet and outlet coolant manifolds. Figure 3-250 shows one of these panels being removed with a mobile handling device. Access to the panels is gained by removing a lower shielded cover.

Helium Pump. Figure 3-251 shows a helium pump module being removed with a handling device similar to that used for removing the particle dump.

Magnet Vacuum Vessel. The anchor and transition coils are inside a common vacuum boundary, which is supported within the anchor cell. The vacuum vessel (with the anchor and transition coils) weighs approximately 80 tons and is the largest modular component of TDF.

Initially, there were several ways to install this module into the anchor cell and several ways of removing it, should its unscheduled replacement be required. However, to keep within the constraints established for a minimum

Figure 3-249. Removal of cryopanel modular assemblies.

Figure 3-250. Particle dump panel removal.

Figure 3-251. Helium pump module removal.

reactor building size, the installation and replacement are accomplished through the rear of the end cell. Figure 3-252 shows the sequential removal of the vessel assembly.

This operation requires that the cryopanel assemblies and the end dome be removed. A rail system mounted at the top centerline of the end cell cylinder is one way to support the vacuum vessel during its removal. The rail(s) and a cable and hoist mechanism also appear to be a straightforward way for initially installing this component.

Figure 3-252. Magnet vacuum vessel assembly removal.

REFERENCES--Section 3.2

1. "The Structural Analysis of Nonhomogeneous Solenoids Using the Stansol Computer Program," Mechanics Research, Inc., MRI-C2754-TR-3, (1975).
2. M. Kohler, Am. Physik 5(32), 211 (1938).
3. R. Van Konynenburg and M. Guinaen, Radiation Effects on MFTF-B Magnets, Lawrence Livermore National Laboratory, Livermore, CA, interdepartment memo (June 1981).
4. Final Design Review for the A-Cell, Transition, and Solenoid Coils for the Mirror Fusion Test Facility-B (MFTF-B), General Dynamics Corporation and the Lawrence Livermore National Laboratory, Livermore, CA, GDC-MFTF-B-81-04 (1982), p. 5-7.
5. J. M. Williams, C. E. Klabunde, J. D. Redman, R. R. Coltman, Jr., and R. L. Chaplin, "The Effects of Irradiation on the Copper Normal Metal of a Composite Superconductor," IEEE Transaction on Magnetics 15(1) (1979).
6. Joel Schultz, If You Must Run Water Past Your Insulation, Plasma Fusion Center, Massachusetts Institute of Technology, Cambridge, MA, MITN-82-06 (1982).
7. R. Chaplin and R. Cultman, Jr., "Defects and Transmutations in Reactor Irradiated Copper," in Proc. Intern. Conf. Neutron Irrad. Effects, Argonne, IL, 1981 (Argonne National Laboratory, Argonne, IL, 1981).
8. D. B. Montgomery, Solenoid Magnet Design: The Magnetic and Mechanical Aspects of Resistive and Superconducting Systems, (Robert E. Krieger Publishing Co., Melbourne, FL, 32901, (1980).
9. Mirror Advanced Reactor System Study, Lawrence Livermore National Laboratory, Livermore, CA, UCRL 53333 (1983).

REFERENCES--Section 3.3

10. E. F. Marguarat, H. H. Hazelton, M. M. Menon, D. E. Schechter, W. L. Shirling, and C. C. Tsai, "Advanced Design of Positive Ion Sources for Neutral Beam Applications," in 8th Symp. Energy Problems Fusion, San Francisco CA, November 1979 (IEEE Service Center, Single Publication Sales Dept., 445 Hoes Lane, Piscataway, N.J. 08854 1979).

11. C. C. Tsai, M. M. Menor, P. M. Ryan, D. E. Schechter, and H. H. Hazelton, "Long-Pulse Ion Source for Neutral Beam Applications," Rev. Sci. Instrum. 53(4)(1982).
12. H. H. Hazelton, private communication.
13. R. A. Dewitt, "Coil Design Considerations and Analysis for Neutral Beam Magnets," in 7th Symp. Eng. Problems of Fusion, Knoxville, TN, 1977 (IEEE Service Center, Single Publication Sales Dept., 445 Hoes Lane, Piscataway, N.J. 08854, 1977).
14. L. D. Stewart, Studies of Neutral Beam Power Loss in Stray Magnetic Fields.
15. H. W. Kugel, H. F. Dylla, E. P. Eubank, T. A. Kozub, R. Moore, G. Schilling, L. D. Stewart, A. von Halle, and M. D. Williams, "PDX Neutral Beam Deionization Losses," in Proc. 28th Symp. Amer. Vacuum Soc., Anaheim, CA, Nov. 1981 (Journal of Vacuum Science and Technology 20, 3-4, March-April, 1982).
16. A. C. Riviera and J. Sheffield, "Transfer Efficiency of Intense Neutral Beams," Nucl. Fusion 15, 944, (1975).
17. R. S. Hemsworth, Enhanced Reionization Loss for Neutral Beams in Magnetic Fields, United Kingdom Atomic Energy Agency, Culham Laboratory, Culham, Abington, Oxfordshire, England, CLM-R162 (1976).
18. M. Pirani and J. Yarwood, Vacuum Technology (Cambridge Press, London, 1960).
19. L. D. Stewart, H. P. Eubank, L. R. Grisham, G. Schilling, R. W. Stooksberry, and M. Ulrikson, "Regionization Losses in Neutral-Beam Ducts," in 8th Symp. Engineering Problems Fusion, San Francisco, CA, Nov. 1979 (IEEE Service Center, Single Publication Sales Dept., 445 Hoes Lane, Piscataway, N.J. 08854, 1979).
20. L. C. Pittinger, Power Density Calculations for Beams from Diffuse, Astigmatic Rectangular Sources, Lawrence Livermore National Laboratory, Livermore, CA, Engineering Note ENC 77-1 (1977).
21. R. Conn University of California-Los Angeles, 405 Hilgard Ave., Los Angeles, CA, 90024, private communication (1982).
22. A. W. Molvik, E. D. Baird, and K. H. Berkner, A Compact 80-keV Neutral-Beam Module, Lawrence Livermore National Laboratory, Livermore, CA, UCRL-79682 (1977).
23. J. W. Shearer, T. A. Cutler, and L. W. Dilgard, Lawrence Livermore National Laboratory, Livermore, CA, UCRL 52824 (1979).

24. Henning, et al., Engineering the Baseball Magnet System, Lawrence Livermore National Laboratory, Livermore, CA, UCRL-73067 (1971).
25. A. K. Chargin and C. J. Anderson, Solving Problems of Neutral-Beam Injectors at LLNL, Lawrence Livermore National Laboratory, Livermore, CA, UCRL-75023 (1973).
26. J. Kim, W. L. Stirling, and W. K. Dagenhart, An Ion Energy Recovery Experiment Based on Magnetic Electron Suppression, Oak Ridge National Laboratory, Oak Ridge, TN, ORNL/TM-7283 (1980).
27. Model conveyed in a private communication from John Conrad, University of Wisconsin Madison, WI, to Larry Stewart, Exxon Nuclear/PPPL 4/21/80 for institution into EFTNB code.

REFERENCES--Section 3.7

28. A. Jung and M. A. Abdou, Nucl. Tech. **41**, 71 (1978).
29. M. Ragheb, A. Klein, and C. W. Maynard, Nucl. Tech./Fusion **1**, 99 (1981).
30. L. El-Guebaly and C. W. Maynard, Trans Am. Nucl. Soc. **42**, 612 (1982).
31. MCNP--A General Monte Carlo Code for Neutron and Photon Transport, Los Alamos National Laboratory, Albuquerque, NM, LA-7396-M (1981).
32. B. Brown, T. Blewitt, T. Scott, and A. Klank, J. Nucl. Mater. **52**, 215 (1974).
33. M. Sawan, Radiation Limits in Copper Stabilizer of Superconducting Magnets, MARS project memo, University of Wisconsin, Madison, WI, WIS-MARS-82-029 (1982).
34. R. Coltman, Jr., and C. Klabunde, J. Nucl. Mater. **103-104**, 717 (1981).
35. M. Soll, J. Nucl. Mater. **72**, 168 (1978).
36. J. Fowler, Jr., G. Hurley, J. Kennedy, and F. Clinard, Jr., J. Nucl. Mater. **103-104**, 755 (1981).

