

**GA-A15054
UC-77**

GAS-COOLED FAST BREEDER REACTOR

**QUARTERLY PROGRESS REPORT FOR THE PERIOD
MAY 1, 1978 THROUGH JULY 31, 1978**

**by
PROJECT STAFF**

**Prepared under
Contract EY-76-C-03-0167
Project Agreement No. 23
for the San Francisco Operations Office
Department of Energy**

**GENERAL ATOMIC PROJECT 3228
DATE PUBLISHED: AUGUST 1978**

GENERAL ATOMIC COMPANY

DISCLAIMER

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

DISCLAIMER

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

PROGRESS REPORT SERIES

GA-5537	November 1, 1963 through July 31, 1964
GA-6667	August 1, 1964 through July 31, 1965
GA-7645	August 1, 1965 through July 31, 1966
GA-8107	August 1, 1966 through July 31, 1967
GA-8787	August 1, 1967 through July 31, 1968
GA-8895	August 1, 1968 through October 31, 1968
GA-9229	November 1, 1968 through January 31, 1969
GA-9359	February 1, 1969 through April 30, 1969
GA-9639	May 1, 1969 through July 31, 1969
GA-9811	August 1, 1969 through October 31, 1969
GA-9838	November 1, 1969 through January 31, 1970
GA-10517	February 1, 1970 through January 31, 1971
GA-10645	February 1, 1971 through April 30, 1971
GA-A10803	May 1, 1971 through July 31, 1971
GA-A10906	August 1, 1971 through July 31, 1972
GA-A12003	November 1, 1971 through January 31, 1972
GA-A12165	February 1, 1972 through April 30, 1972
GA-A12252	May 1, 1972 through July 31, 1972
GA-A12421	August 1, 1972 through October 31, 1972
GA-A12530	November 1, 1972 through January 31, 1973
GA-A12635	February 1, 1973 through April 30, 1973
GA-A12728	May 1, 1973 through July 31, 1973
GA-A12824	August 1, 1973 through October 31, 1973
GA-A12894	November 1, 1973 through January 31, 1974
GA-A13021	February 1, 1974 through April 30, 1974
GA-A13148	May 1, 1974 through July 31, 1974
GA-A13238	August 1, 1974 through October 31, 1974
GA-A13379	November 1, 1974 through January 31, 1975
GA-A13458	February 1, 1975 through April 30, 1975
GA-A13565	May 1, 1975 through July 31, 1975

GA-A13766	August 1, 1975 through October 31, 1975
GA-A13815	November 1, 1975 through January 31, 1976
GA-A13868	February 1, 1976 through April 30, 1976
GA-A13975	May 1, 1976 through July 31, 1976
GA-A14112	August 1, 1976 through October 31, 1976
GA-A14240	November 1, 1976 through January 31, 1977
GA-A14358	February 1, 1977 through April 30, 1977
GA-A14492	May 1, 1977 through July 31, 1977
GA-A14613	August 1, 1977 through October 31, 1977
GA-A14771	November 1, 1977 through January 31, 1978
GA-A14928	February 1, 1978 through April 30, 1978

ABSTRACT

The tasks of the gas-cooled fast breeder reactor (GCFR) program which are supported by the Department of Energy include development of GCFR fuel, blanket, and control assemblies; development of the pressure equalization system for GCFR fuel; out-of-pile loop facility test programs; fuels and materials development; fuel, blanket, and control rod analyses and development; nuclear analysis and reactor physics for GCFR core design; shielding requirements for the GCFR; reactor engineering to assess the thermal, hydraulic, and structural performance of the core and the core support structure; plant systems control; systems engineering; development of reactor components, including reactor vessel, control and locking mechanisms, fuel handling equipment, core support structure, shielding assemblies, main helium circulator, steam generator, circulator test facility, and auxiliary circulator; development of a helium circulator test facility; reactor safety, environment, and risk analyses, including planning and support of an in-pile and out-of-pile safety test program; nuclear island engineering design; and development of a reliability data bank.

CONTENTS

PROGRESS REPORT SERIES	iii
ABSTRACT	v
1. INTRODUCTION	1-1
2. CORE ASSEMBLY DEVELOPMENT (189a No. 00582)	2-1
2.1. Core Assembly Thermal-Hydraulic Analysis	2-1
2.1.1. Introduction	2-1
2.1.2. Fuel Assembly Analysis	2-1
2.1.3. Control Assembly Analysis	2-22
2.1.4. Radial Blanket Analysis	2-22
2.2. Core Assembly Mechanical Analysis	2-22
2.2.1. Rod Bowing in GCFR Fuel Assemblies	2-22
2.2.2. Thermal Shock of the Fuel Assembly Flow Duct	2-37
2.2.3. Interassembly Gap for Hex and Round 300-MW(e) GCFR Fuel Assemblies	2-44
2.2.4. Nonproliferation Core Study	2-48
2.3. Core Assembly Structural Design Criteria	2-51
2.4. Core Assembly Mechanical Testing	2-51
2.5. Heat Transfer and Fluid Flow Testing	2-51
References	2-51
3. PRESSURE EQUALIZATION SYSTEM FOR FUEL (189a No. 00582)	3-1
3.1. Core Assembly and PES Seals	3-1
3.2. Analysis, Models, and Code Development	3-1
3.3. Plateout and Plugging	3-4
3.4. Fission Product Release and Transport	3-5
3.5. Monitor Station and Instrumentation	3-6
References	3-7
4. CORE ASSEMBLY DESIGN VERIFICATION (189a No. 00582)	4-1
4.1. Core Flow Test Loop Program	4-2
4.1.1. Program Planning	4-2
4.1.2. Test Analysis and Prediction	4-3

4.1.3.	Test Bundle Design	4-11
4.2.	GCFR Prototype Assembly Test Program	4-12
4.3.	Helium Flow Test	4-14
4.4.	Depressurized Accident Condition Test	4-16
	References	4-17
5.	FUELS AND MATERIAL ENGINEERING (189a No. 00582)	5-1
5.1.	Oxide Fuel and Blanket Technoloty	5-1
5.2.	Cladding Technology	5-1
5.2.1.	ANL Mechanical Testing Program	5-1
5.2.2.	Helium Loop Test Program	5-7
5.3.	F-1 (X094) Fast Flux Irradiation Experiment	5-8
5.4.	F-3 (X206) Fast Flux Irradiation Experiment	5-13
5.5.	F-5 (X317) Grid-Spaced Fuel Rod Bundle Fast Flux Irradiation Experiment	5-13
5.6.	GB-10 Vented Fuel Rod Experiment	5-15
5.7.	GCFR Cladding Irradiation Program	5-15
	References	5-16
6.	FUEL ROD ENGINEERING (189a No. 00583)	6-1
6.1.	Fuel, Blanket, and Control Rod Analytical Methods . . .	6-1
6.1.1.	Gas-Cooled Version of LIFE Transient Code . .	6-1
6.1.2.	Comparison of FRAP and LIFE Codes	6-2
6.2.	Analysis of Irradiation Tests	6-8
6.2.1.	F-1 Irradiation Test Analysis	6-8
6.2.2.	GB-10 Irradiation Test Analysis	6-20
6.3.	Rod Analysis and Performance	6-20
6.4.	Fuel Rod Mechanical Tests	6-20
	References	6-22
7.	NUCLEAR ANALYSIS AND REACTOR PHYSICS (189a No. 00584)	7-1
	References	7-2
8.	SHIELDING REQUIREMENTS (189a No. 00584)	8-1
8.1.	Generic Studies for Radial Shielding	8-1
8.1.1.	Analysis	8-1
8.1.2.	Radiation Exposure Criteria	8-5
8.1.3.	Exposure Margins	8-7
8.1.4.	Results	8-9

8.1.5.	Summary and Recommendations	8-31
8.2.	Conceptual Shielding Configuration I	8-36
8.3.	300-MW(e) GCFR Lower Plenum Streaming	8-38
8.4.	Heating Rates in a Proposed Depleted Uranium Core Catcher	8-50
8.4.1.	Method	8-51
8.4.2.	Hand Calculated Heating Rates in Depleted Uranium	8-51
	References	8-57
9.	SYSTEMS ENGINEERING (189a No. 00585)	9-1
9.1.	Systems Design	9-1
9.2.	Systems Integration	9-1
	References	9-1
10.	COMPONENT DEVELOPMENT (189a No. 00586)	10-1
10.1.	Reactor Vessel	10-1
10.2.	Control and Locking Mechanisms	10-19
10.3.	Fuel Handling Development	10-20
10.4.	Core Support Structure	10-20
10.4.1.	New Concepts for Up-Flow Core Support Structure	10-21
10.5.	Reactor Shielding Assemblies	10-21
10.5.1.	Reference Design Thermal Analysis	10-23
10.5.2.	Up-Flow Configuration Thermal Analysis	10-26
10.6.	Main Helium Circulator, Valve and Service System	10-26
10.6.1.	Main Helium Circulator	10-27
10.6.2.	Evaluation and Recommendations	10-28
10.6.3.	Selected Design Concept	10-29
10.6.4.	Aerodynamic Design	10-32
10.6.5.	Electric Motor Drive	10-34
10.7.	Steam Generator	10-35
10.8.	Auxiliary Circulator, Valve and Service System	10-38
10.8.1.	Rotor Dynamics of the Auxiliary Circulator	10-39
10.8.2.	Rolling Element Bearing Life	10-39
10.8.3.	Auxiliary Loop Isolation Valve	10-41
10.9.	Helium Processing Components	10-41
10.9.1.	High-Temperature Filters	10-47

10.9.2.	High-Temperature Adsorbers	10-47
10.9.3.	Oxidizer-Cooler Module	10-47
10.9.4.	Dryer Module	10-47
10.9.5.	Low-Temperature Adsorber Module	10-48
10.9.6.	Compressors	10-48
10.9.7.	Regeneration Oxidizer Module	10-48
10.9.8.	Regeneration Module	10-49
10.10	Control and Electric Components	10-49
10.10.1.	Main Circulator Electric Drive Study	10-49
10.10.2.	Motor Missiles	10-51
10.10.3.	Power Loading Sequence for Starting Core Auxiliary Cooling System Loops	10-51
10.10.4.	Main Loop Cooling System and Main Loop Residual Heat Removal System Using Electric Motor Drive(s)	10-59
10.10.5.	Control and Instrumentation Delays During CACS Initiation	10-59
10.10.6.	Control Rod Drive Actuation and Position Indication Subsystem	10-62
	References	10-63
11.	CIRCULATOR TEST FACILITY (189a No. 00586)	11-1
	Reference	11-4
12.	PLANT DYNAMICS (189a No. 00638)	12-1
12.1.	Control Systems	12-1
12.1.1.	Plant Dynamic Modeling	12-1
12.1.2.	System Synthesis	12-17
12.2.	Seismic Engineering	12-19
12.3.	Flow-Induced and Acoustically Induced Vibrations	12-19
13.	REACTOR SAFETY, ENVIRONMENT, AND RISK ANALYSIS (189a No. 00589)	13-1
13.1.	Reactor Safety Program Coordination	13-2
13.2.	Probabilistic Safety Analysis	13-2
13.3.	Core Accident Analysis	13-3
13.3.1.	Introduction	13-3
13.3.2.	Core-Wide Material Melting Sequence During a Protected Loss of Flow Accident	13-3

13.4.	Postaccident Fuel Containment	13-4
13.4.1.	Assessment of a Heavy Metal Bath System for Molten Fuel Containment	13-4
13.4.2.	Scoping Postaccident Fuel Containment Study for the Up-Flow Core Design	13-9
13.5.	Engineering Reliability Integration	13-11
13.6.	Gas-Cooled Reactor Reliability Data Bank	13-12
13.6.1.	Data Sources	13-12
13.6.2.	Data Analysis	13-14
	References	13-15
14.	GCFR SAFETY TEST PROGRAM (189a No. 00588)	14-1
14.1.	GRIST-2 Program	14-1
14.1.1.	Organizational Responsibilities	14-1
14.1.2.	Trade Study	14-2
14.1.3.	Fuel Requirements	14-3
14.1.4.	Preliminary GRIST-2 Program Test Plan	14-4
14.1.5.	GRIST-2 Technical Requirements Review	14-7
14.2.	Duct Melting and Fallaway Test Program	14-8
14.2.1.	FLS-1 Experiment	14-8
14.2.2.	FLS-1 Analytical Support	14-8
	References	14-10
15.	GCFR NUCLEAR ISLAND DESIGN (189a No. 00615)	15-1
16.	ALTERNATE DESIGN STUDY (189a No. 00759)	16-1
16.1.	Refueling Systems	16-1
16.2.	Integrated Individual PCRV System	16-1
16.3.	Alternative Configurations	16-2
16.4.	Alternate Residual Heat Removal System Study	16-3
	Reference	16-4
17.	ALTERNATE FUEL CYCLES (189a No. 00759)	17-1
17.1.	Approximate Sensitivity Analysis	17-2
17.2.	Mass Flow Information	17-2
17.3.	Preliminary Pu/U Carbide Core Design	17-9
17.4.	Alternate Fuel Cycles Materials	17-9
	References	17-10

FIGURES

2-1.	Bundle subdivision, showing rod and subchannel numbers . .	2-4
2-2.	Axial distribution of heat transfer coefficient over one rib: (a) local heat transfer coefficient variation; (b) cladding surface temperature; (c) cladding midwall temperature	2-8
2-3.	Roughening transition	2-10
2-4.	300-MW(e) fuel rod at roughening transition: (a) cladding o.d. temperature; (b) fuel centerline temperature	2-11
2-5.	Heat transfer enhancement due to spacers	2-12
2-6.	Effect of spacer in smooth region: (a) cladding o.d. temperature; (b) fuel centerline temperature	2-13
2-7.	Effect of spacer in roughened region: (a) cladding o.d. temperature; (b) fuel centerline temperature	2-14
2-8.	Area change due to duct dilation (worst subchannel)	2-16
2-9.	Worst edge rod ΔT	2-17
2-10.	Second-row rod ΔT	2-19
2-11.	Edge rod ΔT (80% edge spacing) for two spacer types and revised spacer locations	2-20
2-12.	Edge rod ΔT	2-21
2-13.	Input for nominal case, worst edge rod hex assembly (52% gap, 75% roughening, BOL nominal gap)	2-24
2-14.	Input for nominal case, worst second-row rod hex assembly (52% gap, 75% roughening, BOL nominal gap)	2-25
2-15.	EOL deflections for optimized 10-spacer grid arrangement, nominal configuration, nondilating duct case	2-26
2-16.	Area change due to duct dilation, worst subchannel	2-28
2-17.	Input for edge rod, 10,000-hr dilated duct	2-29
2-18.	Input for second-row rod, 10,000-hr dilated duct	2-30
2-19.	EOL deflections for optimized 10-spacer grid arrangement, nominal configuration	2-31
2-20.	Input for revised design, worst edge rod hex assembly [80% gap, 75% roughening, maximum gap (average over life)]	2-33
2-21.	Input for revised design, worst second-row rod hex assembly [80% gap, 75% roughening, maximum gap (average over life)]	2-34

FIGURES (Continued)

2-22.	Rod and duct shapes for 316 stainless steel hex duct case, 18,000 hr (10.2-mm pitch, 80% gap, 75% roughness, worst expected tolerances plus average over life duct dilation)	2-35
2-23.	Rod to duct gap vs distance from grid manifold for 316 stainless steel hex duct case, 18,000 hr (10.2-mm pitch, 80% gap, 75% roughness, worst expected tolerances plus average over life duct dilation)	2-36
2-24.	Edge rod to second-row rod gap for 316 stainless steel hex duct case, 18,000 hr (10.2-mm pitch, 80% gap, 75% roughness, worst expected tolerances plus average over life duct dilation)	2-38
2-25.	Assembly outlet thermal shock model	2-39
2-26.	Duct wall temperature during thermal shock	2-41
2-27.	Maximum in-plane stresses during thermal shock	2-42
2-28.	Fatigue life for irradiated 20% cold-worked 316 stainless steel ($T = 538^{\circ}\text{C}$)	2-43
2-29.	BOL interassembly gap required to prevent duct to duct contact for various duct wall thicknesses	2-47
2-30.	Required interassembly gap for proliferation-resistant core	2-50
4-1.	Rod thermocouple location schemes (each letter denotes a thermocouple)	4-4
4-2.	CFTL thermocouple location diagram, scheme A	4-5
4-3.	37-rod CFTL bundle flow channels, one-sixth section	4-8
4-4.	Cladding temperature vs rod length (rod 8) for CFTL 37-rod bundle [2 spacer types, 2 hanger rod types, increased interior channel blockage (smooth), COBRA-IV]	4-9
4-5.	Simplified sketch of ORNL proposed coolant inlet area design for CFTL fuel rod simulator test bundle	4-13
4-6.	Helium flow rig	4-15
5-1.	Creep rupture test results	5-5
5-2.	Fission gas release vs fuel rod burn-up	5-11
6-1.	Comparison of calculated and measured fission gas release (F-1 experiment)	6-9
6-2.	Comparison of calculated and measured cladding diameter change $\Delta D/D$, axial section I (F-1 experiment)	6-10
6-3.	Comparison of calculated and measured cladding diameter change $\Delta D/D$, axial section II (F-1 experiment)	6-11

FIGURES (Continued)

6-4.	Comparison of calculated and measured cladding diameter change $\Delta D/D$, axial section III (F-1 experiment)	6-12
6-5.	Comparison of calculated and measured cladding diameter change $\Delta D/D$, axial section IV (F-1 experiment)	6-13
6-6.	G-1 plenum pressure (F-1 experiment)	6-14
6-7.	G-1 total cladding creep strain, axial section III (F-1 experiment)	6-15
6-8.	G-4 total cladding creep strain, axial section III (F-1 experiment)	6-16
6-9.	G-7 total cladding creep strain, axial section III (F-1 experiment)	6-17
6-10.	G-8 total cladding creep strain, axial section III (F-1 experiment)	6-18
6-11.	G-13 total cladding creep strain, axial section III (F-1 experiment)	6-19
8-1.	Alternative radial shielding configurations	8-2
8-2.	Radial blanket management scheme	8-4
8-3.	Inner radial shield nuclear heating distribution for case 6	8-20
8-4.	Radial shield and PCRV nuclear heating distribution for case 6	8-21
8-5.	Stainless steel helium concentration vs EPY at inner surface of first row of inner radial shield assemblies for case 1	8-29
8-6.	Stainless steel helium concentration vs EPY at inner surface of outer radial shield for case 1	8-30
8-7.	Stainless steel helium concentration vs EPY at inner surface of first row of inner radial shield assemblies for case 6	8-32
8-8.	Stainless steel helium concentration vs EPY at inner surface of outer radial shield for case 6	8-33
8-9.	Model of down-flow demonstration plant shielding system and reactor internal components	8-37
8-10.	Model of lower plenum for streaming calculations	8-39
8-11.	Lower plenum streaming, problem No. 1 ($B_4C + C$); isoflux graph for normalized thermal flux, $E < 2.38$ eV	8-40
8-12.	Lower plenum streaming, problem No. 1 ($B_4C + C$); isoflux graph for normalized epithermal neutron flux, 0.12 MeV $> E > 2.38$ eV	8-41