REFERENCES--Section 3.9

37. K. I. Thommassen, V. N. Karpenko, An Axicell Design for the End Plugs of MFTF-B, Lawrence Livermore National Laboratory, Livermore, CA, UCID-19318, (1982).

REFERENCES--Section 3.10

38. Heat Adsorption Panel Development Design Summary Report, McDonnell Douglas Astronautics Company, St. Louis, Mo., Report 4511010 (1981).
39. O'Neill, R. F., et al., Convair Thermal Analyzer Computer Program No. P45600, San Diego, CA, 696-O-T-80-737, 30, (1980).
40. McAdams, W.H., "Heat Transfer at High Rates to Water with Surface Boiling," Industrial and Engineering Chemistry, 41, 1945-1955 (1949).
41. R. A. Langley et al., "Blistering," Nucl. Fusion, to be published (1983).
42. R. Behrisch, "Sputtering by Particle Bombardment II," (to be published by Springer-Verlag, Berlin, 1983).
43. Panelcoil Heat Transfer Handbook, (Dean Products, inc., Brooklyn, New York, 1975) Technical Data No. 358A.
44. D. L. Smith, et al., Starfire - A Commercial Tokamak Fusion Power Plant Study, Argonne National Laboratory, Argonne, IL, ANL/F, Vol 1, pp 80-81 (1980).
45. D. Burgreen, Design Methods for Power Plant Structures (C.P. Press, Jamaica, N.Y., 1975).
46. J. S. Przemieniecki, et al., "Thermal Stresses" in Introduction To Structural Problems in Nuclear Reactor Engineering: (Pergamon Press, New York, 1962).
47. D. S. Gelles, et al., "Void Swelling in the Molybdenum Alloy TZM Irradiated to High Fluences," J. of Nucl. Mater., 103-104, 1141-1146 (1981).
48. M. Gomolinski and G. Brebec, "Mode pour la croissance des cavities sous irradiation," J. of Nucl. Mater., 43, 59 (1972).

REFERENCES--Section 3.12

49. Clement Wong, GA Technologies, Los Angeles, CA, private communication (April 1980).
50. UWMK-III, A Noncircular Tokamak Power Reactor, University of Wisconsin, Madison, WI, UWFD-150 (1976).

REFERENCES--Section 3.14

51. B. L. Hunter, Fusion Engineering Design Center, Oak Ridge, TN, private communication (May 1982).
52. Dr. I-K Kim, General Dynamics, San Diego, CA, private communication (May 1982).
53. T. Batzer, Lawrence Livermore National Laboratory, Livermore, CA, private communication (June 1982).
54. J. Osher, Lawrence Livermore National Laboratory, Livermore, CA, private communication (July 1982).
55. M. Sawan, University of Wisconsin, Madison, WI, private communication (July 1982).
56. P. Kidd, TRW, Redondo Beach, CA, private communication (June 1982).
57. D. Pence, Science Applications, Inc., La Jolla, CA, private communication (June, 1982).
58. G. Gorker, Fusion Engineering Design Center, Oak Ridge, TN, private communication (June 1982).

SECTION 4.0. SAFETY ASSESSMENT

4.1. INTRODUCTION

Here we present a preliminary assessment of the safety aspects of TDF. Our objectives are to identify the safety issues, assist with design trade-offs, and ensure that safety considerations are incorporated in the design at a very early stage. A detailed safety analysis is neither practical nor warranted at this stage of TDF design. However, preliminary safety considerations have played a major role in TDF design.

For an overall safety assessment, the total fuel cycle must be examined. TDF uses a D-T fuel cycle, and a small quantity of lithium is used in the test modules. The deuterium and lithium involved are not radioactive. Only a small quantity of tritium, approximately 45 g/month (at 50% availability), needs to be shipped to the plant. The rest of the tritium fuel cycle, with an inventory of less than 1 kg, is contained within the plant. Thus, the radioactive hazard associated with fuel shipment is minimal.

In performing our assessment, we used the approach of the Fusion Engineering Device (FED)¹ and STARFIRE² design studies. Our safety standards, criteria, and radiological guidelines are based on DOE orders and procedures. These suggest that, under normal operations, the radiation dose rate at the site boundary should be limited to <10 mR per year to an individual, and under an off-normal or accident condition, the total dose should be limited to <1 Rem over 2 hours. Potential accidents and their sources in the TDF design are listed in Table 4-1.

Tritium is the primary source of radioactivity release. A preliminary estimate of potential radiological consequences of tritium release, based on the above guidelines, indicates the following:

- If the tritium leakage rate to the environment is to be kept to 10 Ci/day under normal operating conditions, an exclusion area boundary located 450 m away from the buildings is needed; TDF is provided with a site boundary at 500 m.
- An accidental release of tritium inside a building and on accompanying building overpressurization result in tritium leakage because buildings are not 100% leak-tight. Thus, leak-tightness, together with the site-boundary distance, determines the upper limit on the amount of tritium that can be released inside a building without exceeding the dose limit at the site boundary.

Table 4-1. Sources of safety hazards in TDF design.

Potential hazard	Possible result
Tritium inventory	Tritium release
Induced activity in the shield, magnet, structural material, and building structures	Release of activated structural material
Corrosion products in the primary coolant	Loss of collant flow to the heat dumps
Cryogenics	Release of liquid helium
rf heaters	rf-heater failure
Lithium in test modules	Lithium fire

With the site boundary at 500 m, and assuming the reactor and tritium buildings have a minimum leak rate of 1% per day, the maximum allowable tritium release is approximately 700 g. A cursory analysis indicates that the largest amount of vulnerable tritium for release is approximately 200 g.

We assessed the impact of an accidental release of cryogenics inside the reactor building. The maximum allowable liquid-helium release is approximately 2500 kg, without exceeding the 120-kPa building design pressure. The cryogenics system can be designed to provide appropriate partitioning and valving arrangements so that the maximum potential release is well below the limit.

Our preliminary safety assessment does not identify any aspect that may make the plant unsafe for the public or for personnel.

4.2. SAFETY CRITERIA AND REQUIREMENTS

The DOE Order 5480³ and the former DOE Manual⁴ provide the basis for establishing safety criteria and give several applicable guidelines. The DOE policy is that radiation exposure of individuals and population groups be limited to "as low as reasonably achievable (ALARA)". For DOE projects, most of the burden of interpreting the ALARA policy falls under the DOE safety analysis and review system⁵ described in DOE Order 5481.1. This review system determines if the proposed design meets the intent of the ALARA policy.

4.2.1. Radiological Guideline

It is important to establish some safety guidelines so that design activities may proceed before any safety review is undertaken. In particular, those guidelines that may affect design and construction of the reactor building must be identified and their applicability to TDF determined, because this issue affects the basic configuration of the facilities. Limiting the amount of radioactive release to the general public has a major effect on the reactor building design. The various operating conditions that require safety guidelines are suggested in Table 4-2. Proposed radiation-exposure guidelines for TDF are presented in Table 4-3.

The radiation exposure criteria have been considered by DOE.³⁻⁸ The DOE Order 5481.1, "Safety Analysis and Review Systems," establishes uniform

Table 4-2. Safety guidelines for TDF operating conditions.