FIGURES (Continued)

8-13.	Lower plenum streaming, problem No. 1 (B ₄ C + C); isoflux graph for normalized fast flux, 0.91 MeV > E > 0.12 MeV . .	8-42
8-14.	Lower plenum streaming, problem No. 1 (B ₄ C + C); isoflux graph for normalized fast flux, E > 0.91 MeV	8-43
8-15.	Lower plenum streaming, problem No. 1 (B ₄ C + C); isoflux graph for normalized total neutron flux	8-44
8-16.	Lower plenum streaming, problem No. 2 (graphite); isoflux graph for normalized thermal flux, E < 2.38 eV	8-45
8-17.	Lower plenum streaming, problem No. 2 (graphite); isoflux graph for normalized epithermal neutron flux, 0.12 MeV > E > 2.38 eV	8-46
8-18.	Lower plenum streaming, problem No. 2 (graphite); isoflux graph for normalized fast flux, 0.91 MeV > E > 0.12 MeV . .	8-47
8-19.	Lower plenum streaming, problem No. 2 (graphite); isoflux graph for normalized fast flux E > 0.91 MeV	8-48
8-20.	Lower plenum streaming, problem No. 2 (graphite); isoflux graph for normalized total neutron flux	8-49
8-21.	Heating rates in proposed depleted uranium core catcher (capture gamma rays only)	8-52
10-1.	300-MW(e) GCFR up-flow core	10-3
10-2.	300-MW(e) GCFR up-flow core, concept No. 5	10-4
10-3.	300-MW(e) GCFR up-flow core, concept No. 7	10-5
10-4.	300-MW(e) GCFR up-flow core, concept No. 2	10-6
10-5.	300-MW(e) GCFR up-flow core, concept No. 3	10-7
10-6.	300-MW(e) GCFR up-flow core, concept No. 4	10-8
10-7.	300-MW(e) GCFR up-flow core, concept No. 6	10-9
10-8.	300-MW(e) GCFR up-flow core, concept No. 4A	10-10
10-9.	300-MW(e) GCFR up-flow core, concept No. 7A	10-11
10-10.	Replaceable thermal barrier installation, zone 7, alternate No. 1 (view looking down)	10-13
10-11.	Replaceable thermal barrier installation, zone 7, alternate No. 1 (circumferential elevation view)	10-14
10-12.	Replaceable thermal barrier installation, zone 7, alternate No. 2 (view looking down)	10-15
10-13.	Replaceable thermal barrier installation, zone 7, alternate No. 2 (circumferential elevation view)	10-16
10-14.	GCFR up-flow core closure system	10-18

FIGURES (Continued)

10-15. Conceptual up-flow core configuration	10-22
10-16. Lower plenum shielding reference design	10-24
10-17. Radial shielding concept for up-flow configuration	10-25
10-18. Main helium circulator installation	10-30
10-19. Steam generator	10-36
10-20. Helium purification system	10-42
10-21. Timing cycle to energize CACS equipment using a diesel generator; a main CACS load is energized every 5 s	10-53
10-22. Timing cycle to energize CACS equipment using on- or off-site power; a main CACS load is energized every 5 s	10-54
10-23. Timing cycle to energize CACS equipment using oversize diesel generator for rapid cycling of CACS loads	10-55
10-24. Timing cycle to energize CACS equipment using on- or off-site power for rapid cycling of CACS loads	10-56
10-25. Timing cycle to energize CACS equipment using standard size diesel generator; a main CACS load is energized every 5 s	10-57
10-26. Timing cycle to energize CACS equipment using oversize diesel generator for rapid cycling of CACS loads	10-58
10-27. Schematic diagram of single circulator drive motor for main loop and shutdown cooling	10-60
10-28. Schematic diagram of main circulator motor with pony motor for shutdown cooling	10-61
11-1. GCFR helium circulator test facility	11-2
11-2. Schematic diagram of GCFR helium circulator test facility .	11-3
12-1. Steam-turbine-driven circulator plant configuration	12-2
12-2. Electric-motor-driven circulator plant configuration	12-3
12-3. Reactor core model	12-5
12-4. Representative fuel rod	12-6
12-5. Equivalent fuel channel thermal model	12-7
12-6. Feedwater configuration	12-9
12-7. Feedwater model	12-10
12-8. Feedwater heater	12-11
12-9. Steam generator model	12-13
12-10. Compressor model	12-15

FIGURES (Continued)

12-11. Circulator turbine model	12-16
12-12. High-pressure turbine model	12-18
13-1. Circulator coast-down in 40 s	13-5
13-2. Loss of flow 1 week after shutdown	13-6
17-1. Approximate characteristics for Pu/U core, Th axial blanket, Th radial blanket	17-3
17-2. Approximate characteristics for Pu/U core, Th axial blanket, Th radial blanket	17-4
17-3. Approximate characteristics for Pu/U core, U axial blanket, U radial blanket	17-5
17-4. Approximate characteristics for Pu/Th core, Th axial blanket, Th radial blanket	17-6

TABLES

2-1. AGATHE-1C test specifications	2-3
2-2. Comparison of calculated and measured mean surface tempera- tures (°C) for AGATHE-1C test 1	2-5
2-3. Comparison of calculated and measured mean surface tempera- tures (°C) for AGATHE 1-C test 2	2-7
2-4. Temperature variations at fuel rod surface and centerline .	2-15
2-5. Input parameters for nonproliferation core study	2-49
3-1. Test assembly arrangement	3-2
4-1. 37-rod CFTL channel/thermocouple axial location arrange- ment for scheme A	4-6
5-1. Complete rupture time results from the ANL-III biaxial creep rupture test	5-3
5-2. Strain at failure of various types of specimens used in ANL-III creep-rupture test	5-4
5-3. Results of gas sampling and analysis	5-10
5-4. Results of gas sampling and analyses for previously analyzed F-1 fuel rods	5-12
6-1. Items to be modified in FRAP codes for GCFR fuel rod analysis	6-6
6-2. Advantages and disadvantages of FRAP codes compared with LIFE codes	6-7
6-3. Fuel cladding mechanical test matrix	6-21

TABLES (Continued)

8-1.	Radiation exposure and damage at the inner surface of the shield reflector assemblies	8-10
8-2.	Radiation exposure and damage at the inner surface of the first row of inner radial shielding	8-11
8-3.	Radiation exposure and damage at the inner surface of the second row of inner radial shielding	8-12
8-4.	Radiation exposure and damage at the inner surface of the outer radial shield	8-13
8-5.	Radiation exposure and damage at the PCSB (PCR liner and PCR)	8-14
8-6.	Exposure margins for first row of removable shielding at 3 EPY	8-15
8-7.	Exposure margins for second row of removable shielding at 3 EPY	8-16
8-8.	Exposure margins for permanent outer radial shield at 30 EPY	8-17
8-9.	Exposure margins at primary coolant system boundary for cases 1 through 7	8-18
8-10.	Comparison of maximum nuclear heating rates in various materials in inner radial shield assemblies for cases 1 through 7	8-22
8-11.	Comparison of total energy deposition rate in inner radial shield assemblies for cases 1 through 7	8-23
8-12.	Comparison of stainless steel helium concentrations for radial shield cases 1 through 7	8-27
8-13.	Helium concentrations in stainless steel at various times for radial shield cases 1 through 7	8-28
8-14.	One-group capture and fission cross sections for U-235 and U-238	8-54
8-15.	Distribution of energy per fission of U-235 and U-238 in core catcher	8-55
8-16.	Energy deposition in depleted uranium fraction of U-235 = 0.003	8-56
10-1.	GCFR auxiliary circulator critical speed versus bearing stiffness (ROTOR3)	10-40
10-2.	Flow conditions for the process section of the helium purification system	10-44
10-3.	Flow conditions for the regeneration section of the helium purification system	10-45

TABLES (Continued)

13-1.	Pool conditions and heat transfer results at a pool bulk temperature of 1600°C	13-8
13-2.	Active reliability data collection sources for electrical power plant application	13-13
14-1.	GRIST-2 preliminary five-year test plan	14-5
14-2.	FLS-1 test sequence	14-9
17-1.	Fuel cycle and core design characteristics for GCFR alternate fuel cycle designs	17-7

1. INTRODUCTION

The various tasks of the gas-cooled fast breeder reactor (GCFR) program for the period May 1, 1978 through July 31, 1978 sponsored by the Department of Energy (DOE) are discussed in this quarterly progress report. The GCFR utility program, which is sponsored by a large number of electric utility companies, rural electric cooperatives, and General Atomic (GA), is primarily directed toward the development of a GCFR demonstration plant. The utility-sponsored work and the DOE-sponsored work are complementary.

Analytical, experimental, and fabrication development is being accomplished under the core assembly development task to establish the basis for the design of GCFR fuel, blanket, and control assemblies. Methods development for structural, thermal-hydraulic, and mechanical analyses is discussed, and the results of structural analysis of the fuel assembly components and thermal-hydraulic analysis of the blanket assembly during low power are presented. Current progress on rod-spacer interaction tests, fuel assembly seismic and vibration test planning, and development of assembly fabrication techniques is also presented. The various subtasks of core assembly development and the work accomplished during this reporting period are discussed in Section 2.

The technology to support the design and construction of the pressure equalization system (PES) for GCFR fuel is being developed. This includes (1) the development of analytical models and computer codes which will be verified by test programs and testing of materials and seals and (2) the development of fabrication processes for the PES. These are discussed in Section 3.

To demonstrate the ability of GCFR fuel, control, and blanket assembly designs to meet design goals and verify predictions of analytical models, a

series of out-of-pile simulation tests will be performed. The emphasis of the tests will be on obtaining thermal-structural data for steady-state, transient, and margin conditions using electrically heated rod bundles in a dynamic helium loop. These are discussed in Section 4.

In the fuels and materials development program, thermal flux and fast flux irradiation studies are being conducted to establish conditions and design features specific to GCFR fuel rods, such as vented fuel, fission product traps, and surface-roughened cladding. In addition, an irradiation test program of smooth and surface-roughened GCFR cladding specimens is being conducted to determine how these materials behave under irradiation. The fuels and materials tests, the analytical studies, and the results to date are presented in Section 5.

Under the fuel rod engineering task, performance of the fuel and blanket rods under steady-state and transient conditions is being evaluated to determine performance characteristics, operating limits, and design criteria. In addition, surveillance of the fuel rod and blanket rod technology of other programs is being carried out. These studies are presented in Section 6.

The objectives of the nuclear analysis and reactor physics task are to verify and validate the nuclear design methods which will be applied to the GCFR core design. Data from a critical assembly experimental program at the ZPR-9 facility at Argonne National Laboratory (ANL) are being used for this purpose. Critical assembly design, analysis, and methods development are discussed in Section 7.

Verification of the physics and engineering analytical methods and the data for design of the GCFR shields is being conducted under the shielding requirements task along with an evaluation of the effectiveness of various shield configurations. The results of radial shield analyses and the work being done on structural analysis are presented in Section 8.

Section 9 discusses systems engineering for the GCFR. This includes systems integration; coordination of interface requirements between plant systems; and development and implementation of effective documentation management.

Section 10 discusses the evaluation and development of the main components of the GCFR, including reactor vessel, control and locking mechanisms, fuel handling, core support structure, shielding assemblies, main helium circulator, steam generator, auxiliary circulator, and helium processing components. Section 11 is concerned with the engineering required to design and develop the circulator test facility, and Section 12 reports on the development of control systems and the assessment of seismically induced and flow-induced vibration behavior for the GCFR demonstration plant.

The reactor safety task, which is discussed in Section 13, includes (1) maintenance of liaison between GA and other organizations and integration of the overall GCFR safety analysis effort; (2) formulation and review of the GCFR safety program plan; (3) performance of detailed safety, environmental, and risk analyses of the GCFR; (4) evaluation of the post-accident fuel containment (PAFC) capability of the GCFR; (5) integration of the results of DOE safety studies into the licensing reviews; and (6) evaluation of probabilistic design methods for use in the GCFR program. Procurement, supply, and storage of reliability data are also reported along with estimates in support of probabilistic analyses of accident events being analyzed for gas-cooled reactors.

Section 14 discusses the safety test program, which involves quantification of fuel and cladding behavior during accidents leading to core damage and identification of safety test information required for licensing and commercialization of the GCFR. The GRIST-2 and duct melting and fall-away test programs (DMFT) are also examined.

Section 15 discusses the nuclear island. The purposes of this task are to accomplish engineering design work on the nuclear island portion

of the demonstration plant and to resolve the interface requirements of major nuclear steam supply (NSSS) and balance of plant (BOP) systems.

Development of an alternate design concept for the NSSS and related BOP facilities and equipment is discussed in Section 16, and the characteristics of a GCFR fueled with combinations of U-233, U-235, U-238, plutonium, and thorium are reported in Section 17.

2. CORE ASSEMBLY DEVELOPMENT (189a No. 00582)

2.1. CORE ASSEMBLY THERMAL-HYDRAULIC ANALYSIS

2.1.1. Introduction

Experimental data are being evaluated to develop the analytical basis for the design and development of the GCFR fuel, control, and blanket assemblies. Because complete prototype in-pile tests cannot be conducted, a strong analytical base supported by development tests is required to design the core assemblies. The current effort is devoted to the development of an adequate steady-state and transient analysis capability in the areas of thermal-hydraulic and structural analysis to provide a basis for assembly design criteria and specific test requirements. The main efforts have focused on improvement of thermal-hydraulic correlations and development of methods for applying the correlations to the design and analysis of GCFR core assemblies.

2.1.2. Fuel Assembly Analysis

2.1.2.1. Analysis of AGATHE-1C Bundle for Turbulent and Transition Flow.

COBRA*GCFR-1 is a modified version of the COBRA-IV code (Ref. 2-1) for thermal-hydraulic analysis of GCFR fuel, blanket, and control assemblies. The following properties and procedures have been included in COBRA-IV for fuel assembly analysis:

1. Helium properties (for GCFR analysis).
2. CO₂ properties (for AGATHE analysis).
3. Procedure for calculating friction factors and Stanton numbers in interior channels by integration of logarithmic velocity and temperature profiles (subroutine BASIC)).

4. Procedure for calculating friction factors and Stanton numbers in wall channels by inverse transformation (subroutine IDDT).
5. Procedure for calculating Stanton numbers in the smooth part of the bundle by Petukhov-Roizen method.
6. Biot number correction procedure based on local Biot number.
7. Mixing coefficient procedure based on the experimental results of Ref. 2-2.

In addition to these modifications, a set of reference correlations for thermal-hydraulic analysis of GCFR fuel assemblies has recently been obtained (Ref. 2-3). These correlations were used with the COBRA*GCFR-1 code for analysis of the AGATHE 1C bundle.* The AGATHE 1C bundle geometry (37 rods) and the test conditions are described in Ref. 2-4. The test conditions are summarized in Table 2-1. Figure 2-1 shows the model used for this analysis. It is necessary to analyze a one-sixth bundle since the spacers are not completely symmetrical.

Table 2-2 compares the calculated and measured surface temperatures** for test 1 ($\overline{Re}_B \sim 3.1 \times 10^5$). Most calculated temperatures are within $\pm 2.5\%$ (% difference based on calculated surface temperature minus inlet coolant temperature), except temperatures in the corner channels. As shown by the SCRIMP code analysis (Ref. 2-5), the corner channel temperatures are considerably influenced by circumferential conduction, which is not yet modeled in COBRA*GCFR. The difference between the calculated and the measured pressure drop was 9.7%, indicating that the predicted rough friction factor using the correlation in Ref. 2-3 may be too large.

*These tests were performed and funded by the Swiss Federal Institute for Reactor Research (EIR).

**In the AGATHE 1C experiment, the rod temperatures were measured at the inside cladding location, and the surface temperatures were deduced from this measurement by correcting for cladding temperature drop.

TABLE 2-1
AGATHE-1C TEST SPECIFICATIONS^(a)

Test 1	
$\bar{Re}_B$	$\sim 3.1 \times 10^5$
P(inlet)	~ 3.955 MPa
T(inlet)	$\sim 104.2^\circ\text{C}$
Energy input	~ 200.27 W/cm
Heat loss	~ 399 W
Test 2	
$\bar{Re}_B$	$\sim 10^4$
P(inlet)	~ 0.6806 MPa
T(inlet)	$\sim 101.6^\circ\text{C}$
Energy input	~ 6.613 W/cm
Heat loss	~ 412.3 W

^(a) Coolant = CO₂.

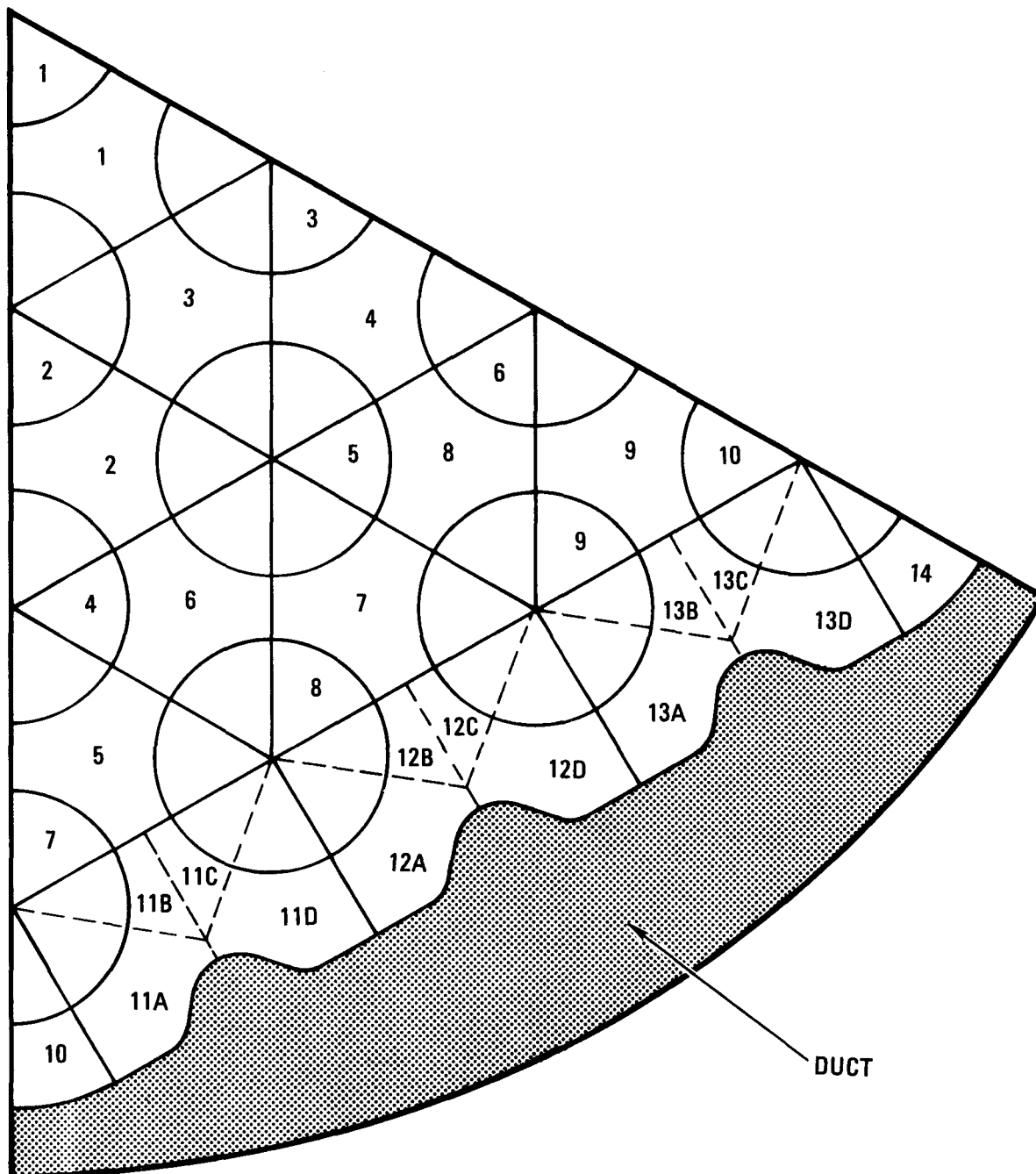


Fig. 2-1. Bundle subdivision, showing rod and subchannel numbers

TABLE 2-2
COMPARISON OF CALCULATED AND MEASURED MEAN SURFACE
TEMPERATURES (°C) FOR AGATHE-1C TEST 1

Rod No. (a)	Subchannel No. (a)	Level II (X = 310 mm) Smooth Region ^(b)		Level IV (X = 610 mm) Rough Region		Level V (X = 1160 mm) Rough Region	
		Calculated	Measured	Calculated	Measured	Calculated	Measured
1	1	491.6	484.7	443.7	436.1	620.2	618.9
2	3	491.5	481.4 482.7	443.6	430.8 442.8	619.8	622.6 617.8
4	6	491.5	--	443.5	437.9 436.5	616.9	626.6 631.9
6	4	491.5	481.5	443.6	445.7	619.2	618.9
6	9	492.2	486.4	442.4	447.8	611.1	613.2
7 ^(c)	10	618.7	550.9	471.7	460.1	629.8	--
7	11A	587.2	--	445.7	--	604.5	623.6
8	6	491.5	492.3	443.5	433.8	616.9	612.1 628.7
8	7	492.1	502.2	442.3	437.8	611.7	607.4
8	11C	520.8	512.8	444.2	433.2	606.7	594.3
8	12A	543.6	521.1	425.0	423.1 456.3	575.5	585.9
8	12B	505.2	--	435.9	456.3 437.8	593.5	--
9	12C	505.2	500.4	436.1	432.9	593.5	603.3 596.8
9	12D	543.9	516.5	424.4	427.4	574.7	603.3 580.5
10	9	492.2	--	442.4	438.5	611.2	597.3
10	13C	505.8	--	437.3	433.0	596.1	588.8
	13D	548.3	--	430.6	438.5	583.5	588.9
10 ^(c)	14	592.8	535.8	457.7	454.0	610.0	599.8

(a) See Fig. 2-1.

(b) Spacer effect on Stanton number is taken into consideration for level II.

(c) Locations with significant conduction effects.

Table 2-3 compares the calculated and measured surface temperatures for test 2 ($\overline{Re}_B \sim 10^4$). All calculated temperatures in the interior channels are within $\pm 4.6\%$ of the measured temperatures. For test 2, the effect of conduction in the wall and the corner channels is larger (than for test 1) owing to the lower magnitude of the heat transfer coefficient. This is demonstrated by the SCRIMP code analysis with and without conduction reported in Ref. 2-5. The predicted pressure drop for test 2 was also about 9% larger than the measured pressured drop. The following is concluded from this analysis:

1. Whenever circumferential conduction effects are not important, COBRA*GCFR, with the GCFR reference correlations, can predict the rod surface temperature within $\pm 2.5\%$ for fully turbulent flow conditions and within $\pm 5.0\%$ for transition flow conditions.
2. The correlation of the R function (Ref. 2-6) in the GCFR reference correlations predicts a friction factor which is too large.

2.1.2.2. Axial Fuel Temperature Gradients at the Smooth to Roughened Transition and at Spacer Locations. Because of the partly roughened design of the GCFR fuel rods, the cladding surface experiences a sudden temperature drop on the order of 90°C owing to enhancement of the heat transfer coefficient in the rough region. Cesium, a fission product, migrates toward lower fuel temperatures, and therefore it may be trapped at the point of minimum temperature if the fuel experiences the same temperature dip. Other potential cesium traps (where the heat transfer coefficient has large variations) are under the grid spacers and over each rib of the roughened rod.

Figure 2-2 shows the axial distribution of the heat transfer coefficient over one rib and its effect on cladding temperatures. A temperature variation of 22.2°C at the surface results in only 5.6°C at the cladding midwall. At the fuel surface (not shown in Fig. 2-2) the temperature swing is 2.8°C , and there is no variation at the fuel centerline.

TABLE 2-3
COMPARISON OF CALCULATED AND MEASURED MEAN SURFACE
TEMPERATURES (°C) FOR AGATHE 1-C TEST 2

Rod No. (a)	Subchannel No. (a)	Level II (X = 310 mm) Smooth Region (b)		Level IV (X = 610 mm) Rough Region		Level V (X = 1160 mm) Rough Region	
		Calculated	Measured	Calculated	Measured	Calculated	Measured
1	1	339.6	334.9	398.5	398.9	571.4	585.3
2	3	339.5	337.9 337.4	398.5	397.8 400.9	571.4	580.5 581.4
4	6	339.6	--	398.9	396.5 396.9	571.2	585.5 585.9
6	4	339.5	337.3	398.5	402.9	571.2	577.7
6	9	340.4	337.4	399.2	403.6	568.7	577.3
7 (c)	10	457.4	355.2	465.8	411.6	632.0	584.7
7	11A	417.8	351.4 354.4	430.4	410.5 410.0	596.5	586.5
8	6	339.6	343.8	398.9	403.8	571.2	581.2 576.9
8	7	340.2	344.9	398.8	403.3	568.2	576.5
8	11C	388.4	345.3	412.3	403.6	581.0	574.8
8	12A	380.8	345.2	403.1	402.9 411.6	556.1	574.0
8	12B	358.4	--	401.0	411.6 403.3	562.2	--
9	12C	358.4	345.6	401.0	405.9	562.2	564.3 582.5
9	12D	381.9	347.5	401.8	398.0	554.8	582.5 563.0
10	9	340.4	--	399.2	413.0	568.7	577.6
10	13C	359.2	--	403.0	410.1	565.9	571.5
	13D	386.0	--	409.9	413.4	566.2	570.9
10 (c)	14	432.7	358.9	446.8	415.6	603.9	571.7

(a) See Fig. 2-1.

(b) Effect of spacer on Stanton number is taken into consideration for Level II.

(c) Locations with significant conduction effects.

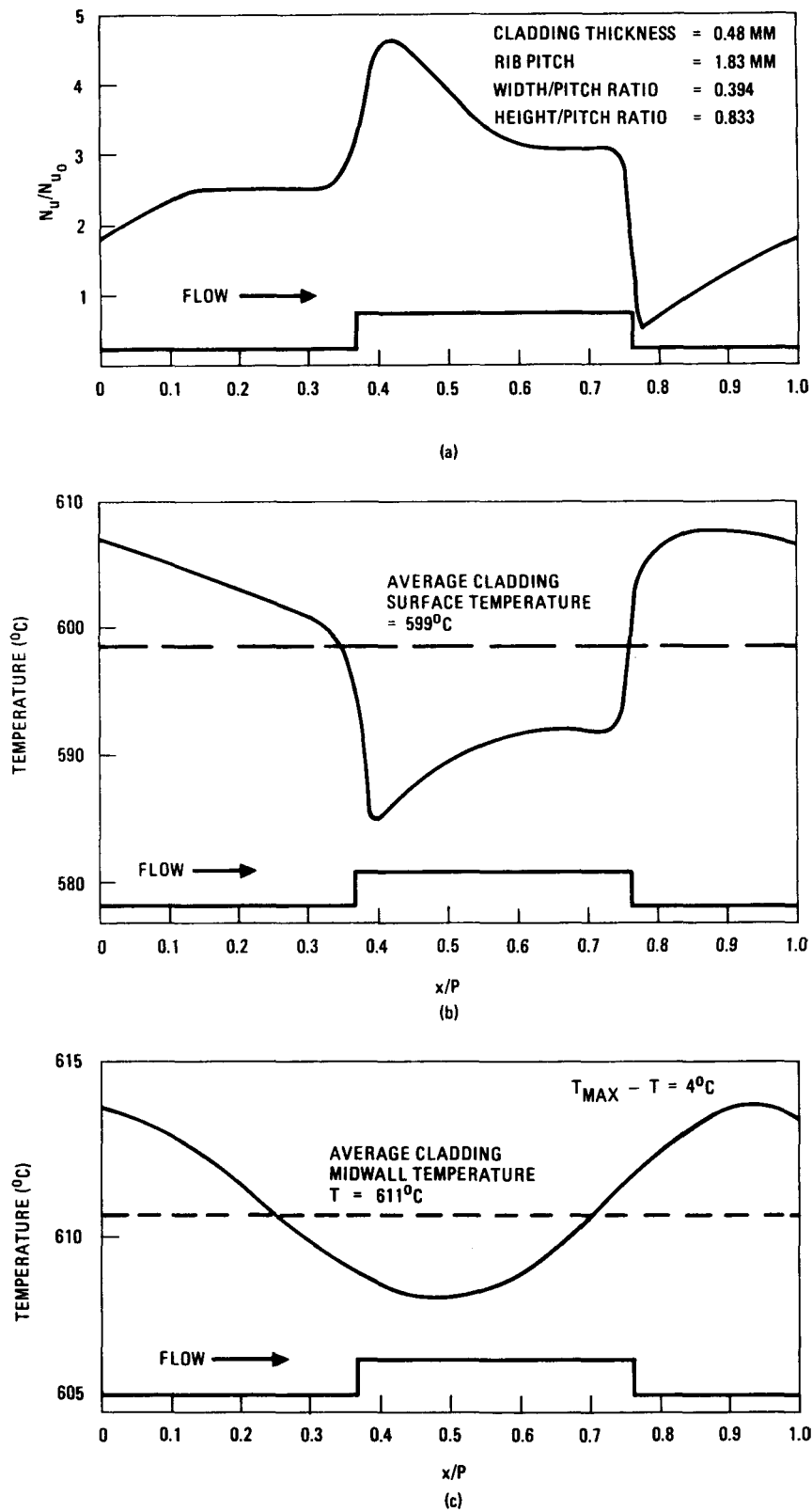


Fig. 2-2. Axial distribution of heat transfer coefficient over one rib:
 (a) local heat transfer coefficient variation; (b) cladding surface temperature; (c) cladding midwall temperature

The dimensions of the ribs are of the same order of magnitude as the cladding thickness, and therefore the effect of axial conduction in dissipating the temperature swings at the cladding surface can clearly be seen. In the cases of roughening transition and spacer grid locations, the heat transfer coefficient varies over a length of cladding which is 100 times the cladding thickness (Figs. 2-3 through 2-7). The effect of axial conduction is limited to a few millimeters on either end of the temperature dip. Hence, large axial temperature gradients of the cladding cause similar temperature gradients in the fuel.

Table 2-4 summarizes the cases analyzed, their heat transfer conditions, and the resulting temperature variations at the cladding surface and the fuel centerline. In the case of a spacer in a rough subchannel, there is even some enhancement of the temperature swing owing to the nonlinear nature of fuel conductivity.

2.1.2.3. Temperature Gradients in the Edge Rod Due to Duct Dilation. For the fuel assembly with a rod to duct wall spacing of 52% of rod to rod spacing, the effect of duct dilation on the edge rods was studied at two points in the lifetime of the fuel assembly: at 10,000 and 18,000 hr. The objective of this study was to obtain temperature gradients which could be used to calculate radiation- and metal-swelling-induced rod bowing. For the subchannel analysis, a one-twelfth section of the fuel assembly was modeled to take into account the effect of the coolant which will be diverted from the central channels to the edge channels when the duct dilates. Creep and elastic dilation were translated into area changes for the edge subchannels by assuming a triangular dilated profile (Fig. 2-8).

The flow redistribution caused by duct dilation results in the edge rods operating at lower average temperatures. At the assembly exit, the average temperature for the edge rod is about 20°C lower at 10,000 hr and 40°C lower at 18,000 hr. The maximum temperature drop across the rod increases from 25° to 63° to 90°C (Fig. 2-9). For the next row of rods, the effect from nominal to 18,000 hr is smaller; i.e., there is a drop of 12°C in average temperature, and the maximum temperature drop change is

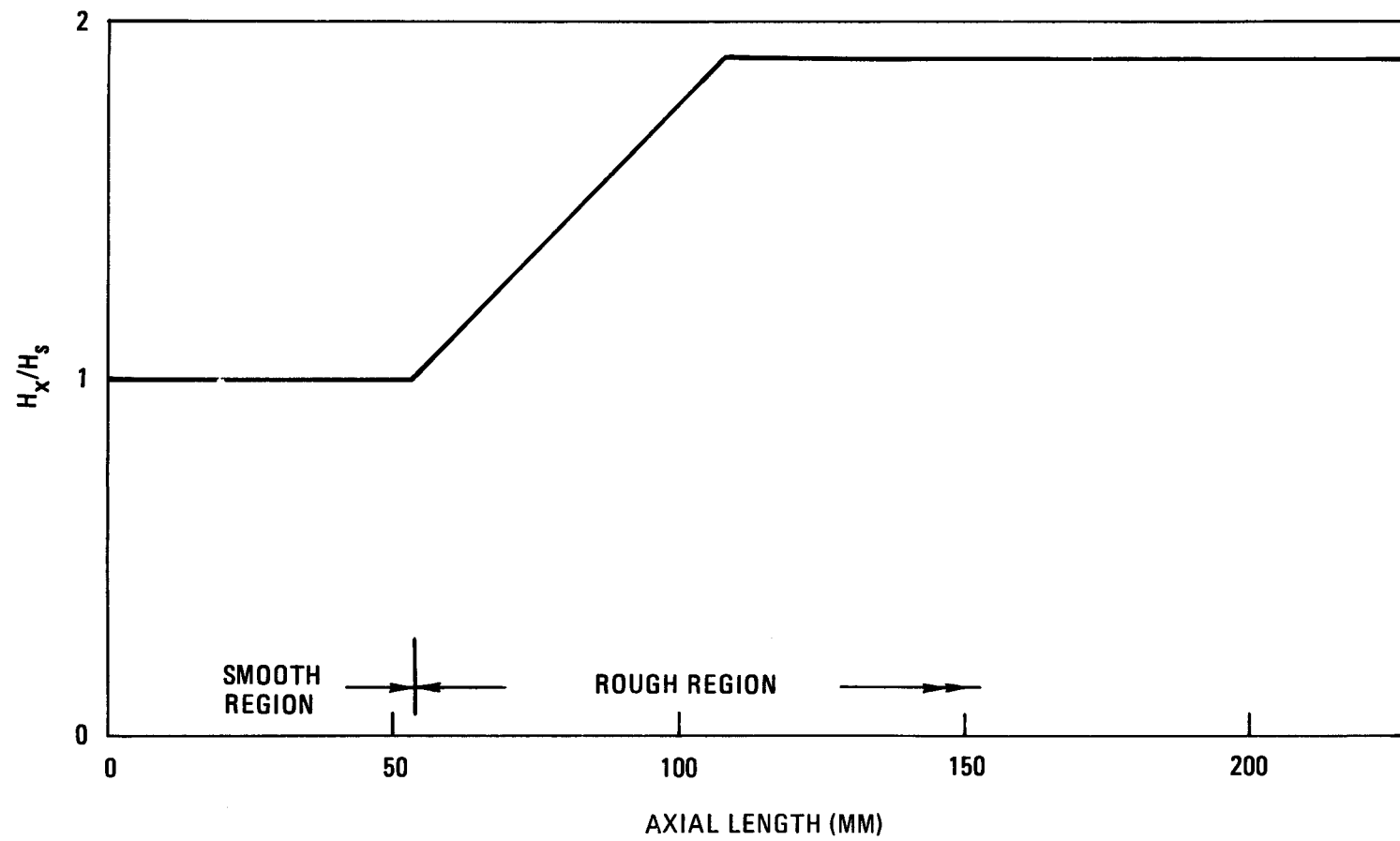


Fig. 2-3. Roughening transition

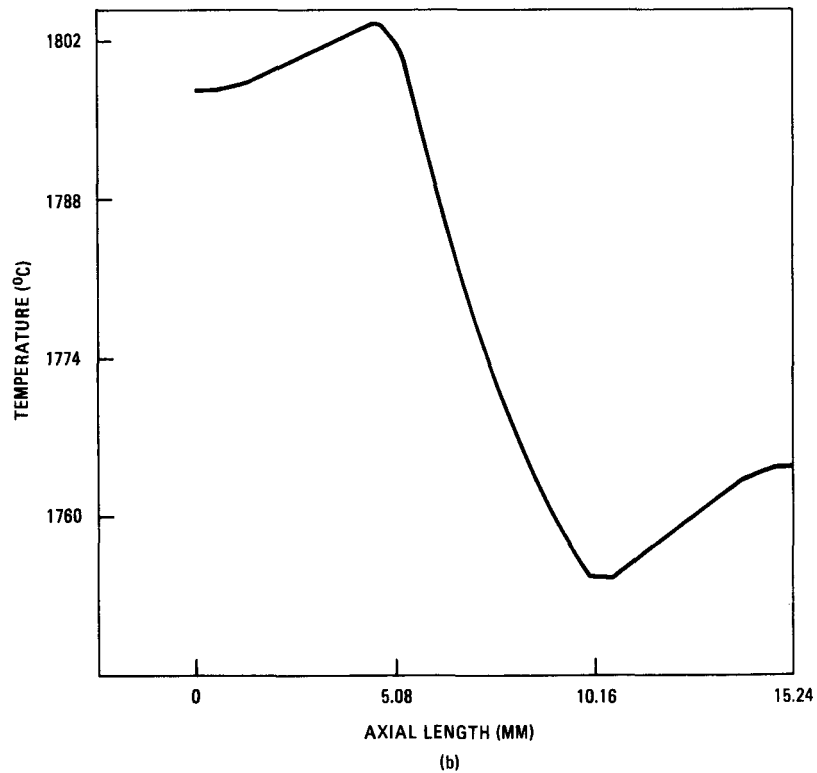
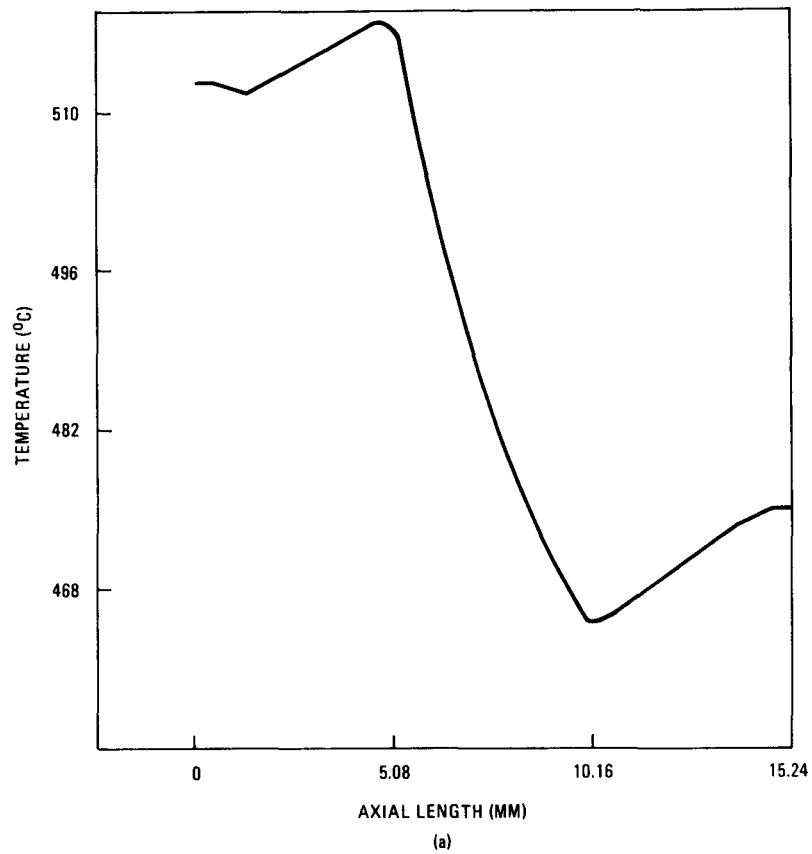