Operating condition	Description	Probability of occurrence (P) (yr^{-1})	Safety guidelines
I	Regular planned events (e.g., normal facility operations)	$p = 1$	Normally operating modes should provide health and safety protection
II	Events that may occur several times during life of facility (e.g., minor incidents)	$1 > p > 10^{-1}$	Facility should be able to return to operation without extensive corrective action
III	Events that may occur infrequently during life of facility (e.g., more severe incidents)	$10^{-1} > p > 10^{-3}$	Facility should be able to return to operation following extensive corrective actions
IV	Limiting fault events that are not expected during facility life but are postulated because of potential significant radioactive release and, second, they represent upper bounds on failures or accidents with sufficient probability ($> 10^{-7}$ per year) to require consideration in design.	$10^{-3} > p > 10^{-7}$	Facility damage may preclude returning to operation

^aOperating condition descriptions were taken from Ref. 6.

Table 4-3. Dose-limit guidelines for TDF.

Operating conditions	Off-site		On-site	
	Limit	Design objective	Limit	Design objective
I. Normal operation (mrem/yr)	500 per individual	≤ 10 /per individual ^a ≤ 100 /per individual ^b	500 in uncontrolled area	≤ 100 in uncontrolled area
II. Incidents of frequency (mrem/yr)	170 per average population member	≤ 35 average population member ^b	5000 in controlled area	≤ 1000 in controlled area
III. Infrequent incidents (rem)	5	≤ 1	5	≤ 5
IV. Limiting faults not expected during TDF life (rem)	25	≤ 5	25	≤ 25

^aFor operating condition I, (normal operations)

^bFor operating condition II, (incident of moderate frequency)

requirements for the preparation and review of safety analyses for all DOE operations. The DOE Order 5480.1, Chapter V, "Safety at Nonreactor Nuclear Facilities," establishes additional requirements for an Environmental, Safety, and Health program. The intent of DOE policy is to assure that environmental protection, safety, and health matters are comprehensively addressed and that identifiable risks are reduced to acceptably low levels.

The design of DOE-owned nuclear facilities is based on a philosophy that the facility be capable of being safely shut down following postulated accidents. In addition, it must be designed to provide a defense-in-depth against an uncontrolled release of radioactivity to the environment.

The radiological consequence guidelines in Table 4-3 are to be interpreted as minimum guidelines to protect health and safety. In addition, DOE policy requires that radiation exposures meet ALARA policy. The dosage guidelines shown in Table 4-3 are based on review of appropriate DOE Orders, DOE Manual chapters, and Tritium Systems Test Assembly and Tokamak Fusion Test Reactor reports on preliminary safety analyses. The exposure guidelines suggested in Table 4-3 apply only to TDF and should not be construed as applying to other present or future fusion test facilities, research facilities, or reactor projects. The values quoted are for whole body exposures; however, the DOE values for other organs (hands, arms, bone, etc.) and other time periods (quarterly) are also applicable.

Magnetic Fields. We have adopted a suggested criterion⁹ as an interim exposure guideline. It suggests that personnel exposure be limited to magnetic fields less than 100 G. The magnetic-field background of earth is approximately 0.5 G. This will probably make it necessary to de-energize all the magnets before personnel are allowed to enter the reactor building. It will be also necessary to magnetically shield the control room or locate it far enough away from the reactor building so that the magnetic fields will be acceptably small.

Confinement System Criteria. The confinement of radioactive releases normally involves developing a system to provide the desired level of protection. The guide in Ref. 6 contains a discussion of confinement systems that have been reviewed and adapted to the TDF situation. Physical barriers are normally provided to prevent an uncontrolled release of radioactivity to the environment and to prevent or minimize the spread of contamination within the

facility. These barriers should be provided either between the environment and those areas of TDF that contain radioactive material or between areas in the facility that are normally expected to have contamination levels lower than those for which the barrier is designed. Such physical barriers and their associated systems (e.g., ventilation, atmosphere cleanup, etc.) make up the confinement systems.

Confinement systems should prevent or mitigate the effects of an uncontrolled release of radioactive material so that on-site and off-site doses and effluent concentrations are maintained within recommended guidelines (Table 4-3) during all facility operating conditions. Current estimates of tritium-release consequences confirm that a confinement system is necessary.

The TDF design considers the use of primary, secondary, and tertiary confinement boundaries to provide a defense-in-depth against a release of radioactive material to the environment. When warranted by lesser potential hazards, portions of the facility could be designed with fewer confinement boundaries (e.g., primary and secondary confinement or, in some cases, primary confinement alone). These confinement boundaries are defined as follows:

- Primary confinement refers to the barrier or enclosure and its associated systems for ventilation and atmosphere cleanup or off-gas treatment that will be exposed directly to potential sources of high-level radioactive contamination (e.g., hot cells, glove boxes, tanks, process piping, fume hoods, etc.).
- Secondary confinement refers to the barrier and associated ventilation system that enclose the primary confinement and that separate potentially contaminated operating areas from normally noncontaminated areas of the facility.
- Tertiary confinement refers to the structure and associated ventilation system that enclose the primary and secondary confinements and include other facility areas that are not expected to become contaminated. This is the final barrier between the outside environment and the potential contamination.

Typical characteristics of these three confinement barriers, including safety-classification recommendations, are summarized in Table 4-4. These do not establish the safety importance of any potential confinement systems.

Table 4-4. Characteristics of TDF barriers for confinement of radioactive materials.

Subject of confinement	Primary confinement	Secondary confinement	Tertiary
Type of structure	Hot cell, glove box,	Operating compartment or	Facility
buildings-- ^c			
process piping tank, fume hood, etc.	cell, e.g., vacuum pump or fueling-machine cell.	reactor, hot cell, tritium building.	
Design-basis events	Accidents, earthquakes,	Accidents, earthquakes,	Accidents,
earthquakes		and other severe conditions.	
Safety classification ^a	--	--	--
Seismic design ^b	--	--	--
Contamination	Normally contaminated.	Potentially contaminated, contaminated.	Normally not contaminated.
Operating pressure	Negative with respect	Negative with respect to	Negative with
respect			
to secondary confinement.		tertiary confinement.	to atmosphere.
Atmosphere control	Separate ventilation	Redundant ventilation	Redundant
ventilation			
or off-gas treatment	system with air cleanup.	system with air clean-	
system, atmosphere		up.	
cleanup, inert gas			
atmosphere.			

^a The required safety-class confinement barriers should be determined from the potential radioactive hazards associated with each part of the facility. The failure of any nonsafety-class barrier should not cause a loss of function in any remaining safety-class barriers.

^b Safety-class barriers should be designed to special safety-related seismic standards. Nonsafety-class confinement barriers may be designed to normal seismic standards.

Safety Classification. We suggest the following definitions, based on discussion in Ref. 6, for establishing the safety importance of various parts of TDF.

- Safety class applies to those components, systems, and structures that are required to prevent or mitigate the consequences of a postulated release of radioactive material that has the potential for causing off-site doses in excess of 0.5 rem to the whole body, or the equivalent dose to any part of the body.
- Nonsafety class components, systems, and structures are those not defined as safety class. The maximum off-site dose resulting from the failure of a nonsafety class system should not exceed 0.5 rem to the whole body, or the equivalent dose to any part of the body.

The determination of safety class requires accident analysis. Because these analyses are just beginning for most fusion components and systems, no firm determinations are made at this time. The class carries with it special quality and reliability criteria to establish a high level of assurance that safety class systems will perform their safety-related functions as designed. These special attributes include the ability to withstand severe natural phenomena. The implications of safety class designation on design are considerable and need to be worked out specifically and as needed.