Fig. 2-4. 300-MW(e) fuel rod at roughening transition: (a) cladding o.d. temperature; (b) fuel centerline temperature

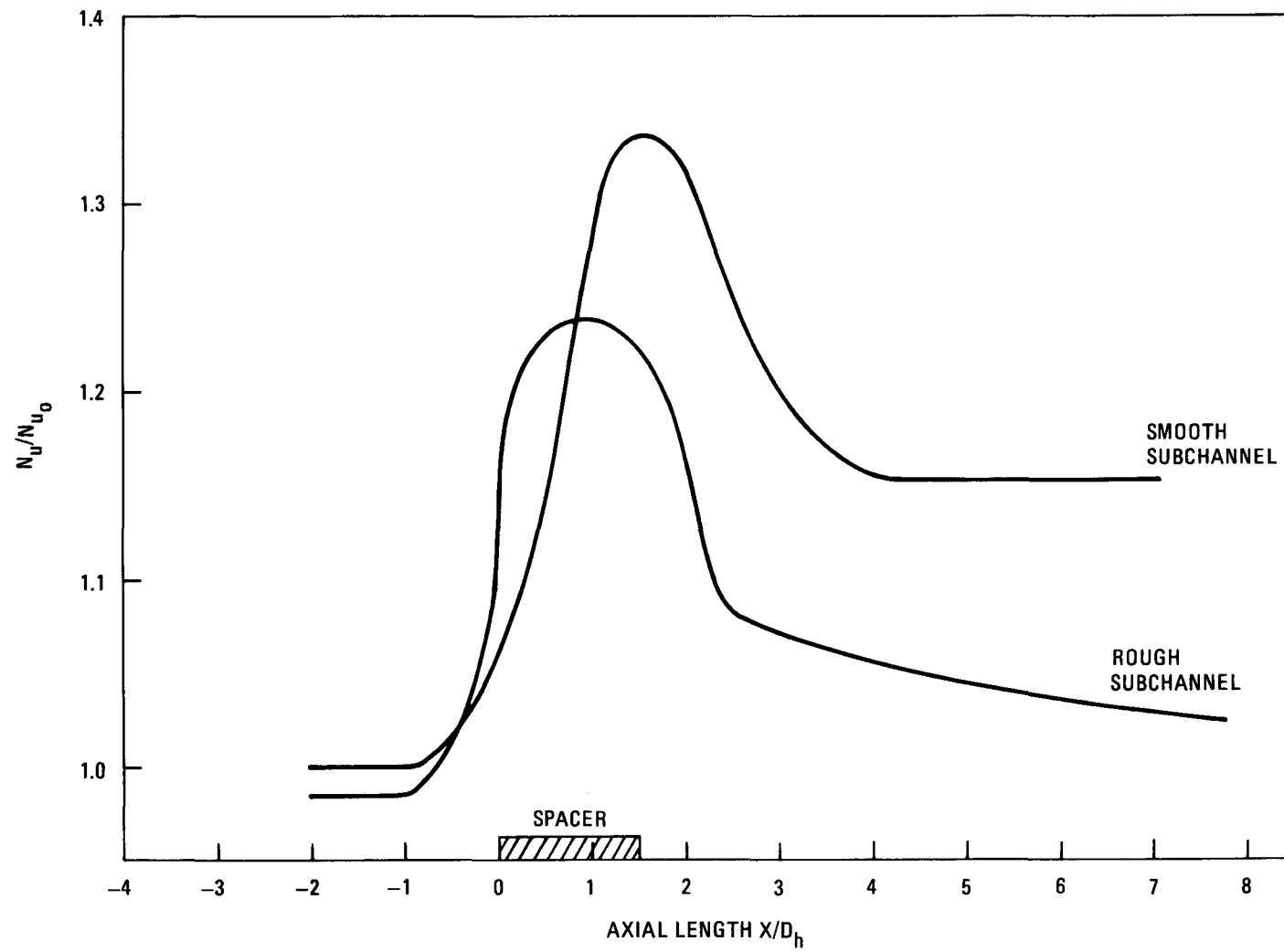


Fig. 2-5. Heat transfer enhancement due to spacers

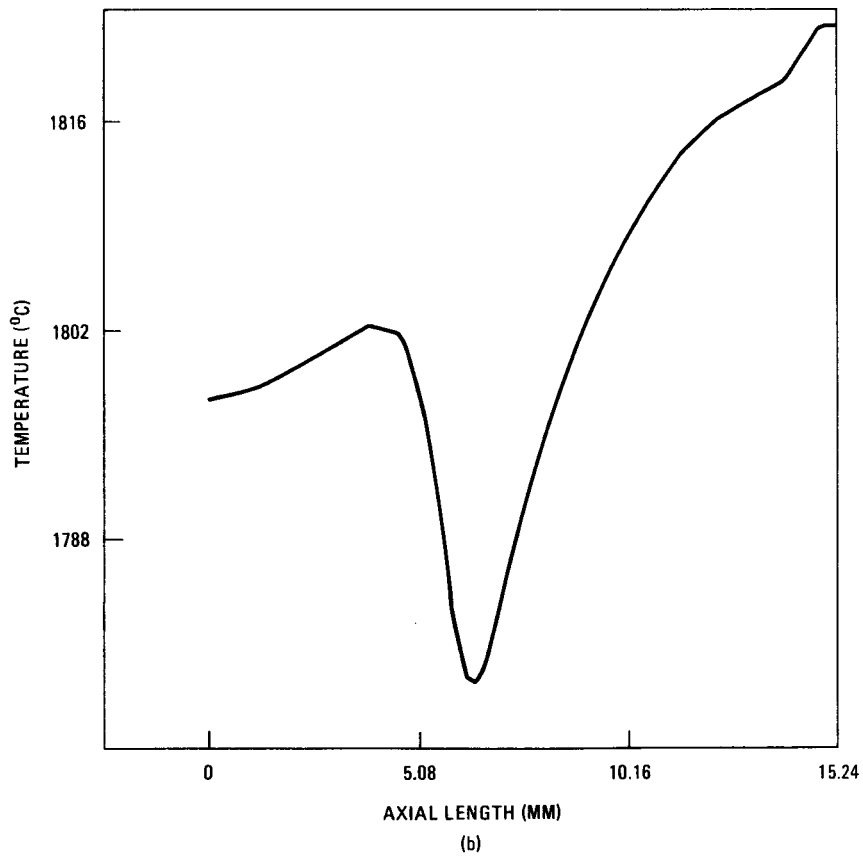
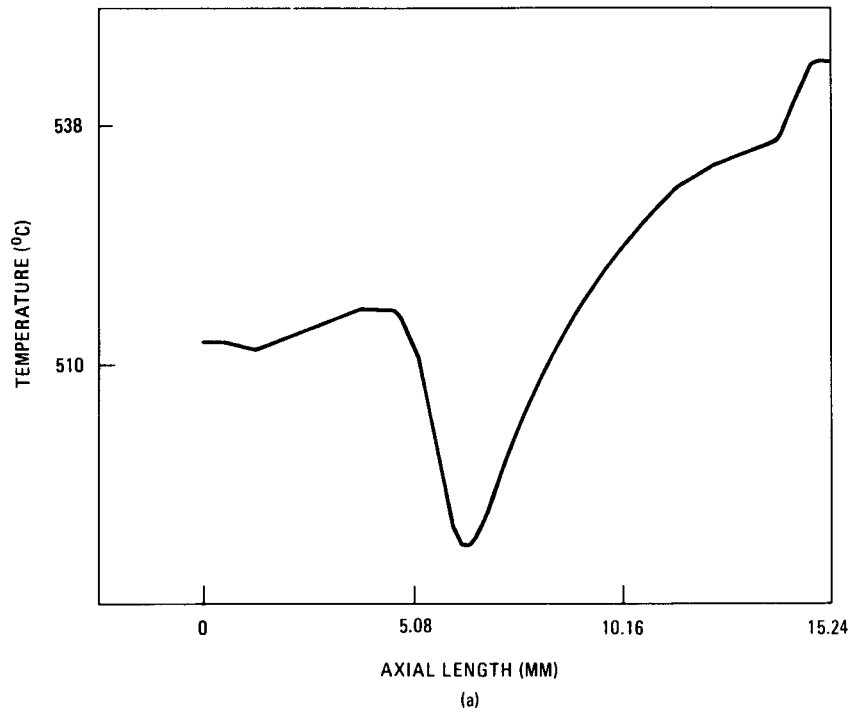


Fig. 2-6. Effect of spacer in smooth region: (a) cladding o.d. temperature; (b) fuel centerline temperature

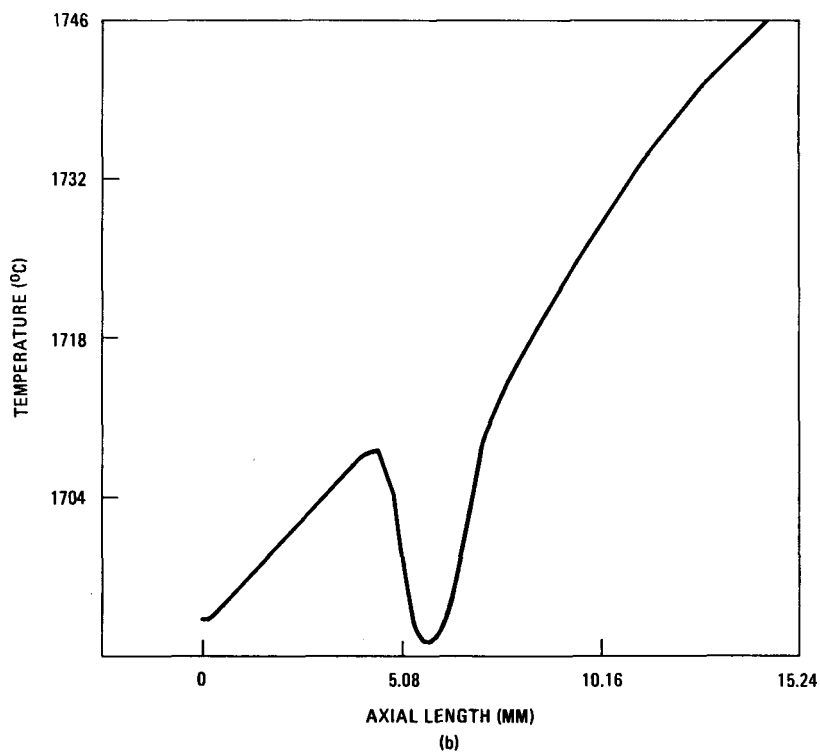
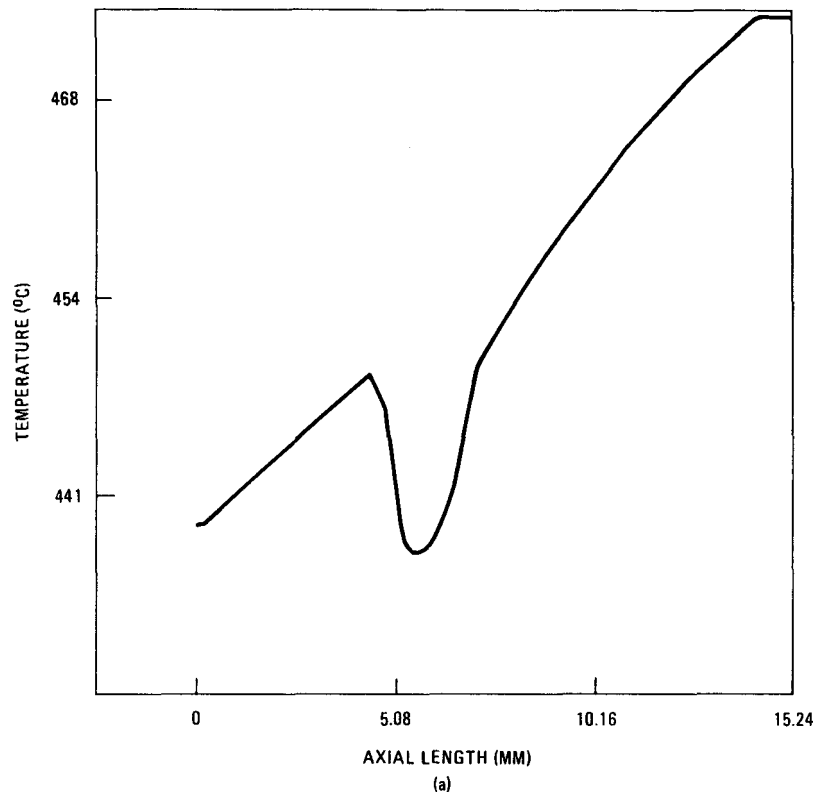


Fig. 2-7. Effect of spacer in roughened region: (a) cladding o.d. temperature; (b) fuel centerline temperature

TABLE 2-4
TEMPERATURE VARIATIONS AT FUEL ROD
SURFACE AND CENTERLINE

Case	Heat Transfer Coefficient Variation		Temperature Variation	
			Cladding Surface (°C)	Fuel Centerline (°C)
	Magnitude	Axial Length		
Rib temperature variation	+460 -50	One rib pitch (1.83 mm)	22.2	0
Roughening transition	+90	10 hydraulic diameters (50 mm)	53	49
Spacer in smooth section	+33.5	5 hydraulic diameters (25 mm)	28	24
Spacer in rough section	+24	4 hydraulic diameters (20 mm)	13	17

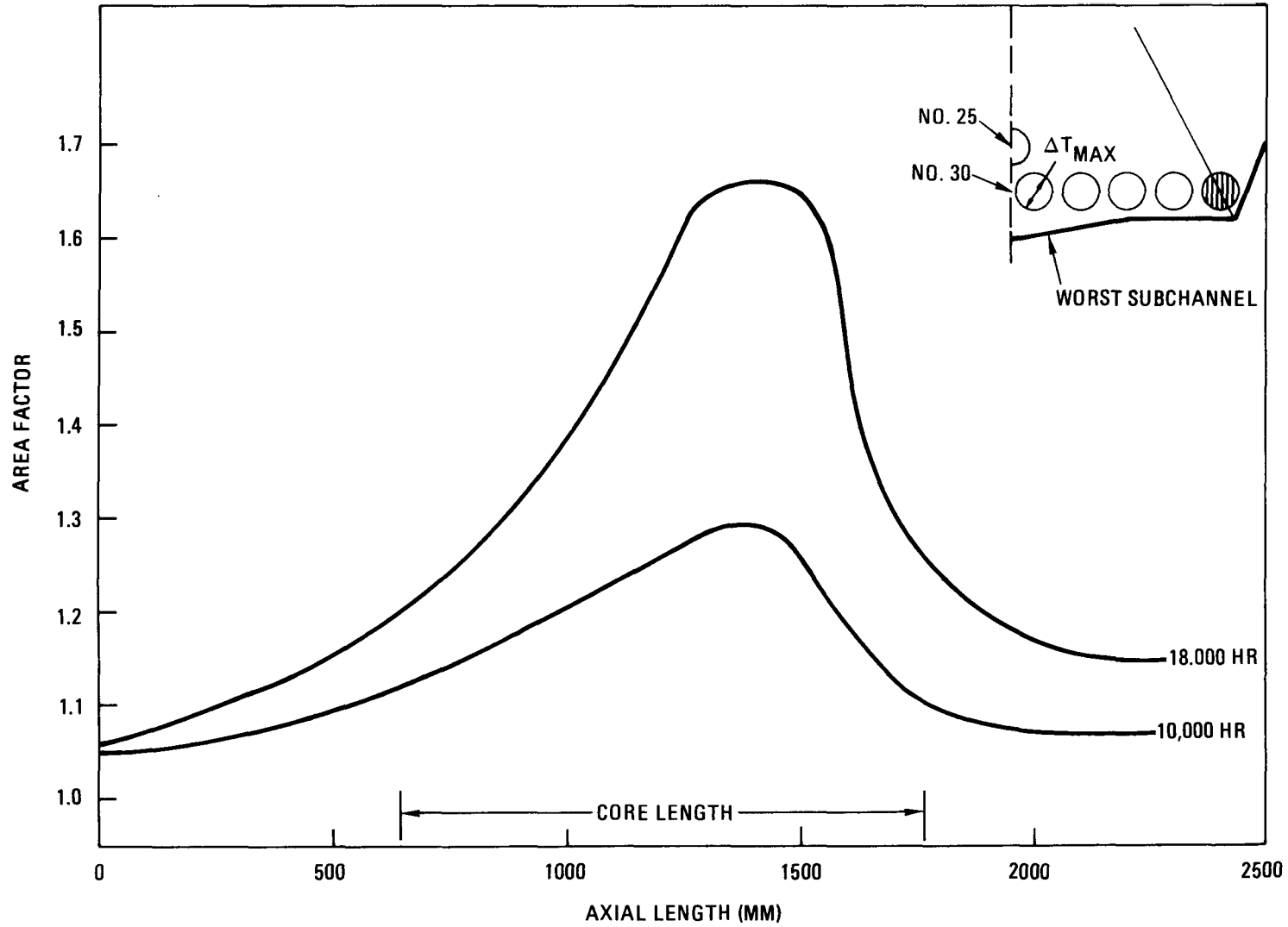
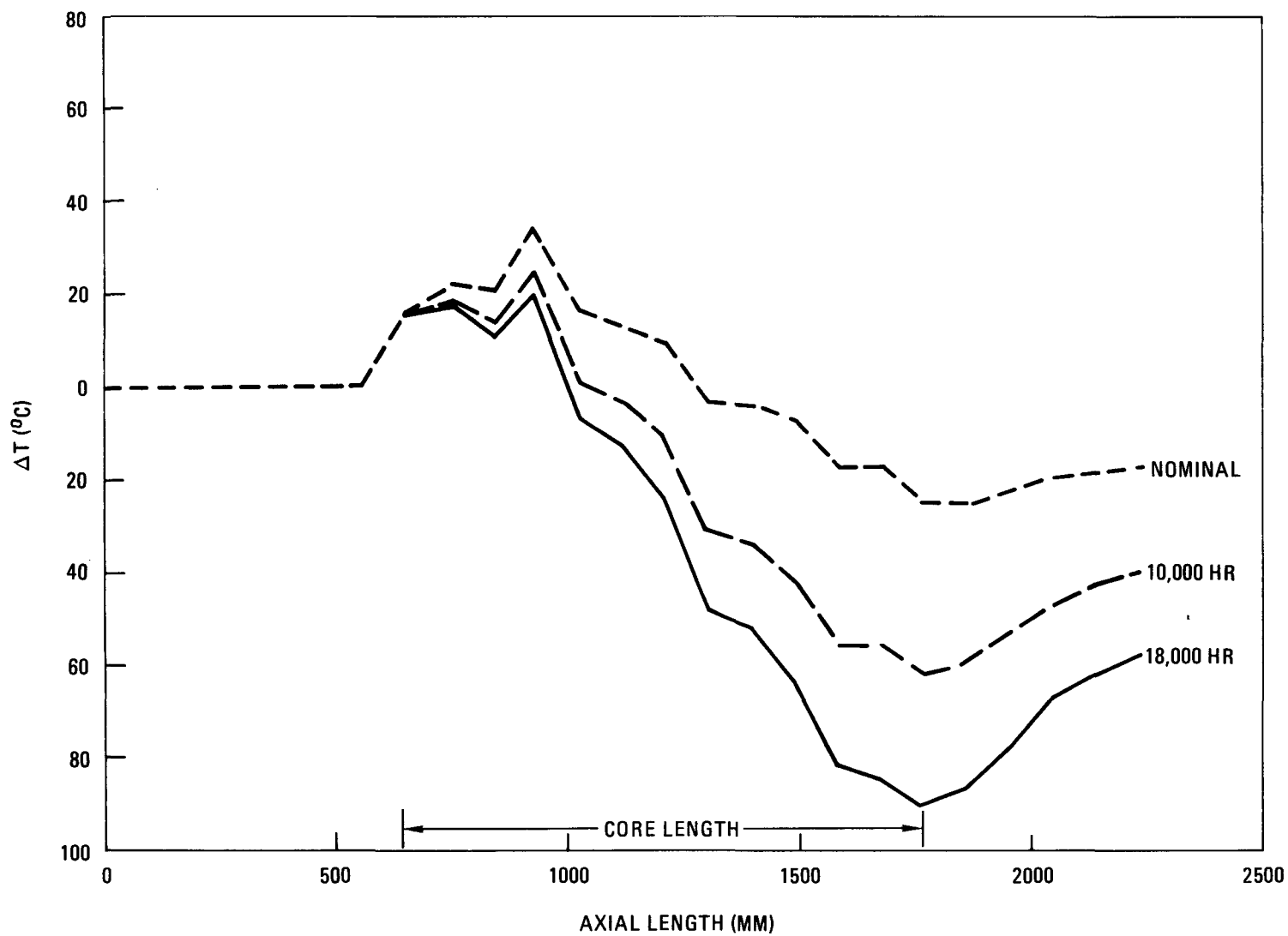


Fig. 2-8. Area change due to duct dilation (worst subchannel)

Fig. 2-9. Worst edge rod ΔT

from 12° to 50°C (Fig. 2-10). The central rods end up 7°C hotter at the end of 18,000 hr; however, this increase does not occur at the location of cladding hot spot temperature.

Bowing of edge rods due to the temperature drop (ΔT) across them at 10,000 hr necessitates the study of larger edge spacings. Although this provides more space for edge rods to bow without contacting the duct wall, it also gives rise to excessive temperature drops across the edge rods owing to overcooling on the wall side. Large solidities for spacers have been introduced into the edge subchannels to reduce the overcooling effect. This helps by diverting the flow from the edge subchannels to the internal subchannels.

Edge gaps of 50%, 80%, and 100% of the rod to rod gap were considered with varying spacer solidities in the edge subchannels. Figure 2-11 indicates the effect of larger solidities on ΔT . Since ΔT in the smooth region with the original spacer solidity was acceptable, a higher-solidity spacer was used in the roughened region to reduce the ΔT at that location (Fig. 2-12). Large solidities were required to achieve this: up to a spacer coefficient (s) of 55% for 80% and 100% edge spacings. It was noticed that with large solidities, the temperature drop varied very rapidly, even alternating from positive to negative. This is because the large amount of flow diverted from the edge subchannels by the spacer returns to the edge immediately downstream of the spacer. Since this can be detrimental to rod bowing, a 43% solidity for edge subchannels in an assembly with 80% edge gap is a design which should be analyzed further.

A similar study of temperature-dependent, radiation-induced, and metal-swelling-induced rod bowing was initiated using fabrication tolerances as the basis for maximum edge channel variations. The study is being done for hexagonal and circular fuel rod arrays, and initial results indicate that hexagonal assemblies perform quite well and circular assemblies do not yield advantages which would warrant a change of assembly configuration.

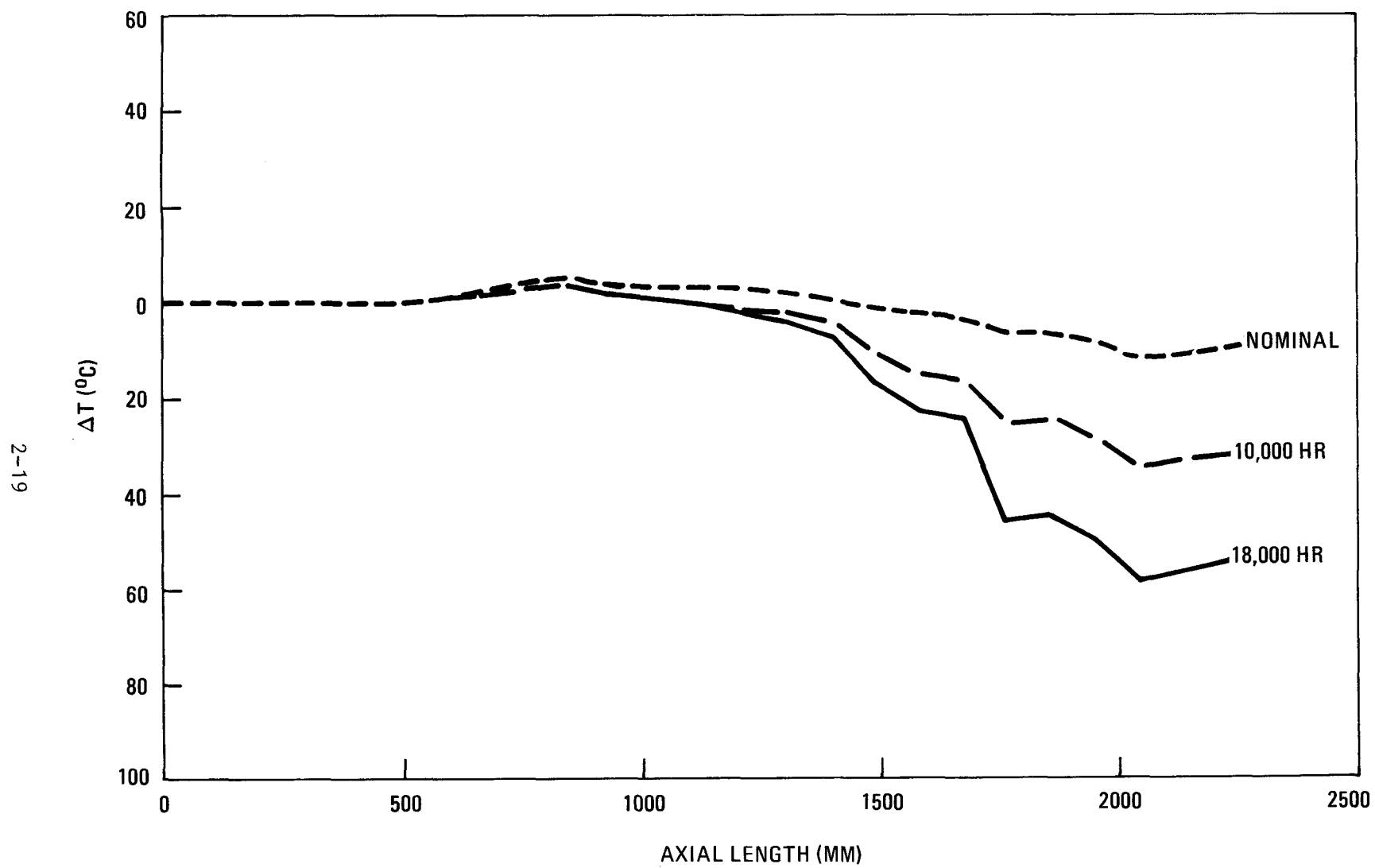


Fig. 2-10. Second-row rod ΔT

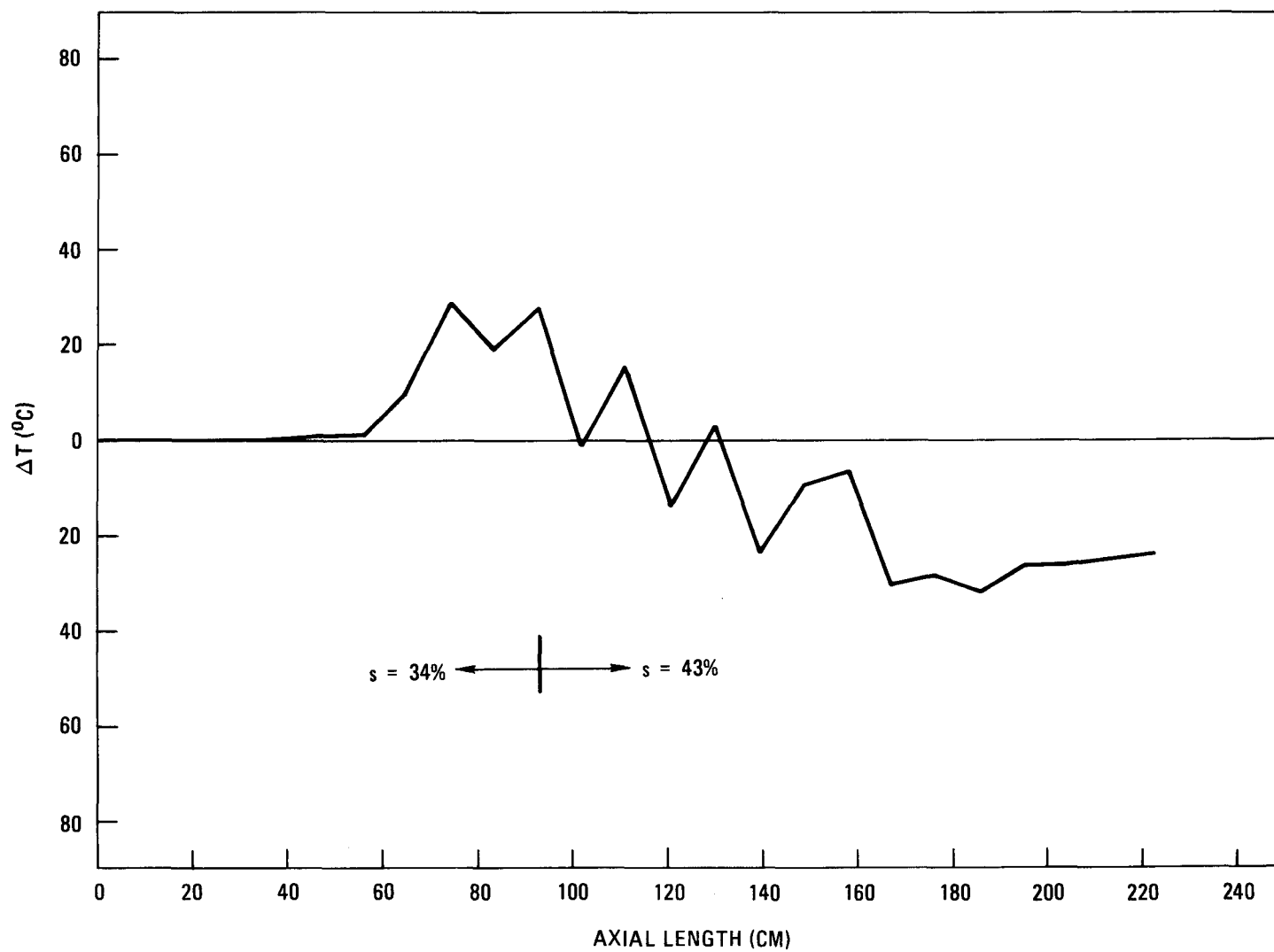
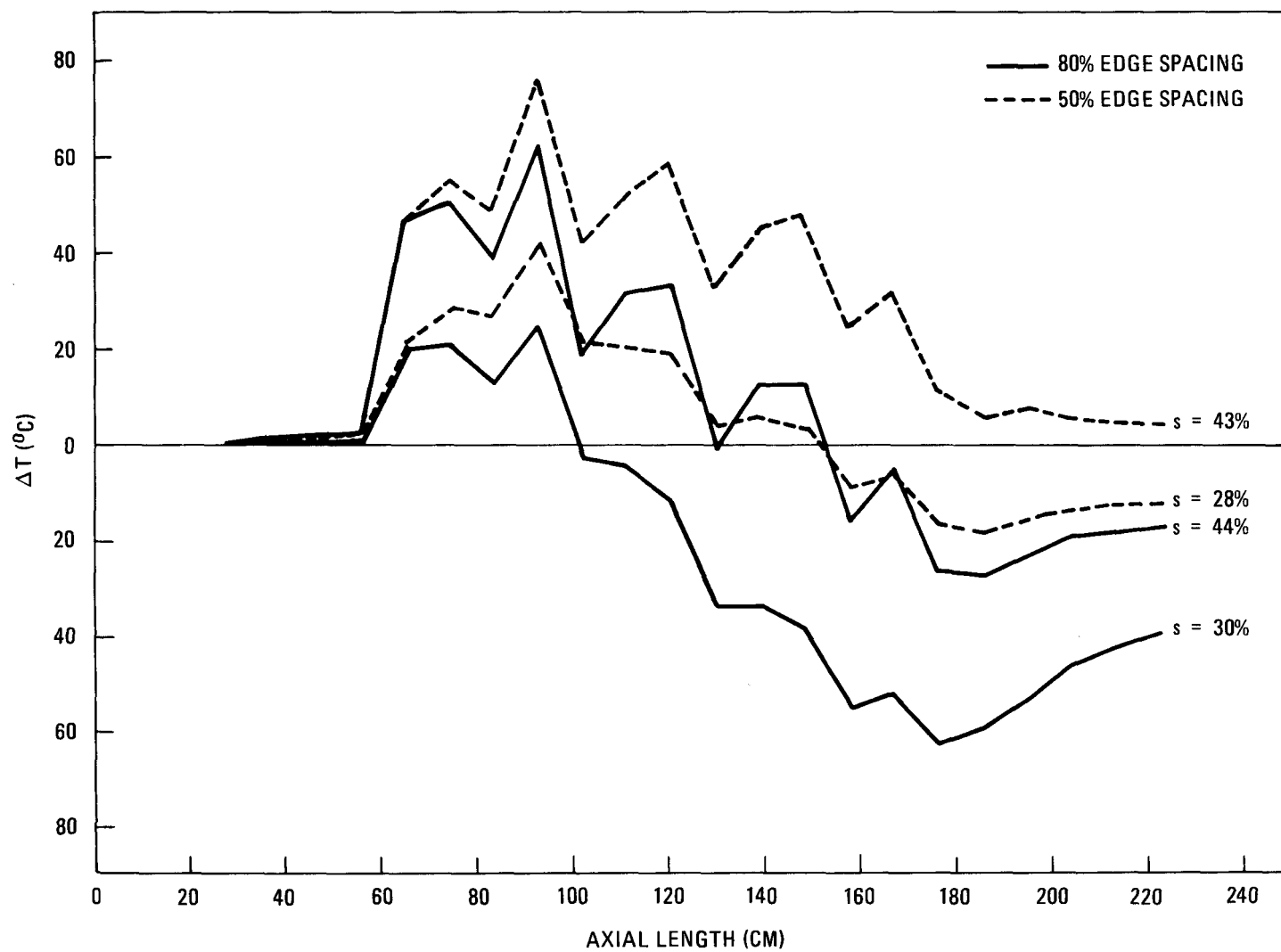


Fig. 2-11. Edge rod ΔT (80% edge spacing) for two spacer types and revised spacer locations

Fig. 2-12. Edge rod ΔT

2.1.3. Control Assembly Analysis

There was no activity on this subtask during this quarter.

2.1.4. Radial Blanket Analysis

Thermal-hydraulic analyses of the radial blanket first-row average-powered assembly and the second-row maximum-powered assembly have been performed for the end of equilibrium cycle (EOEC) condition. The simplified COBRA-IV wire-wrap model which was calibrated against the full-bundle analysis (Ref. 2-6) was used in this study. To maintain the maximum cladding midwall temperature (hot spot) below the limit of 700°C, the required mass flow rates in the first- and second-row assemblies were determined to be 6037 and 1078 kg/hr, respectively. The mixed-mean coolant outlet temperatures for full assemblies were calculated to be 474° and 533°C for the first- and second-row assemblies, respectively. The results of these analyses are being used as input to core performance orificing studies.

2.2. CORE ASSEMBLY MECHANICAL ANALYSIS

2.2.1. Rod Bowing in GCFR Fuel Assemblies

Bowing of fuel rods is a potentially lifetime-limiting consideration in the design of fast breeder reactor fuel assemblies in general and GCFR fuel assemblies in particular. A previous evaluation of the nominal bowing of GCFR edge fuel rods (Ref. 2-6) considered bowing of edge rods (rods located at the periphery of the rod bundle adjacent to the flow duct wall) caused by thermal expansion, differential irradiation swelling, and creep, but neglected the effects of tolerances and flow duct dilation. This analysis has been extended to second-row rods (rods in the row immediately adjacent to the edge row) and has been modified to include the effects of duct dilation and manufacturing tolerances.