Design Implications. Confinement capabilities are based on the ability of ventilation systems to maintain a controlled, continuous airflow pattern from the environment into the confinement building and then from noncontaminated areas of the building to potentially contaminated areas and then to normally contaminated areas. This airflow sequence, in conjunction with other design and operating features, should prevent the spread of contamination within the facility during normal operations, minimize the spread of contamination during abnormal conditions, and ensure adequate cleanup of exhaust-gas streams before they are released.

The TDF conditions may require some confinement barriers to be pressure-containing. If a barrier is not so designed, it cannot control the release of radioactive material whenever the pressure exceeds a minimum negative pressure. For periods when this happens, reduced credit will be given for cleanup systems in the normal exhaust path. The confinement

ventilation should be able to maintain desired air-flow characteristics when access doors or cell hatches are open. When necessary, air locks should be used to minimize the impact of facility access requirements.

Safety class confinement ventilation systems should have duplicate features to ensure airflow characteristics that will provide the necessary confinement under all credible circumstances. If necessary, the ventilation system should be supplied with safety class electric power and be operable on both on- and off-site power sources.

Confinement air cleanup systems should be provided within the major confinement ventilation-exhaust system. The air-filtration units should be functional throughout postulated accidents and should retain collected radioactive material after such accidents. The number of air-filtration stages required for any area should be determined by the quantity and type of radioactive materials to be confined. Air-filtration units should be installed as close as practical to the source of contamination to minimize ductwork contamination. Off-gas stream pretreatment should be provided upstream of the off-gas treatment system to remove or reduce the chemical concentrations that would adversely affect system operation.

The air-filtration units should be located and shielded to maintain occupational doses within the recommended guidelines during operations and maintenance. The system should facilitate periodic inspection and testing during operation and shutdown.

The level of radioactivity in the exhaust should be continuously monitored and alarmed to detect high activity. Appropriate protective actions should be taken to prevent an uncontrolled release of radioactivity to the environment and to minimize the spread of contamination within the facility.

4.3. POTENTIAL RADIOLOGICAL CONSEQUENCES

4.3.1. Tritium Release Considerations

The radiological aspects of released tritium are discussed in Ref. 10. Since our basic concern is the impact of released tritium on the general public, the potential concentration and dose rate of accidentally released tritium at the exclusion boundary are the most important parameters. These

concentrations and dose rates depend on a variety of variables, many of which are site-dependent. For example, the distance to the site-exclusion boundary, the wind, atmospheric, and topographical conditions are all important to the analysis, as are the building and ventilation designs. These variables make the calculation of dose rates very complex.

A conservative estimate (upper bound) of allowable tritium release from the plant (both normal and accidental) is given in later sections, using the dose guidelines given in Table 4-3. Actual allowable limits will be higher. Our conservatism originates from using the most stringent atmospheric and topographical conditions. In addition, no credit has been taken for an elevated exhaust from a stack.

4.3.2. Normal Releases

During normal operations, tritium can be released in liquid or gas.

Gaseous releases to the atmosphere originate from permeation through components and from leakages of valves and pumps within the plant (see Fig. 4-1). These releases would eventually exit to the atmosphere through the ventilation systems. Also, releases to the atmosphere could originate from the radioactive-waste treatment systems. The liquid releases also originate from the radioactive-waste treatment systems. Radioactive-waste water and the cooling water would be mixed at the cooling-tower blowdown before being discharged into the receiving body of water. Figure 4-2 shows the release pathway available.

We consider only the consequences of gaseous release. We anticipate the doses from liquid release to be minimal, as the quantity and potential for release are both small.

4.3.3. Normal Gaseous Release

Airborne releases of tritium are carried and diluted by the atmosphere. A certain portion is inhaled by humans, and some is deposited on the vegetation and works its way through the food chain.

The doses from all airborne pathways are calculated using an average of actual-site meteorological conditions from several light-water reactor (LWR)

4-13

Figure 4-2. Tritium-release pathways to environs.

plants.¹¹ All dose pathways are assumed present at the same location. Using the models of Ref. 12, we can characterize the dose from all pathways as:

$$D_A = CQ [X(x)]/Q, \quad (4-1)$$

where

- D_A = dose (mR/yr) from all airborne pathways,
- Q = airborne release rate from the plant (Ci/yr),
- $X(x)/Q$ = atmospheric dilution factor (s/m^3) at a distance x from the point of release,
- C = a constant converting release rate ($\text{Rem}\cdot\text{m}^3/\text{Ci}\cdot\text{s}$) to dose for all pathways.

e evaluated the constant C by using the recommended values and models of Ref. 12. For the atmospheric dilution factors we used an average of 29 LWR sites¹¹ as a function of distance. A functional dependence of X/Q with respect to distance was derived from the data of Ref. 11. This dependence is of the form

$$X(x)/Q = 0.078 \cdot x^{-1.37}, \quad (4-2)$$

where x is the distance in meters.

Equation (4-2) is valid for the region from 800 to 80,000 m from the plant (data from Ref. 11 covers this region only). As the distance from the plant decreases to less than 800 m, the building wake effects begin to dominate the X/Q ; thus Eq. (4-2) tends to be overconservative.

The dose calculation using the models of Ref. 12 indicates that the ingestion of vegetation, inhalation, and the drinking of milk are the most important contamination pathways (53%, 25%, and 16%, respectively). Since tritium does not contribute to the external whole-body dose or skin dose, only the organ-dose guideline (<10 mR per year) applies. Equation (4-1) is compared to this guideline, and the release rate is calculated for different distances and plotted on Fig. 4-3.

Figure 4-3 can be used in two ways. With a given exclusion area boundary, one can obtain the allowable tritium-release rate from the plant

Figure 4-3. Maximum-allowable tritium-release rate from plant buildings under normal conditions.

through the ventilation system and, in turn, determine the ventilation flow rate from the building. Alternatively, with a given release rate (design goal), one can obtain the required boundary. Thus, with a boundary 500 m from the experimental complex, the allowable tritium release is ~11 Ci per day.

Alternatively, using a release rate of 10 Ci per day of tritium as an example, Fig. 4-3 indicates that an exclusion-area boundary of ~450 m results in an acceptable dose (<10 mR per year) at the site boundary. The tritium-cleanup and ventilation-system designs are targeted for this goal.

4.3.4. Accidental Releases

As soon as it is known that an accident has occurred in the plant, all normal operations are suspended and emergency procedures are put into operation. Normal ventilation exhaust of the buildings is discontinued and liquid discharges are stopped, leaving atmospheric leakage as the only release pathway available for tritium.

The dose levels for mandatory evacuation (1 rem whole-body dose) are used to evaluate the short-term effects of accidental tritium release. Of the dose pathways available to man, only inhalation is important for the zero to 2-hour dose. Immersion in a cloud of tritium does not contribute to the dose, because tritium is a weak beta emitter; clothing stops it. The other pathways can be avoided by importing food from outside the affected area.

The dose from an accidental release is evaluated as a function of exclusion area boundary for several building leakage rates. The dose is then compared to the mandatory evacuation whole-body dose, and the maximum tritium inventory is calculated.

The inhalation dose from tritium is given by

$$D = X(x)/Q \cdot BR \cdot A_R \cdot DCF, \quad (4-3)$$

where

D = dose in rem
 $X(x)/Q$ = 2-hour atmosphere-dispersion factor (s/m^3),
 A_R = tritium activity (Ci) released from building,
 BR = breathing rate (m^3/Ci)
 DCF = dose conversion factor (rem/Ci).

The accident X/Q as a function of distance is calculated using Ref. 12 and is evaluated for distances ranging from 200 to 70,000 m.

In calculating the activity we assume that no removal (i.e., cleanup) systems exist to remove tritium from the building atmosphere. The activity released is given by

$$A_R = A_0(1 - e^{-\lambda_L t}) \quad , \quad (4-4)$$

where

A_0 = total activity (Ci) released inside the building,

λ_L = building leakage rate, (hr^{-1}),

t = time (2 hrs) after accidental release.

The initial activity available for release (A_0) is conservatively assumed to be instantly available for release at the start of the accident. In reality it would be released over time ranging from a few seconds to several hours, depending on the severity of the accident.

Equations (4-3) and (4-4) are solved for A_0 and are shown in Fig. 4-4 as a function of exclusion-area distance for different building leakage rates. Also shown is the tritium concentration in the 80,000- m^3 reactor building.

We make several conservative assumptions to obtain a conservative upper bound of allowable tritium release in a building. A value of $5.0 \times 10^{-4} \text{ m}^3/\text{s}$ is used for breathing rate, the rate for very strenuous activity at the site boundary. The dose conversion factor used, 158 rem/Ci, is also very conservative. No credit is taken for an elevated release from a stack, which allows increased dispersion and reduced doses at the site boundary. The atmospheric dispersion factor (X/Q) assumes a ground level release, which is more realistic than an elevated release because all the ventilation systems are closed after an accident.

Figure 4-4 shows that when the site boundary is located at 500 m, the maximum allowable tritium release in a building designed for a leak rate of 1% per day is ~700 g. If the building is designed for a 10% per day leak rate, a release of about 70 g results in a 1-rem dose in 2 hours at the site boundary.

Figure 4-4. Maximum-allowable tritium-release in plant buildings from an accidental release.

Thus, by controlling the three variables--the building leak rate, the location of the site boundary, and the maximum credible amount of accidental tritium release--the TDF plant can be designed to keep the dose consequences of an accidental tritium release within the acceptable limit. The site boundary is usually the most controllable of the three parameters. We discuss the remaining two in the following subsections.

4.3.5. Building Leak Rate

Two major sources of leakage from a building are first, penetrations of pipings, conduits, and the like, and, second, cracks in the concrete. Preventive measures generally used to reduce leak rates include sealing the penetrations, prestressing the concrete, and steel-lining the inside surfaces of floor, wall, and ceiling. All these measures are used in LWR containment buildings, and a volume leak rate ranging from 0.1 to 1.0% per day is obtained when the accidental internal-pressure buildup is about 500 kPa. For the TDF reactor building, neither prestressing nor steel lining is envisioned. Epoxy coating is used for the building internal surfaces and is considered adequate for tritium confinement (reduced tritium permeation into concrete). As a result, it is difficult to ascertain TDF's leak rate. However, considering that the accidental pressure buildup is not expected to exceed 120 kPa, the leak rate should be limited to a range of 1% to 10% per day.

4.3.6. Credible Tritium Release

Without a detailed analysis identifying potential accident scenarios, we cannot determine the amount of tritium release in the worst credible situation. As a cursory assessment, however, the following seven major cases are examined:

- Case 1. Failure of fueling system and escape of tritium into reactor building.
- Case 2. Vacuum-vessel leak.
- Case 3. Loss of cryogen supply to the end cell cryopanel.
- Case 4. Rupture of duct connecting the end cell and the tritium processing system.
- Case 5. Combination of Case 3 and Case 4 in a seismic event.

- Case 6. Loss of cryogen supply to the tritium-processing system.
- Case 7. Rupture of isotopic primary vessel of separation unit (ISS)

Case 1: Tritium is pumped through the neutral beam injector at the rate of 136 g/hr. In an accident, isolating valves can be closed in a matter of seconds resulting in a release of approximately 10 to 20 g, which is well below the allowable limit.

Case 2: A vacuum-vessel leak will not result in much tritium release because the pressure inside the vessel is less than the building atmosphere. The plasma from the vessel can be evacuated and tritium feeding stopped before the pressure is equalized.

Case 3: About 200 g of tritium is available for outgassing from the cryopanel in each end cell. In case of a complete loss of cryogen, all the tritium may be outgassed and will be pumped into the tritium-processing system without any release into the building.

Case 4: If a duct connecting the end cell and the tritium-processing system ruptures, events will be similar to those of Case 2. No significant tritium release is anticipated.

Case 5: If we postulate a complete loss of cryogen (see Case 3) and the rupture of the connecting duct (Case 4), the entire inventory of one end cell (200 g) is available. How much of this will be released into the reactor building and how much will be pumped into the tritium-processing system are difficult to estimate without detailed analyses. In the worst case, the entire amount will be released.

Case 6: In a loss of cryogen supply to the tritium-processing system, no tritium release is anticipated, as the system is designed to transfer the tritium from the major components (e.g., ISS units) to uranium beds.

Case 7: In the tritium-processing system, the components containing tritium are provided with three confinement barriers. The inner vessel serves as the primary containment, while a glove box surrounding the inner vessel serves as secondary and the tritium-processing building serves as tertiary confinement. The probability of simultaneous failure of the primary and secondary confinements is extremely small. Even if both confinements fail, the maximum release will not exceed 200 g as each of the ISS units contains that amount.

In summary, assuming 200 g as the maximum credible amount of accidental release of tritium, either of the following design alternatives is adequate to

mitigate the consequences of normally and accidentally released tritium:

- Alternative 1. A design building-leak rate of 10% per day and an exclusion area boundary location of 950 m.
- Alternative 2. A design building-leak rate of 5% per day and an exclusion area boundary location of 500 m.

If for some yet-unidentified reasons, a leak rate of less than 1% per day is needed, two possibilities exist: a leak-tight steel lining or a double-confinement building, consisting of a steel enclosure surrounded by a concrete building (Fig. 4-5). A detailed study specific to TDF is needed to determine which is more economical.

4.3.7. Activated Structural Materials

In addition to tritium, there is induced radioactivity in the first wall, the shield, and the heat-dump materials. This activity is much less mobile than that of tritium. No credible mechanism that would cause melting of the structural material has been identified. Since there is a bulk shield around the plasma chamber, the likelihood of this activity becoming mobile, even without plasma shutdown, is not considered likely. Nevertheless, because some of this activity is in the form of corrosion products, it imposes constraints on maintenance and repair activities, and therefore strongly influences design and choice of structural materials. Though not present at the beginning of reactor operation, this radioactivity will increase with time and after several weeks of operation reach a level that is significant for access considerations. However, the associated afterheat is small and is distributed through a large volume, thus making it easier to ensure adequate cooling in emergency shutdowns.

Special effort is given to the materials selection to reduce both long-term radioactivity and radwaste storage. The reactor is also designed to be accessible within 24 hours after shutdown for hands-on maintenance and later for maintenance of reactor internals by remote operations.

4.3.7. Loss-of-Flow Accident

The pressurized water coolant for removing heat in the beam and end cell dumps is subcooled to a minimum of 100 °C. If the plasma is not shut down

Figure 4-5. Single confinement compared to double confinement.

in the event of a loss-of-flow accident, such as a loss of pumping power or a loss of pressurization, the coolant will go first into nucleate boiling, then into film boiling, and finally into burnout. Although the beam and end cell dumps have a large thermal inertia, if the plasma is not shut down within a short time after loss of flow, the wall material will ablate and will extinguish the plasma. However, the decay heat is not sufficient to cause melting.

4.3.8. Liquid Helium

Liquid helium is used for maintaining cryogenic temperatures in the superconducting coils and the cryopumps. In the event of coil break or helium pipe break, the liquid helium will be spilled into the reactor building, which will be maintained at a slightly negative differential pressure. On exposure, liquid helium will flash into a two-phase vapor-liquid mixture. The volume of the reactor building is large, but a certain degree of pressurization will result from the helium vapor; we must ensure that the design pressure of the reactor building will be greater. Furthermore, if the superconducting coils are in operation, the loss of helium will drive the conductors normal and could lead to melting of coils, which in turn could aggravate the effects of the helium spill.

A preliminary calculation shows that a liquid-helium release exceeding 2500 kg is needed to exceed the 120-kPa design pressure of the reactor building. The cryogenics system may be designed so that a single failure does not result in a release larger than this.