The rod bowing analyses were performed using the CRASIB rod bowing model described in Refs. 2-7 and 2-8 and employed the "optimized" spacer

grid arrangement (10-3) developed for that study. The thermal input was obtained from COBRA-III (Ref. 2-9) analyses of Refs. 2-10 and 2-11. Nominal fuel assembly dimensions were taken from Ref. 2-12 and were modified by the duct dilation data of Ref. 2-13 and the tolerance study in Ref. 2-14, which included duct tolerance information supplied by a representative duct manufacturer.

Edge and second-row rod bowing analysis results are given for three cases: (1) a nominal case which considered only the effect of the beginning-of-life (BOL) temperature gradients and neglected duct dilation and tolerances; (2) a case which incorporated the effect of average-over-life (AOL) duct dilation; and (3) a modified assembly design which included the effects of duct dilation and manufacturing tolerances. The results of the third case were compared with rod bowing criteria, and a fuel assembly lifetime limit was established.

2.2.1.1. Nominal Case. The thermal-hydraulic data used as input for the bowing analysis of the nominal case are shown in Figs. 2-13 and 2-14 for the edge rod and second-row rod, respectively. As can be seen from the figures, the nominal temperature gradient across the edge rod varies from about $\pm 25^\circ$ to about -35°C [the sign convention is that a positive gradient indicates that the side toward the duct is hotter]. The reason for this variation is the different thermal-hydraulic characteristics of the edge channels in the smooth region of the rod bundle compared with those of the roughened region. This effect is also present in the second-row rods, but is much smaller. The resulting rod bowing shapes are illustrated in Fig. 2-15, which shows the edge rod, the second-row rod, and the duct shapes plotted to show the gaps between them (touching of the lines on the plot would indicate contact between the respective components). As can be seen, because of the very small gap between the edge rod and the duct (~ 1.5 mm), there is very limited room for the edge rod to bow, and in fact, by the end of life (EOL), very little gap would remain at a point in the active core approximately 1200 mm from the grid manifold. This case is not very representative of the actual situation, because in reality, by EOL the duct will have moved (dilated) outward, increasing the room available for

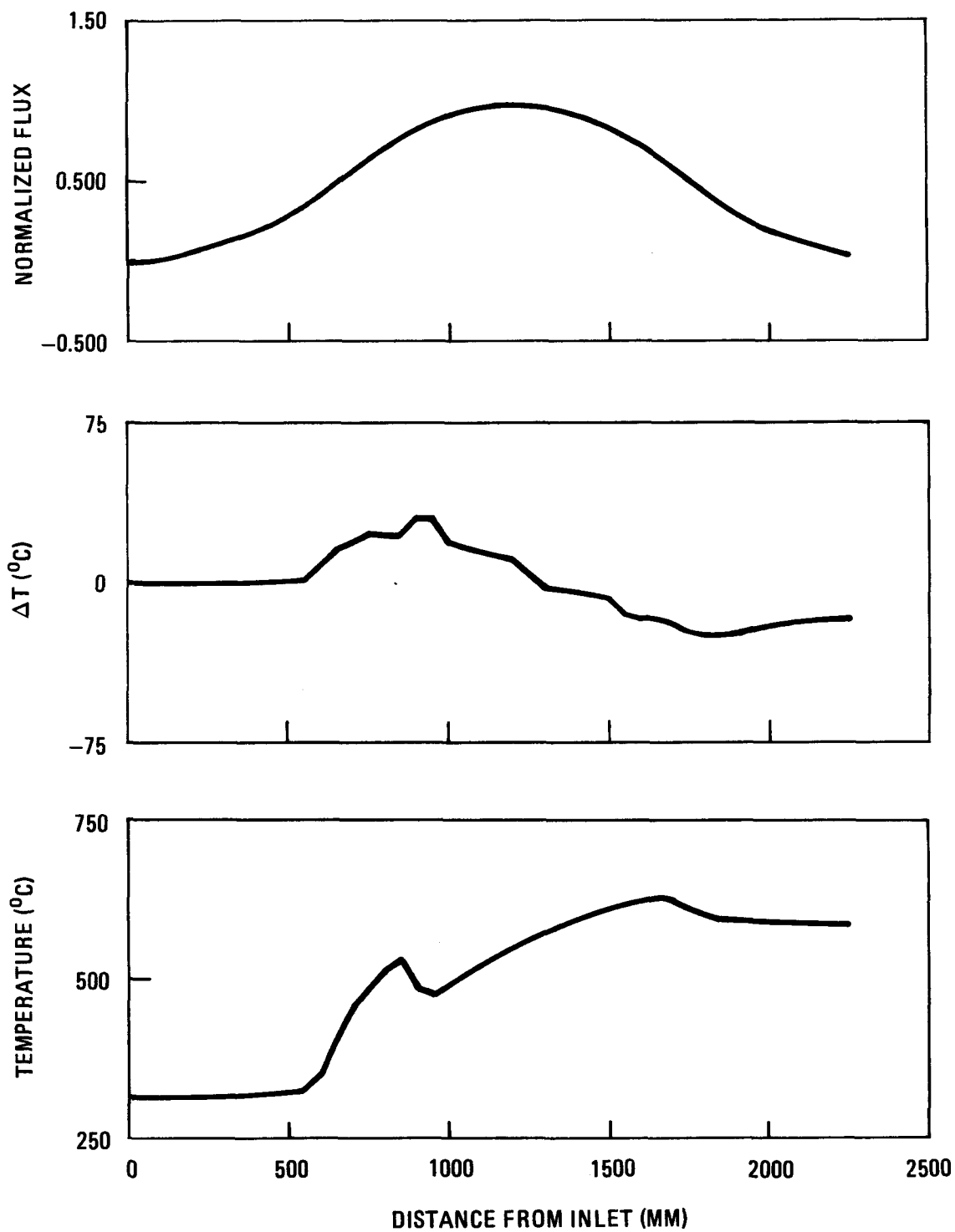


Fig. 2-13. Input for nominal case, worst edge rod hex assembly (52% gap, 75% roughening, BOL nominal gap)

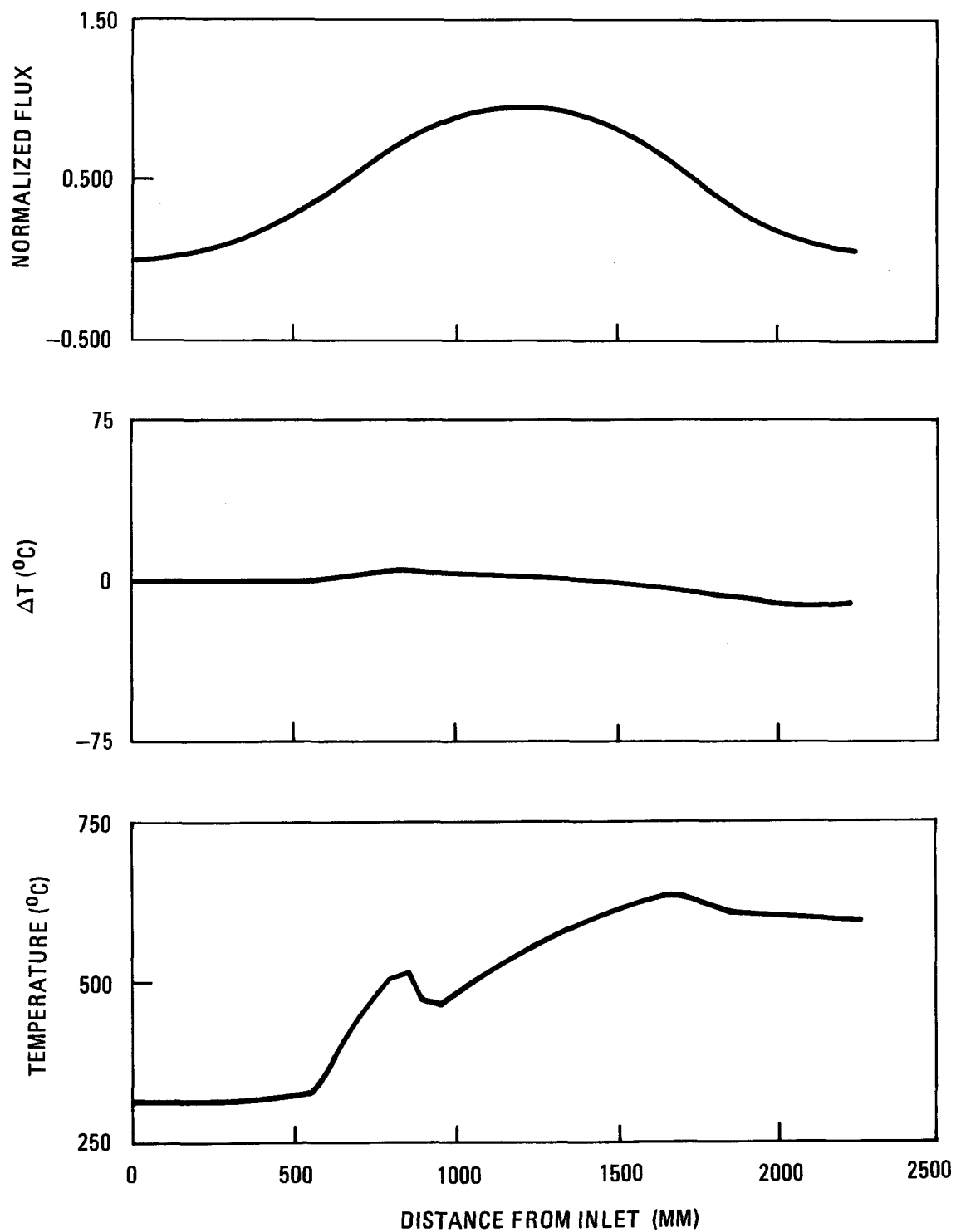


Fig. 2-14. Input for nominal case, worst second-row rod hex assembly (52% gap, 75% roughening, BOL nominal gap)

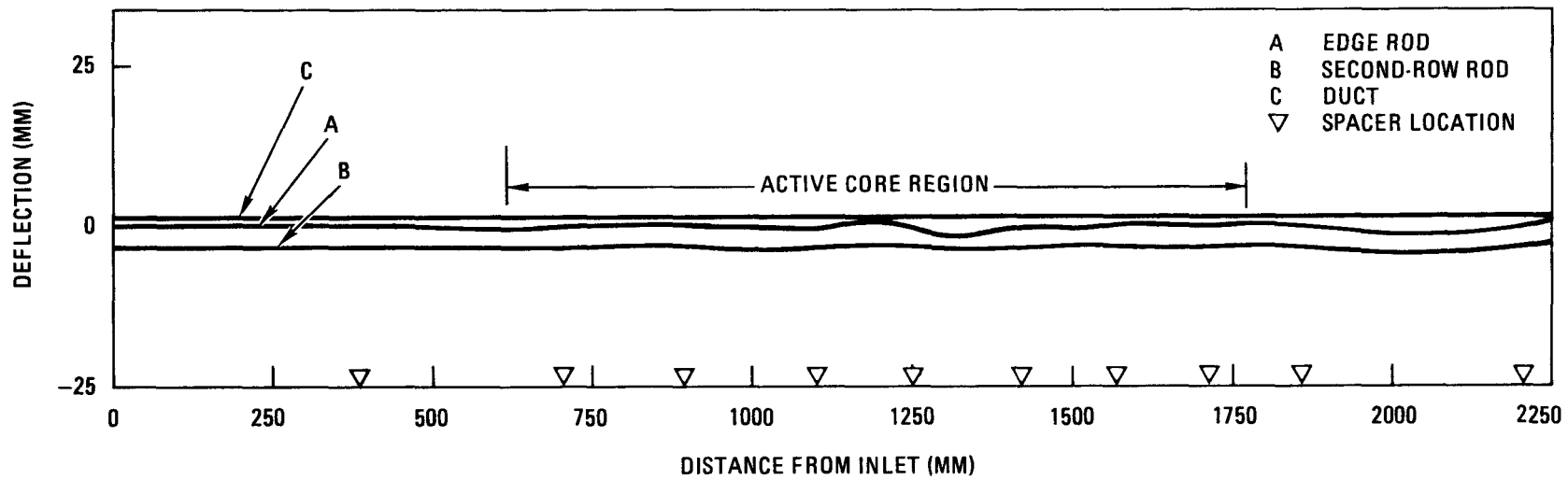


Fig. 2-15. EOL deflections for optimized 10-spacer grid arrangement, nominal configuration, nondilating duct case

the rod to bow. This also increases the magnitude of the rod transverse temperature gradients, and hence aggravates the bowing. This is discussed in Section 2.2.1.2.

2.2.1.2. Nominal Case With Duct Dilation. The effect of duct dilation is to increase the area of the edge flow channels, causing a flow redistribution within the bundle which increases the magnitude of the rod transverse temperature gradients. The duct dilation data of Ref. 2-13 were converted to subchannel area variation factors for COBRA-III thermal-hydraulic analysis. These factors represent the increase in subchannel area over the nominal area and are illustrated in Fig. 2-16 for two times in the assembly lifetime. The 10,000-hr data represent the approximate AOL values. One of the reasons for the factors being as large as they are is that with the edge rod to duct spacing of the reference design (which equals 52% of the rod to rod spacing), the initial area is very small, making the percentage change in area quite sensitive to very small absolute area changes. The resultant edge and second-row rod temperatures and transverse temperature gradients are illustrated in Figs. 2-17 and 2-18, which show that the temperature gradients for both rods increase dramatically. These increased temperature gradients result in significantly increased EOL rod bows (Fig. 2-19). Although rod to duct interaction in the active core region does not appear to be a problem, interaction between the edge rod and the second-row rod does seem to be a problem, particularly near the end of the active core, where rod to rod contact appears imminent. This clearly represents an inadequate situation, particularly since the effects of tolerances have not yet been incorporated.

As mentioned above, one of the reasons that duct dilation has such a severe effect on bowing is that the original edge channel area is very small. In an attempt to reduce this sensitivity, efforts were undertaken to increase the edge channel gap while holding the rod transverse temperature gradients to a minimum. This was accomplished by employing increased spacer solidities in the edge channel to cause flow to be diverted back toward the interior of the bundle. After much trial and error manipulation of various combinations of edge channel gaps and spacer edge channel