4.3.9. Rf Heating

We have made a preliminary assessment of the biological hazard from the rf heating system². The permissible exposure to rf waves is 10 mW/cm². Leakage will not exceed this unless the rf system plumbing cracks, and even so, the effect of a crack would be readily detectable from changes in system behavior. In addition, rf generation requires high voltages, and these produce x rays. The TDF design will incorporate features for reducing these biological hazards.

4.3.10. Lithium Fire

The current design does not require the use of lithium, but relatively small quantities may be needed for test modules. Violent reactions may be expected if lithium contacts water (or concrete, which contains water). However, if it is spilled on steel in air, the lithium reacts with the nitrogen in the air and several minutes elapse before the reaction becomes severe. Flooding the atmosphere with an inert gas such as argon stops the reaction. The TUF design criteria must contain any spill from a test module in a steel sump or catch basin that can be so flooded. Concrete surfaces that could be contacted by the lithium should be lined with steel, and lithium piping should have double walls to reduce chances of a leak. We will design the facility to accommodate whatever breeding-blanket test materials are chosen, rather than attempt to retrofit the facility.

4.4. SAFETY FEATURES

To develop a safe design for the plant, we take several steps to reduce or eliminate each safety-related problem. The most important are the use of a leak-tight, neutron- and gamma-shielded, and seismic- and tornado-protected reactor building. The major safety-related design steps are listed in Table 4-5, together with their rationale. The following paragraphs discuss the most important safety features.

4.4.1. Pressure Vessel Building

One technique for reducing the consequences of radioactive release is to design the building as a pressure vessel that can withstand the accidental release. The pressure vessel will contain all the radioactivity from an unexpected release until slower acting systems can be used to reduce the building pressure and remove the radioactivity. Our rectangular reactor building can withstand some low amount of overpressure (about 120 kPa) and still maintain a low leakage rate (on the order of 1 to 10% of the building volume per day).

Table 4-5. Design steps for reducing safety-related hazards.

Hazard	Objective	Design step
Tritium (T) inventory	Reduce T accident potential	Reduce T
	Reduce worker exposure	Partition T Use remote
maintenance	Reduce leak potential	Use
double-walled piping in lines		plasma-exhaust
cleanup system	Reduce T concentration in buildings	Atmospheric T-
	Reduce T permeation of containment loop	Use T-cleanup
system for	Provide better dispersion of released gases	primary cooling Use vent stack
Activation selection for products	Reduce long-term activity	Use materials
	Reduce worker exposure	dumps and shield
maintenance	Provide biological shielding	Use remote
reactor-building	and overpressure	Increase
		wall thickness
cryogen	Provide protective mechanism	Partition
Lithium fire	Reduce lithium fire	
floodings	Provide steel-lined potential	catch basin and with inert gas.

4.4.2. Reduce Vulnerable Tritium

One design objective of the TDF systems is to reduce the amount of tritium that can be released in an accident. The amount of vulnerable tritium is made as low as practical by reducing the inventories in the processing equipment, e.g., recycling cryovacuum pumps more often. The tritium system is also partitioned so that an accident that causes the release of tritium from one part of the system does not cause a release from other parts of the system or from other equipment. For example, isolation valves between the fueling device and the plasma chamber are used to prevent tritium release from the fueling device in the event that the integrity of the plasma-chamber vacuum is lost.

4.4.3. Partition Cryogenics

Since the potential impact of cryogen evaporation is quite significant, another design objective is to reduce the amount of cryogen that could be released by a single accident. This is accomplished with piping arrangements and valving so that a major cryogen leak in one part of the system does not cause the whole system to blow down. For example, each superconductor coil is piped separately from the storage Dewar.

4.4.4. Vent Stack

A vent stack can significantly reduce the consequences of tritium release. *By releasing radioactive effluents well above ground level, we* achieve better mixing and greater dispersion, which results in a significantly lower dose at the site boundary. The stack-effluent consequences are further reduced by processing or diluting the effluent stream before release.

4.4.5. Site Characteristics

Because site characteristics can have a major influence on the consequences of radioactive releases, site selection is very important. Criteria such as site-exclusion boundary and the population density of the surrounding areas directly affect the dose rate at the boundary as well as the

postulated average population dose. For example, if the distance from the plant to the site boundary is long, the consequences of tritium release will be significantly reduced. The exclusion area boundary of 500 m appears adequate for TDF.

4.4.6. Discussion

Several techniques are available to reduce the probability, and hence consequences, of radioactivity release. Incorporating these techniques into to provide adequate protection from tritium release appears feasible. Alternatives can be selected based on potential cost and schedule impact.

4.5. ENVIRONMENTAL CONSIDERATIONS

An environmental review is needed to identify potential environmental hazards. All elements, from the procurement of raw materials to the handling and disposing of radioactive wastes, should be considered. Under DOE auspices, we are preparing a report to assess the *technical* status of environmental concerns related to the total fusion energy program. Through our study we will provide guidance for minimizing hazards in this area. Environmental studies for TDF should be consistent with this generic effort.

The major environmental concerns for TDF are exposure to radiation and magnetic fields. Radiation exposure has the greatest potential for impact both within and outside the plant, under all conditions. Since *first-generation fusion machines* will use the D-T fuel cycle, technology for tritium control and cleanup are being further developed to lessen any possibility of release to the environment. The ISTA project will demonstrate the effectiveness of these technologies.

Exposure to magnetic fields is a potential concern to on-site personnel. Present information about the biological effects of magnetic fields is sparse but generally suggests that health effects are not significant.

Aside from these, the environmental impacts of TDF are expected to be either less than or equal to those of other large fusion projects. Potential impacts stem from thermal discharge, land use, and toxic chemical-waste effluent. None of these is expected to cause abnormal concern or delay the project, and all can be handled with current technology.

Following is a list of the key environmental hazards in approximately their order of importance. As usual, it is difficult to distinguish between safety and environmental issues. Our study did not specifically address all of them.

- Radionuclide releases (routine and accidental),
- Radioactive waste transportation and disposal,
- Magnetic fields,
- Land use,
- Thermal discharge,
- Radioactive liquid and gas releases,
- Chemical releases,
- Socioeconomic impact.

4.6. KEY SAFETY ISSUES

The key issue is the potential release of radioactive materials from the plant, and the basic concern is the impact of this release (primarily of tritium) on the general public. We use a deterministic method to obtain information on the upper bounds of the consequences of radioactivity release. But a detailed probabilistic-risk assessment is needed in the future, because the events that lead to accidents or failures of the reactor and its systems are probabilistic. The causes of accidents and sources of radioactivity release will be identified. Regardless of whether the evaluation is probabilistic or deterministic, tritium is the major source of concern. Utmost care is needed to design systems and components for transport, storage, pellet formation, and tritium processing to provide the required multiple barriers against its release. These barriers will include the following: double-walled piping system for tritium transport; glove boxes around the tritium-processing equipment; redundant systems and components, e.g., atmospheric tritium-recovery system, emergency detritiation system.

REFERENCES--Section 4

1. Fusion Engineering Device Design Description, Vol. 2, Oak Ridge National Laboratory, Oak Ridge, TN ORNL/TM-7948/V2 (1981).
2. STARFIRE - A Commercial Tokamak Fusion Power Plant Study, Vol. II, Argonne National Laboratory, Argonne, IL, ANL/FPP-80-1 (1980).
3. "Safety of Non Reactor Nuclear Facilities," DOE Order 5480.1, Chapter V, Department of Energy, Washington, D. C. (1981).
4. "Standards for Radiation Protection," DOE Manual, Chapter 0524, (from Energy Research Development Agency Manual, Chapter 0524, approved March 30, 1979.)
5. "Safety Analysis and Review System," DOE Order 5481.1, Department of Energy, Washington, D. C. (1979).
6. Draft Nonreactor Nuclear Facilities: Standards and Criteria Guide, U.S. Department of Energy, Washington, D.C. (1981).
7. Tritium System Test Assembly, Preliminary Safety Analysis Report, Los Alamos Scientific Laboratory, Los Alamos, NM (1979).
8. Tokamak Fusion Test Reactor, Preliminary Safety Analysis Report, Princeton Plasma Physics Laboratory, Princeton, NJ (1978).
9. E. L. Alpen, U.S. Department of Energy, Washington, D.C. private communication (1980).
10. P. S. Rohwer and W. H. Wilcox, "Radiological Aspects of Environmental Tritium," Nuclear Safety, 17, (2) (1976).
11. SWESSAR-PI, Section 2.3, Stone and Webster Standard Safety Analysis Report (Stone and Webster, Boston, MA, 1974).
12. "Calculation of Annual Doses to Man from Routine Releases of Reactor Effluent for the Purpose of Evaluating Compliance with 10 CFR Part 50," Regulatory Guide 1.109, Appendix I, U.S. Nuclear Regulatory Commission, Washington, D. C. (1977).

SECTION 5.0. COST ESTIMATES

5.1. INTRODUCTION

One of the goals of the engineering design effort in the TDF program was to describe each subsystem in enough detail to allow the preparation of a credible cost estimate. An estimate of total capital cost was generated using three different methods. All three methods used the same direct costs with the add-ons varying to represent the experience of the three costing organizations (Bechtel Group, FEDC, and LLNL). The costs presented here were assembled by Bechtel, with the raw data provided by each subsystem design group.

The resulting total capital costs are, in millions of dollars: Bechtel - \$1,290, FEDC - \$1,147, LLNL - \$960 $\pm$ 20%. Although there are significant differences in these estimates, they are all within the uncertainty associated with completeness of the design work. Our assessment is that the cost is about what should be expected of a device about the size of MFTF with the addition of requirements for tritium, activation, and high-duty cycle.

5.2. TOTAL CAPITAL COSTS

The total estimated capital costs are shown in Table 5-1.

The estimate assumes that:

- Our scope will be that of a prime contractor responsible to the owner for engineering, procurement, and construction.
- Equipment and materials will be procured from U.S. sources, and lead times will be able to support the project schedule without cost penalties.
- Sufficient manual and nonmanual personnel to complete the project on schedule is available in the project vicinity.
- Existing water sources and power will be adequate for the projects requirements.

5.2.1. Direct Costs

The direct costs shown in Table 5-2 include the plant equipment, materials and subcontracts, and installation labor associated with the permanent facilities, systems, and equipment.

Table 5-1. TDF capital cost summary, as of mid-1982. Dollars are in millions.

<u>Description</u>	<u>Cost</u>
Direct cost	\$ 653
Indirect cost	<u>110</u>
Total field cost	763
Home office cost	191
Owner's cost (operating entity)	38
Contingency	<u>298</u>
Total capital cost	<u>1,290</u>

Table 5-2. YDF direct cost summary, as of mid-1992. Dollars are in thousands.

Account	Description	Component engineering	Plant equipment	Material and subcontract	Installation labor	Total
21	Structures and site facilities	--	--	80,640	53,760	134,400
22.01.01	Vacuum vessel and beam dumps	877	13,336	--	1,825	16,038
22.01.02	Shield	--	5,568	--	1,634	7,202
22.01.03	Magnets	7,394	41,334	--	1,958	50,686
22.01.04	Heating and fueling	27,133	125,088	--	10,834	163,855
22.01.05	Primary structure	146	1,775	--	374	2,295
22.01.06	Reactor vacuum systems	2,370	11,847	--	2,370	16,587
22.01.07	Power supply switching and energy storage	700	4,667	--	537	5,904
22.02	Heat transport system	--	10,500	--	3,500	14,000
22.03	Cryogenic system	--	12,420	--	6,650	19,070
22.04	Radioactive waste treatment and disposal	--	2,250	--	750	3,000
22.05	Fuel handling and storage	--	46,990	--	7,670	54,660
22.06	Other reactor plant equipment	--	58,950	--	3,100	62,050
22.07	Instrumentation and controls	--	--	26,645	--	26,645
24	Electric plant equipment	--	12,650	--	2,530	15,180
25	Miscellaneous plant equipment	--	4,500	--	500	5,000
BDP	Equipment	--	8,000	--	--	8,000
BDP	Piping	--	--	13,000	7,000	20,000
BDP	Instrumentation	--	--	1,500	700	2,000
BDP	Electrical	--	--	10,000	16,000	26,000
Total direct cost		38,620	360,675	131,585	121,692	652,572

The engineers and organizations responsible for the design and specification of various equipment estimated their own involvements as shown in Table 5-3. A cost data collection methodology was developed to ensure that all components and systems were included, and to ensure that all cost data had a common basis.

The work breakdown structure was first arranged to correspond to the standard accounts for fusion cost estimates developed by Battelle.¹ The engineer responsible for the account developed the work breakdown details below the level shown on Table 5-2. (By reviewing these detailed account breakdowns before actual estimating began, the cost estimating group was able to resolve omissions and redundancies early in the project.)

The responsible engineers were then provided with equipment cost forms, which were used to transmit cost data on equipment, component engineering, and installation. The equipment cost includes the base mechanical equipment plus all required drives, piping, instrumentation, and electrical materials furnished with the equipment before delivery to the jobsite. It excludes installation, freight, vendor representatives, contingency, and component engineering. The component engineering cost, included as a separate item, is the cost associated with the design of a first-of-a-kind component. Installation costs were reported as direct installation hours excluding nonmanual supervision. Allowances for vendor representatives, freight, and installation materials were developed by the cost estimating group. Materials include all permanent plant materials usually purchased in bulk quantities, such as earthwork, formwork, rebar and embedded metal, concrete, steel, pipe, valves and connectors, electrical equipment, wire and cable, conduit and cable tray, computers and control modules, field instruments and bulks.

To simplify the estimate's preparation and accurately account for all field labor, subcontracts are generally not included. Instead, we assume that all field labor is performed by the prime contractor's work forces and results in essentially the same total estimated construction cost as that based on some subcontractor work. The instrumentation and control (I&C) system is the only exception to this approach. TRW estimated the I&C package on a subcontract basis; this made it easier to integrate the I&C estimate into the total plant estimate. Bechtel personnel prepared the materials estimate based on the scope definition developed at the FEDC.

Table 5-3. TDF capital cost accounts.

<u>Account</u>	<u>Description</u>	<u>Responsible Organization</u>
21	Structures and site facilities	Fusion Engineering Design Center (FEDC)
22	Reactor plant equipment	
22.01.01	Vacuum vessel	General Dynamics (GD), LLNL
22.01.02	Shield	TRW
22.01.03	Magnets	GD, FEDC
22.01.04.01	Beam heating	TRW, FEDC
22.01.04.02	RF heating	FEDC
22.01.04.03	Fueling	FEDC
22.01.05	Primary structure and support	GD
22.01.06	Reactor vacuum system	LLNL
22.01.07	Power supply switching and energy storage	FEDC
22.02	Heat transport system	FEDC
22.03	Cryogenic system	GD
22.04	Radioactive waste treatment and disposal	FEDC
22.05	Fuel handling and storage Systems	Science Applications, Inc.
22.06	Other reactor plant equipment	FEDC
22.07	Instrumentation and control	TRW
24	Electric plant equipment	FEDC
25	Miscellaneous plant equipment BOP Bulk materials	FEDC Bechtel

Installation labor includes all manual labor directly involved in erecting the permanent plant facilities, systems, and equipment. Typically, the study participants provided installation manhours and Bechtel reviewed them. Where the manhour estimates were omitted or appeared to be inconsistent with general construction experience, Bechtel developed appropriate allowances. We assumed an average U.S. labor rate of \$22.00/hr including all applicable premiums, fringes, taxes, and insurance addit ves based on a review of current U.S. craft labor agreements.