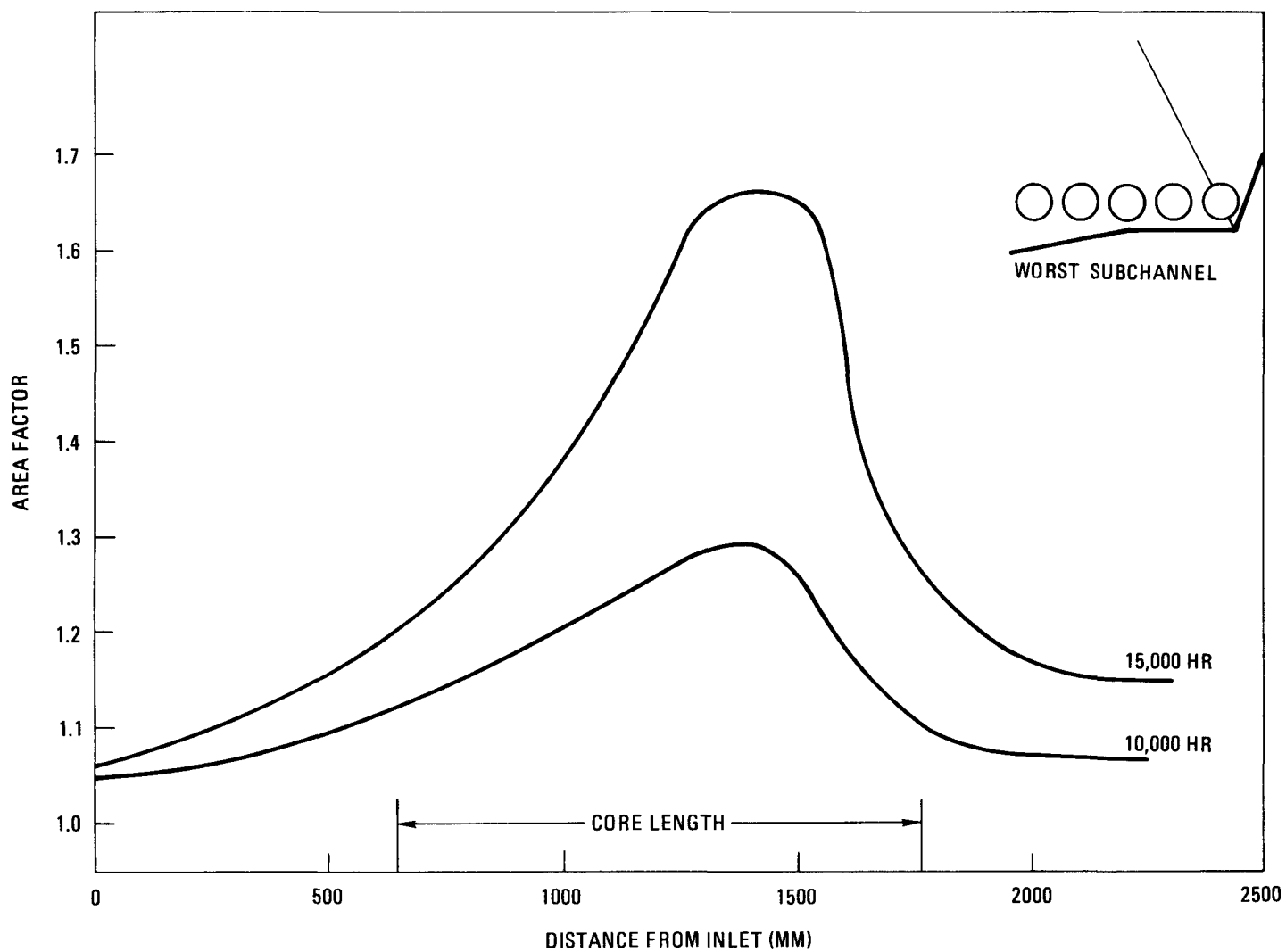


Fig. 2-16. Area change due to duct dilation, worst subchannel

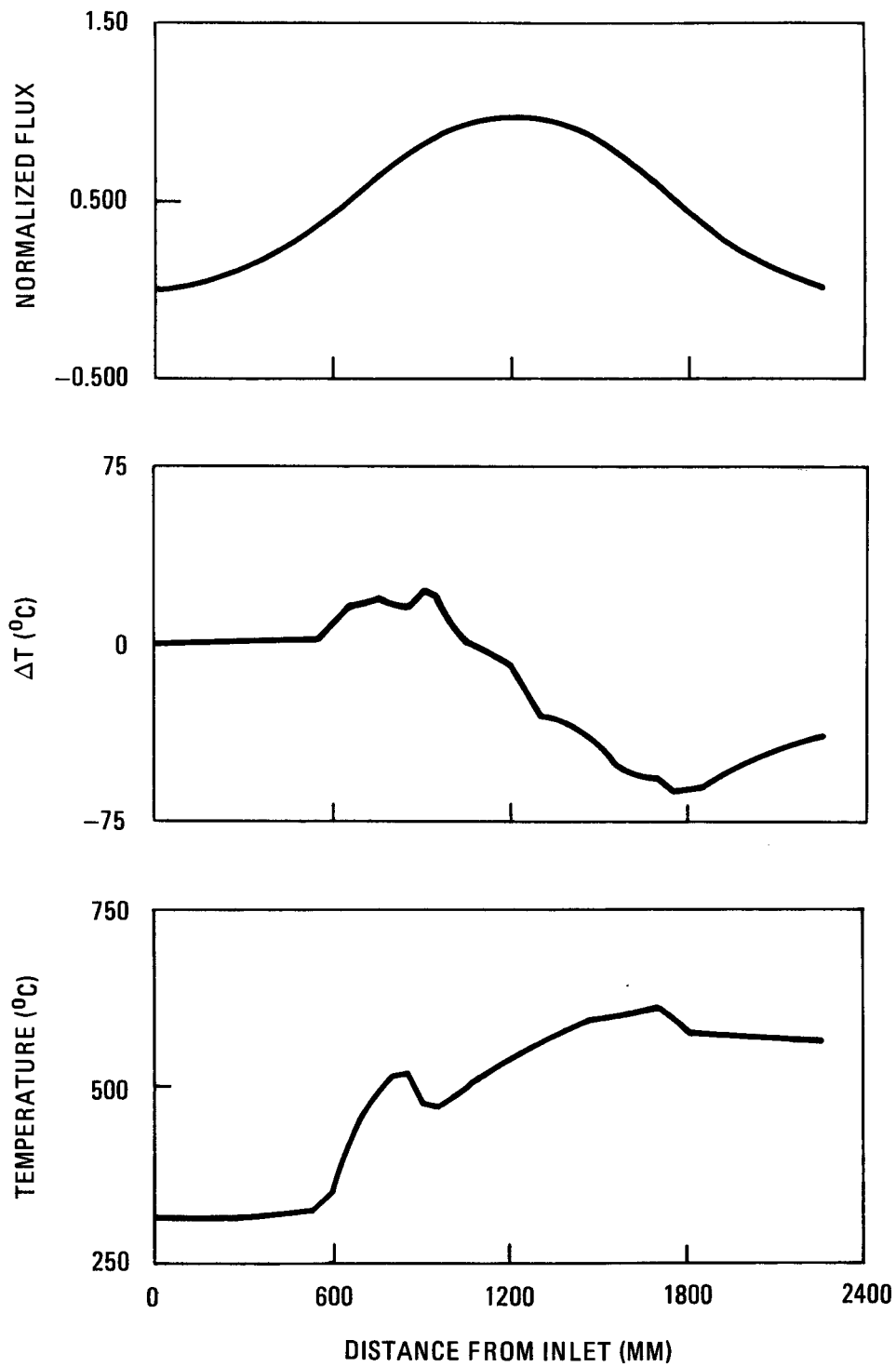


Fig. 2-17. Input for edge rod, 10,000-hr dilated duct

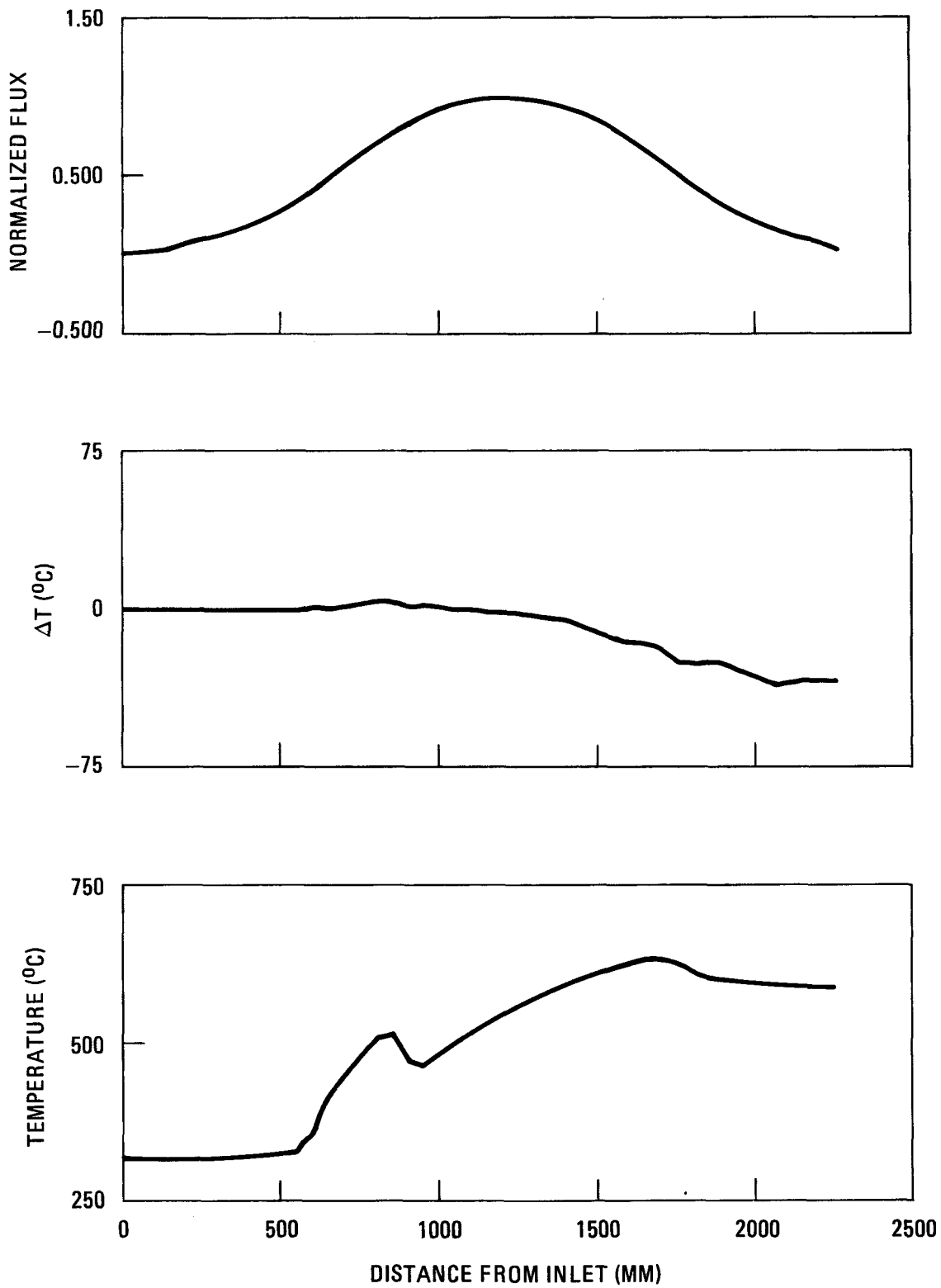


Fig. 2-18. Input for second-row rod, 10,000-hr dilated duct

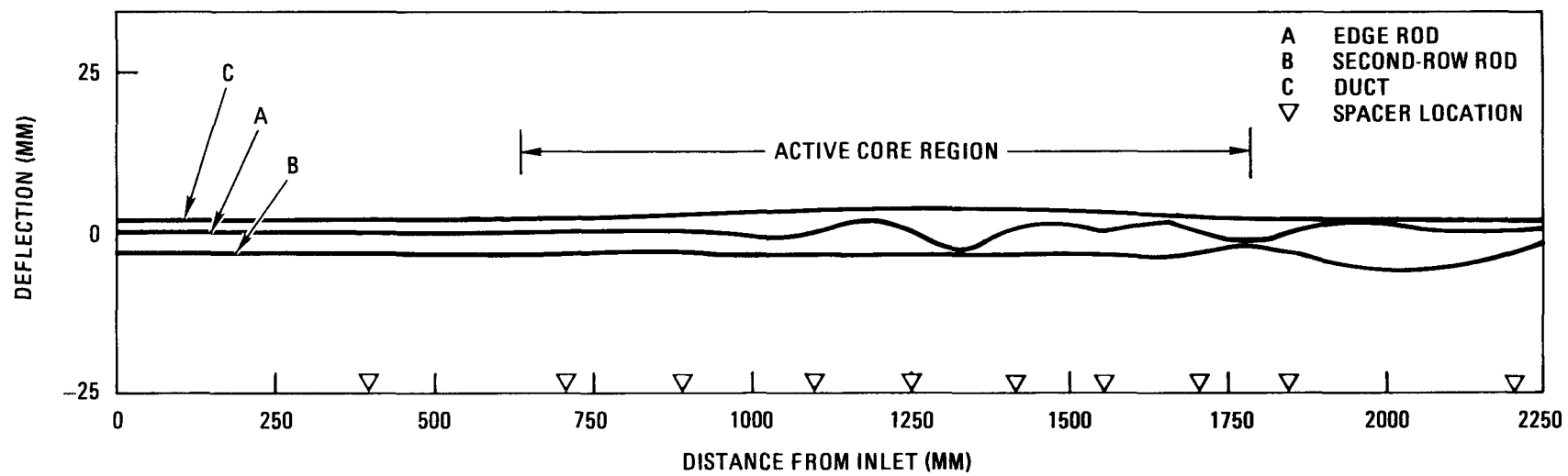


Fig. 2-19. EOL deflections for optimized 10-spacer grid arrangement, nominal configuration

solidities, a combination of 80% gap and two spacer solidities was found which appeared worthy of additional analysis.

2.2.1.3. Rod Bowing for Revised Design, Including Effects of Dilation and Manufacturing Tolerances. In addition to duct dilation, duct and bundle manufacturing tolerances can have a very significant effect on rod transverse temperature gradients. A tolerance study was performed on the revised design to establish the maximum and minimum expected edge channel gaps. This study incorporated actual duct tolerance information obtained from a representative duct manufacturer.

The edge rod and second-row rod temperatures and temperature gradients for this case are shown in Figs. 2-20 and 2-21. These curves include the effects of AOL duct dilation and the worst expected tolerances. Even with the added effect of tolerances, the edge rod gradients are no worse than the edge rod gradients of the previous case (Fig. 2-17), although the second-row gradients are quite a bit more severe. The fluctuations present in the temperature gradient data are caused by flow redistribution resulting from the large spacer solidities employed in the edge channels. It was found that these fluctuations could have a marked detrimental effect on rod bowing in some cases, and they represent a limitation to the amount of edge channel spacer solidity that can be employed for the purpose of flow redistribution.

Rod bowing studies were performed for this configuration and were compared with the rod bowing criterion proposed in Ref. 2-15. Briefly, this criterion states that the gap between the rods must not be reduced to less than 1 mm and the rod to duct gap must not fall below 0.5 mm. For this relatively severe rod bowing condition, the modified assembly design satisfied the rod bowing criteria up until approximately 18,000 hr, or about 85% of the design life of ~21,000 hr, at which time the rod to rod proximity limit was reached. Figure 2-22 illustrates the rod and duct shapes at that time, and Fig. 2-23 shows the rod to duct gap versus the distance from the grid manifold. It can be seen that the gap is everywhere greater than the minimum allowable gap, which is plotted as curve B.

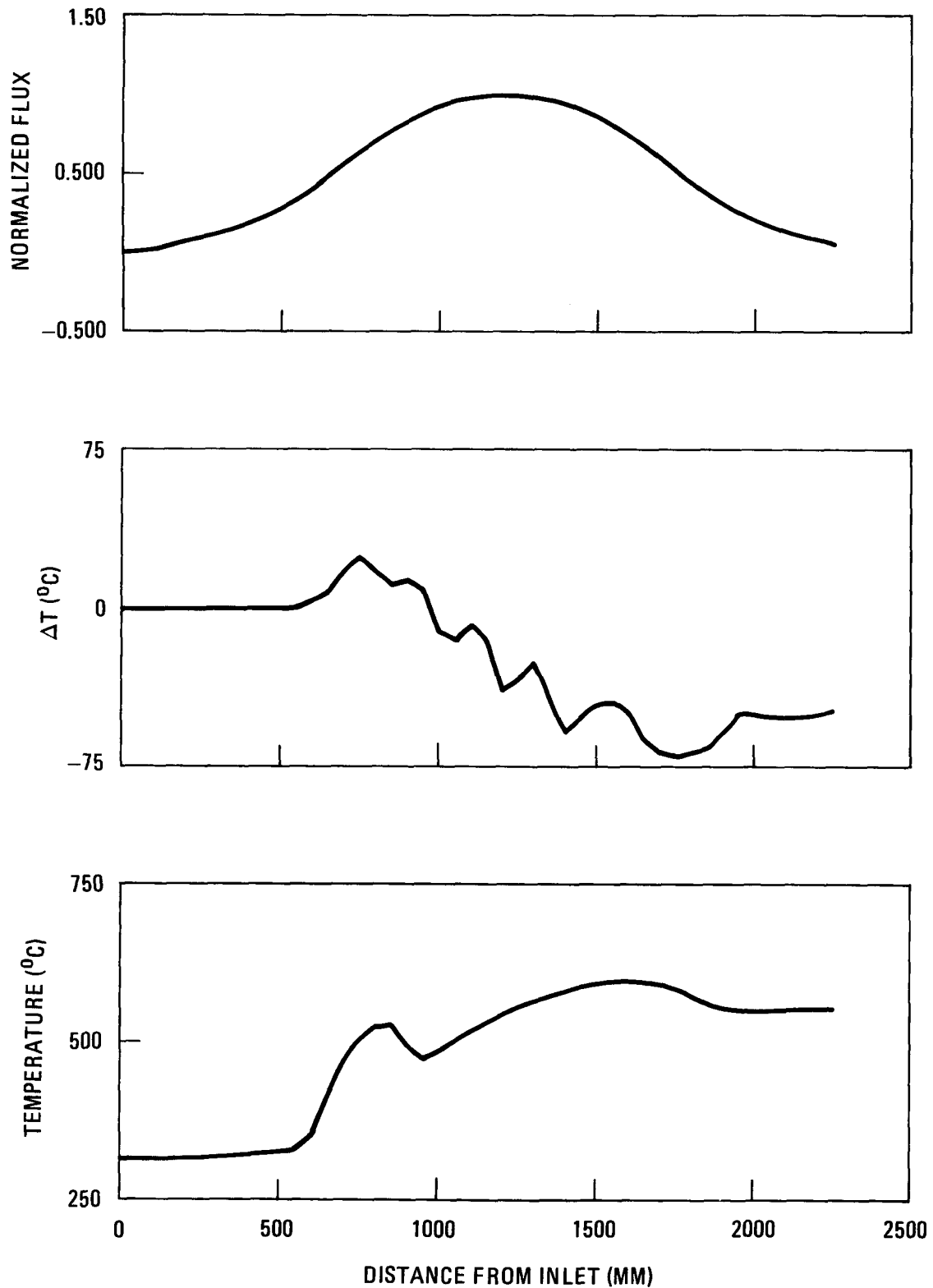


Fig. 2-20. Input for revised design, worst edge rod hex assembly [80% gap, 75% roughening, maximum gap (average over life)]

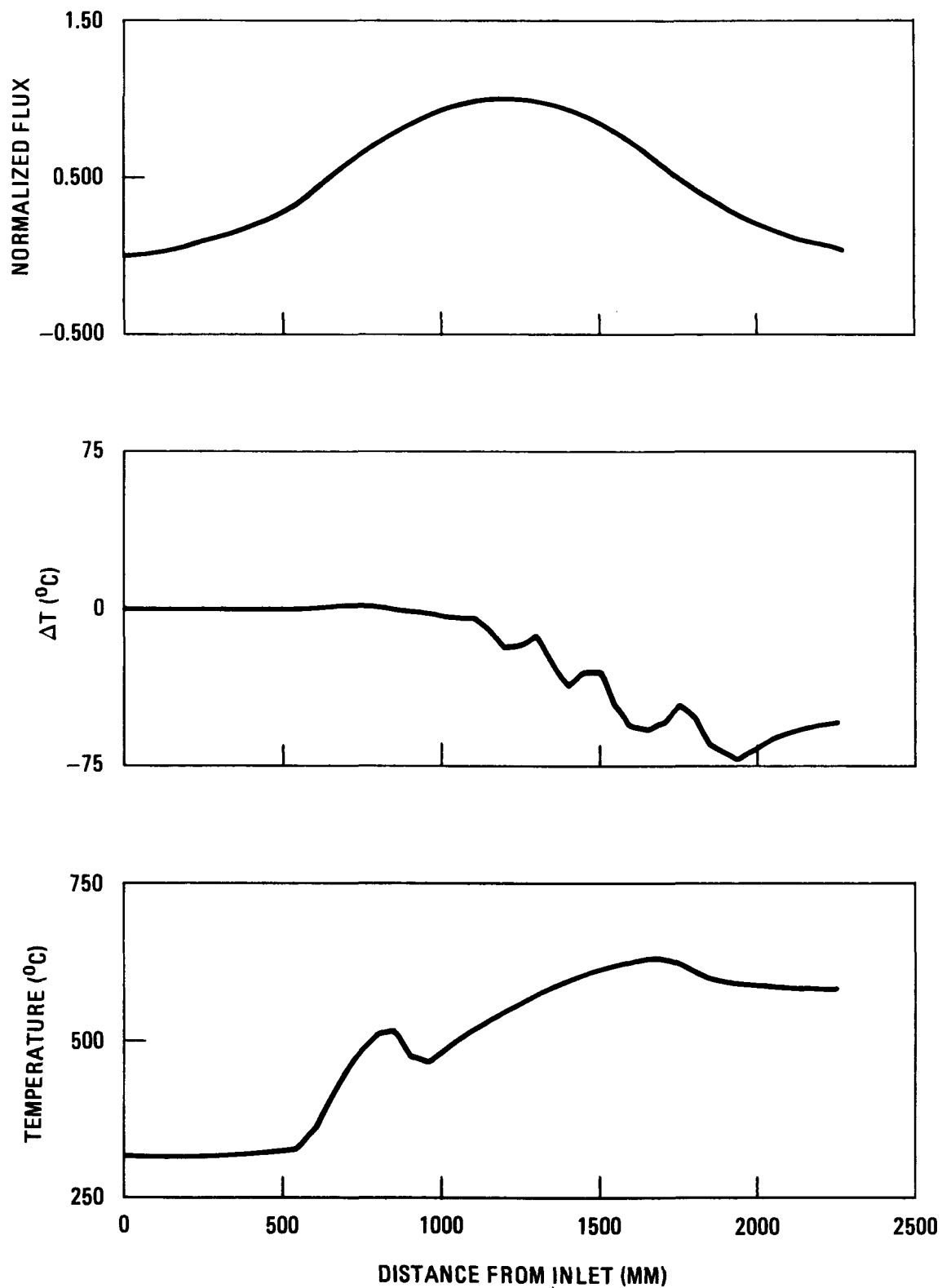


Fig. 2-21. Input for revised design, worst second-row rod hex assembly
[80% gap, 75% roughening, maximum gap (average over life)]