5.2.2. Indirect Costs

Indirect costs are those that cannot be directly identified with specific permanent plant facilities, systems, and equipment. This includes

- Temporary construction facilities: temporary buildings, working areas, roads, parking areas, utility system, and general purpose scaffolding.
- Miscellaneous construction services: general job cleanup, maintenance of construction equipment and tool maintenance, material handling, surveying, and watchmen and guards.
- Construction equipment and supplies: construction equipment, small tools, consummable supplies, and purchased utilities.
- Field office: craft supervision, engineering, procurement, scheduling, personnel administration, warehousing, first aid, and field office operating costs.
- Preliminary checkout and acceptance testing: material and equipment testing.
- Project insurance: public liability, property damage, and builder's risk insurances.

The costs of the facilities, supplies, and services were estimated as a percent of direct labor costs based on an evaluation of Bechtel experience with similar projects. This analysis resulted in an allowance for indirect costs equal to 90% of direct labor.

Home Office Costs. Home office costs include preliminary, definitive, and detail design engineering, cost engineering, scheduling, procurement, and project and construction management services.

The estimate of home office costs is based on an analysis of experience with nuclear facilities and similar projects. This resulted in an allowance for home office costs equal to 25% of total field cost.

Owner's Costs. Owner's costs are those incurred by the project during engineering and construction that are not directly attributable to the prime contractor. They include such items as

- Program administration and management,
- Preliminary engineering,
- Site investigation,
- Research and development,
- Initial charges, inventory, and spares,
- Startup and training.

Because TDF will be a government-owned and -operated facility, we have included a relatively small allowance of 4% of the total field and home office cost.

Contingency. This is an allowance for the design's uncertainty in quantity, pricing, or productivity that is under the control of the engineer/constructor and within the defined scope of the project. Implicitly, the allowance will be spent during design and construction, and it cannot be considered as a source of funds for overruns or additions to the project scope.

Contingency excludes factors outside the engineer's/constructor's control, such as scope changes that force major regulatory, operating, safety, and environmental standards; site selection; and political and financial market conditions. Contingency is 30% of the total of field, home office, and owner's costs.

5.2.3 Discussion of Estimate

Cost comparisons. The total capital cost summary assumes that TDF will be constructed at an undeveloped site with the engineer/constructor as the prime contractor. TDF may also be constructed at an existing national laboratory, with the laboratory as prime contractor, and existing facilities may be used. To estimate the indirect costs, we employed the factors developed at the Fusion Engineering Design Center (FEDC).²

The FEDC definition of total direct cost excludes component engineering and installation labor for the reactor plant equipment. However, labor costs for facilities that would normally be subcontracted to industry are included as direct costs in the FEDC algorithms. These include

- Structures and site facilities (account 21)
- Heat transport system (account 22.02)
- Radioactive waste treatment and disposal (account 22.04)
- Other reactor plant equipment except remote maintenance equipment (account 22.06), and
- Miscellaneous plant equipment (account 25).

The total direct cost of TDF, compatible with FEDC is shown in Table 5-4. This is the cost that should be used in comparing TDF to the FED designs developed by the FEDC. The total cost, developed by the FEDC method, is shown in Table 5-5.

The factors are comparable to the costs shown in Tables 5-1 and 5-2 if engineering and management is identified as the component engineering, home office and owner's cost, and if installation is defined as the labor not included in the FEDC direct cost. The difference between the two total cost estimates is the indirect (field) cost. This will vary according to the applicability and availability of existing facilities for TDF construction.

The capital cost was also estimated by LLNL, based on construction at LLNL and using existing facilities and services. Using the direct cost from in Table 5-2, LLNL applies the following factors:

- Construction equipment and services--10%,
- LLNL project engineering--5%,
- Systems checkout and acceptance--5%,
- Project management--2.5%.

At this stage in the study, an uncertainty in direct cost of $\pm 20\%$ is applied to account for design and scope changes before a commitment to construct. LLNL estimates the contingency is estimated to be 20%. The capital cost is then estimated as shown in Table 5-6.

Direct cost elements. To display the major cost elements, and to provide a basis for scaling the TDF component costs, we show a further breakdown of the cost accounts in Table 5-7. We used the direct costs from Table 5-2, including equipment, material, component engineering, and labor.

Table 5-4. Total direct cost of TDF, compatible with FEDC. This is the cost that should be used in comparing TDF to the FED designs developed by the FEDC.

Item	Cost (\$M)
Equipment cost	360.7
Material and subcontract	131.6
Subcontract labor	<u>58.8</u>
Total direct cost	551.1

Table 5-5. Total cost developed by the FEDC.

Item	Cost (\$M)
Total direct cost	551
Engineering and management (45%)	248
Installation (15% of total direct cost)	<u>83</u>
Subtotal	882
Contingency (30%)	<u>265</u>
Total cost	1147

Table 5-6. Estimated capital cost.

Item	Cost \$ (M)	
Direct cost	653	± 131
Indirect cost (22.5%)	<u>147</u>	<u>± 29</u>
Total field cost	800	± 160
Contingency (20%)	<u>160</u>	<u>± 32</u>
Total capital cost	960	± 192

Table 5-7. Direct cost elements, as of mid-1982. Dollars are in thousands.

Item	Cost
Reactor vessel total	<u>23,417</u>
Central cell vessel	210
Anchor/end cell vessel	13,944
Central cell shield	6,202
Anchor/end cell shield	1,000
Support structure	2,061
Magnets total	<u>50,920</u>
Central cell coils	32,995
Anchor/transition coils	8,001
Choke coils	2,265
Anchor vacuum vessel	7,425
Central intercoil support	234
Heating total	<u>162,372</u>
Central cell NBI	121,758
Anchor/transition NBI	24,980
RF heating	13,750
Beam dumps	1,884
Fuel system total	<u>58,027</u>
Fuel injection	3,367
Purification and preparation	14,153
Water cleanup systems	19,868
Air cleanup systems	11,978
Other tritium processing	8,661
Maintenance equipment total	<u>59,045</u>
Reactor building	22,637
Hot cell	24,551
Special purpose	11,857
Reactor services total	<u>49,657</u>
Vacuum system	16,587
LHe system	11,470
LN ₂ system	7,600
Reactor cooling	6,200
Facility cooling	1,300
Heat rejection	6,500

Table 5-7 (cont'd)

Item	Cost
Electrical systems total	<u>21,089</u>
Magnet power supplies	4,391
Magnet control systems	1,518
Pulsed power substation	11,508
Facility power substation	2,040
Emergency power	1,632
Instrumentation and control total	<u>26,645</u>
Supervisory control/data system	13,055
Local control instrumentation	10,601
Personnel safety/interlocks	2,989
Facilities total	<u>201,400</u>
Reactor building	40,000
Hot cell	24,200
Reactor auxiliary building	17,200
Power supply building	6,000
Other buildings	30,000
Site improvements	14,400
Cooling system structures	2,600
Radioactive waste treatment system	3,000
Miscellaneous plant equipment	8,000
Bulk materials	56,000
Total direct cost	<u>652,572</u>

^aIncludes tritium building.

REFERENCES--Section 5

1. S.C. Schulte, T.L. Willke, and J.R. Young, Fusion Reactor Design Studies - Standard Accounts for Cost Estimates, Battelle-Pacific Northwest Laboratory, Fusion Engineering Design Center Oak Ridge, TN, PNL-2648 (1978).
2. Fusion Engineering Device Design Description, Fusion Engineering Design Center, Oak Ridge, TN, ORNL/TM-7948 (1981).

CJ/kt