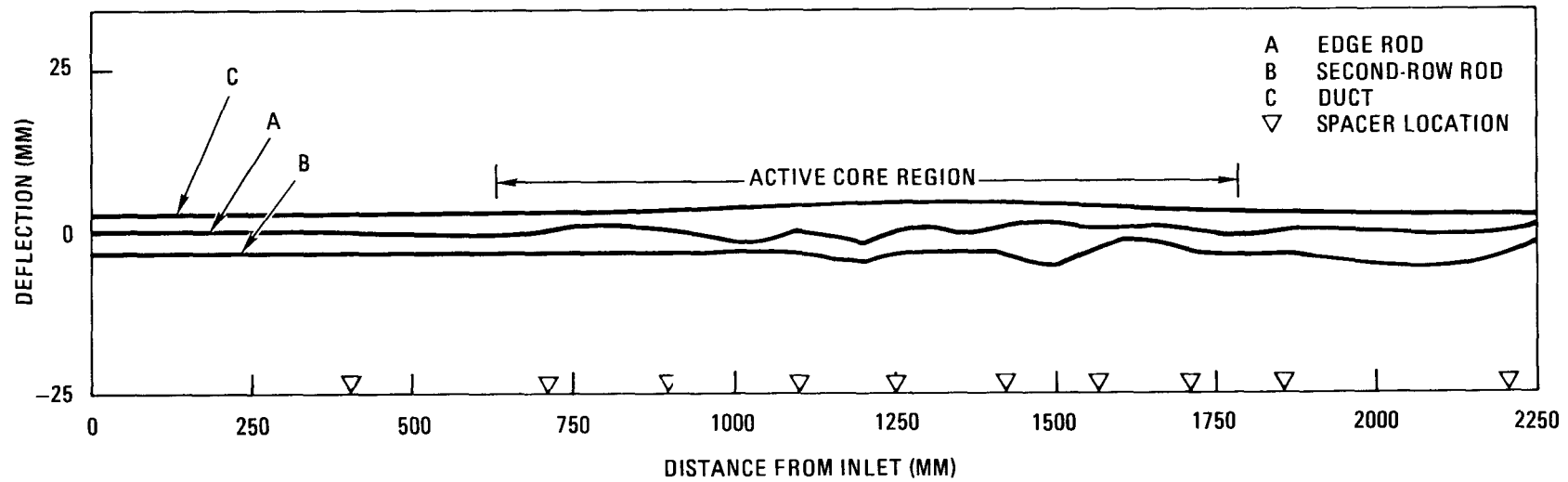


Fig. 2-22. Rod and duct shapes for 316 stainless steel hex duct case, 18,000 hr (10.2-mm pitch, 80% gap, 75% roughness, worst expected tolerances plus average over life duct dilation)

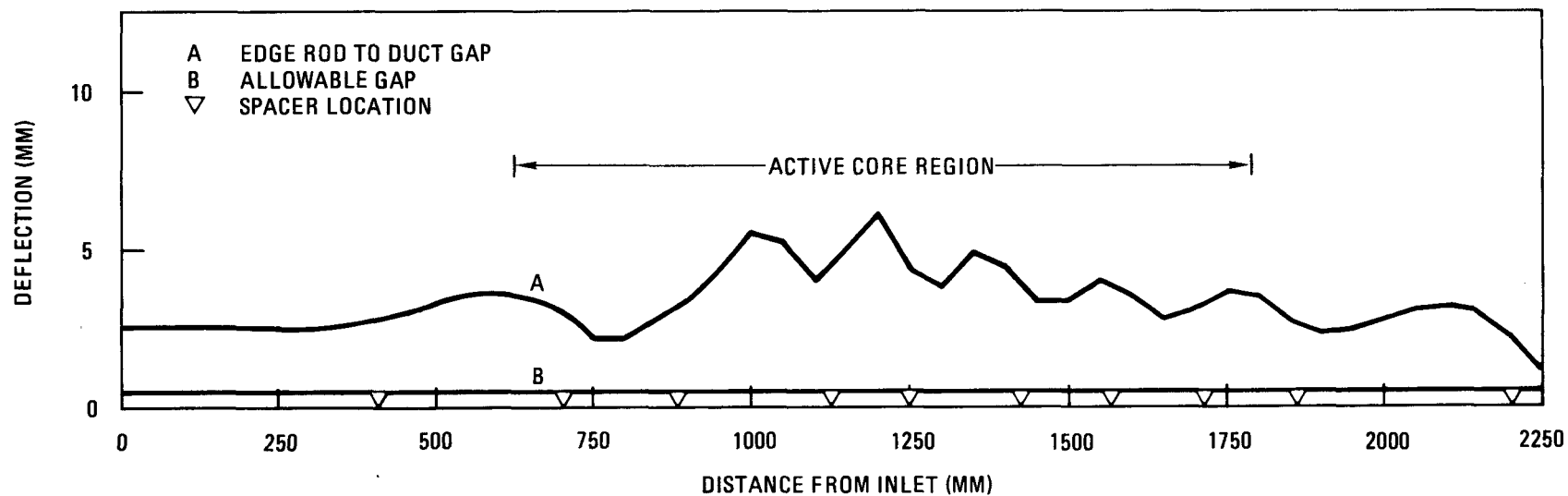


Fig. 2-23. Rod to duct gap vs distance from grid manifold for 316 stainless steel hex duct case, 18,000 hr (10.2-mm pitch, 80% gap, 75% roughness, worst expected tolerances plus average over life duct dilation)

A similar plot of the edge rod to second-row rod gap is presented in Fig. 2-24, which shows that the minimum gap criterion is met at a point in the active core about 1000 mm below the grid manifold and is also about to be met at a point about 1600 mm below the grid manifold.

In summary, it appears that with some modification of the fuel assembly design, rod bowing in the presence of duct dilation and assembly tolerances can be accommodated with only minor reductions in assembly lifetime. For the particular conservative case studied, it was shown that an assembly lifetime of 18,000 hr could be reached before rod bowing criteria were violated. It appears possible that the full design assembly lifetime of ~21,000 hr can be reached with further refinements in the design and/or analysis.

2.2.2. Thermal Shock of the Fuel Assembly Flow Duct

Reactor trip transients using an electric-motor-driven circulator result in rapid variations in coolant temperature. The assembly components, however, experience much slower temperature changes owing to the effects of low heat transfer coefficients and thermal conductivities. This results in possible thermal shock failure of components located in the outlet end of the assembly, where the temperature gradients become the most severe.

The thermal shock aspects of the outlet end of the assembly duct have been analyzed using an approximate model (Fig. 2-25) and closed-form solutions. The thermal model was a flat plate which was initially at a uniform temperature equal to that of the outlet gases and subject to a different heat transfer coefficient on each of the surfaces, thus simulating the flowing and stagnant surfaces of the duct wall. The transient was initiated by an instantaneous drop in coolant temperature on one of the surfaces (with the higher heat transfer coefficient), and the transient temperature distribution was computed as a function of time using classical techniques. The structural model was a flat plate which was simply supported at two opposite edges (at the axial planes of the duct

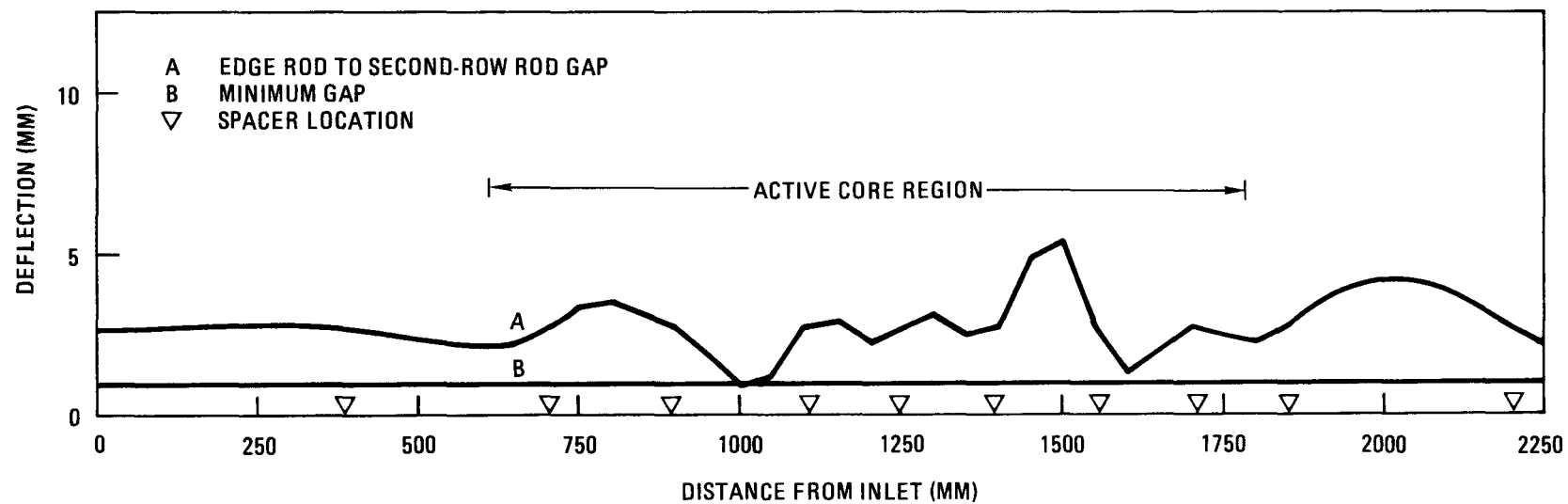


Fig. 2-24. Edge rod to second-row rod gap for 316 stainless steel hex duct case, 18,000 hr (10.2-mm pitch, 80% gap, 75% roughness, worst expected tolerances plus average over life duct dilation)

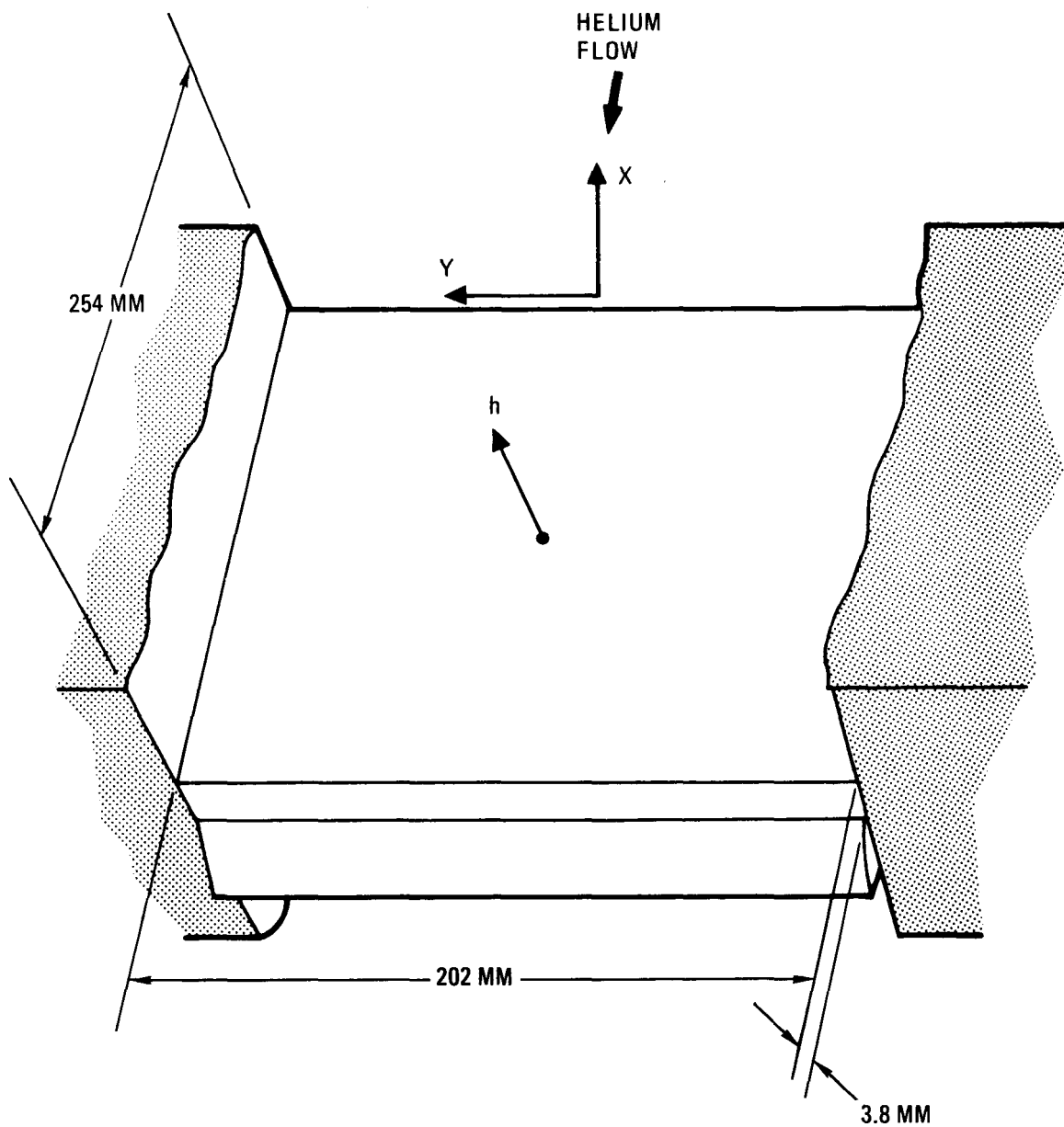


Fig. 2-25. Assembly outlet thermal shock model

outlet and 254 mm above the outlet) and rigidly supported along the other two edges (the corners of the duct wall). The stresses in the plate were found as a function of time based on the previously derived temperature profiles using classical plate equations. The duct wall temperatures were numerically evaluated for times of up to 15 s after initiation of the transient. Times from 0 to 2 s are shown in Fig. 2-26. The stresses were also calculated as a function of time and are shown on Fig. 2-27. It can be seen that the stresses are very low, probably because of the thin wall.

There are two important considerations in interpreting these thermal stresses: fracture and fatigue. At BOL, the material is relatively ductile and the yield and ultimate stresses are high. If the material is sufficiently stressed, it will yield to redistribute the stresses. Consequently, fracture will probably not occur unless the component is severely overstressed. However, later in life, as accumulated fluence becomes large, the ultimate strength will decrease and fracture may occur. At 538°C and a fluence of 2.0×10^{23} nvt, the ultimate strength is 379 MPa. This is much greater than the computed stress of 28.9 MPa.

The second concern arises from the cyclic nature of the shutdown transient. To explore this failure mode, a fatigue curve is needed. Figure 2-28 has been constructed using the methods recommended in the structural design criterion (Ref. 2-16), giving a plot of total strain versus fatigue life. The maximum thermal strain is 1.826×10^{-4} cm/cm, which corresponds to a fatigue life of about 1.2×10^6 cycles. Using a design margin of 20 on life (the greater of 2 on strain or 20 on life as specified in the structural criterion) gives a design life of 6.5×10^4 cycles. Assuming that damage from these transients should be limited to 10% of the total accumulated damage to provide a reasonable allowance for other events, the maximum number of occurrences should be limited to 3250 in the 3-yr life of the assembly. This is probably not a limiting factor. Consequently, transients caused by a reactor trip with an electrically driven circulator pose no problems in terms of thermal stresses to the outlet end of the assembly flow duct.

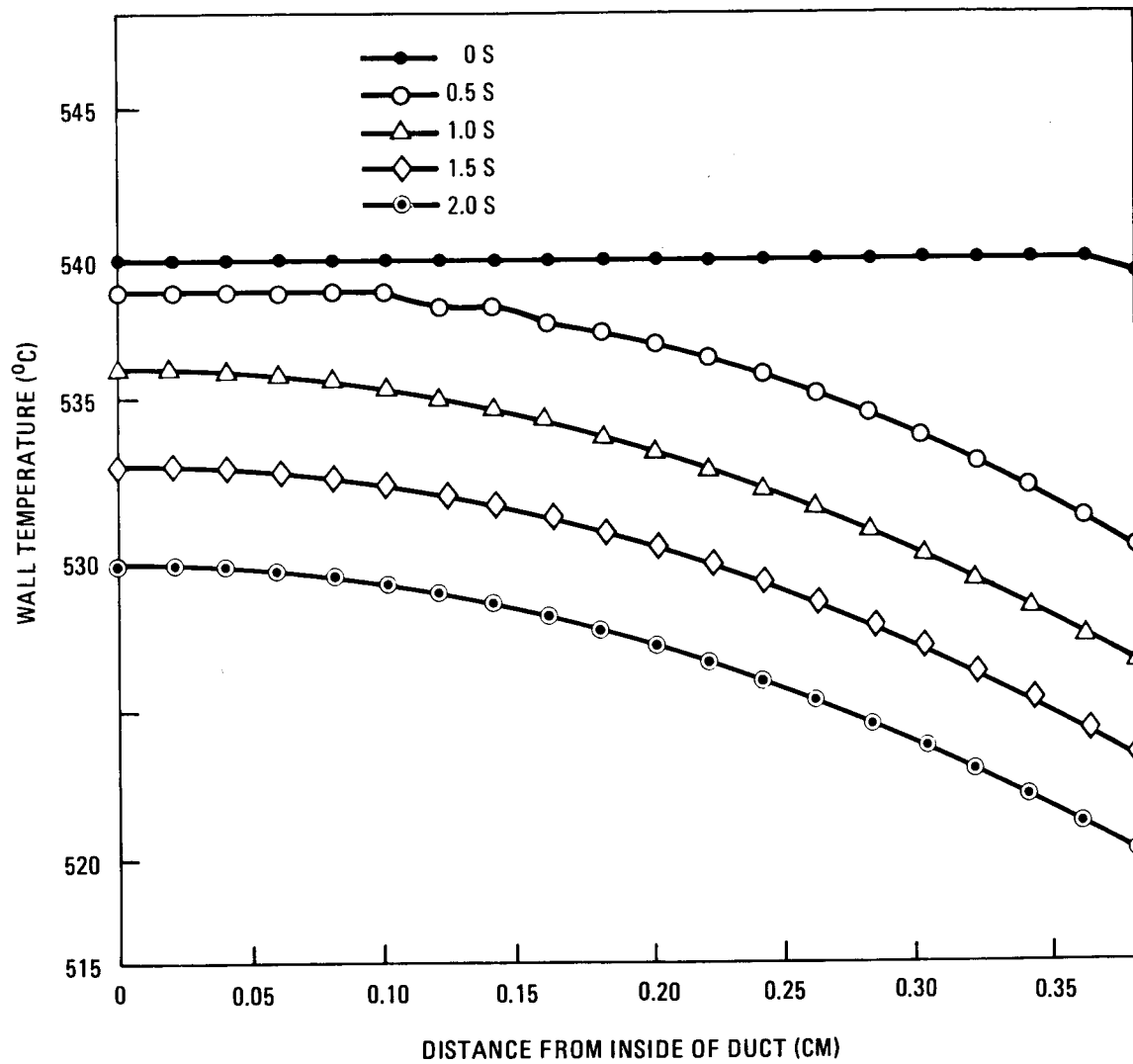


Fig. 2-26. Duct wall temperature during thermal shock

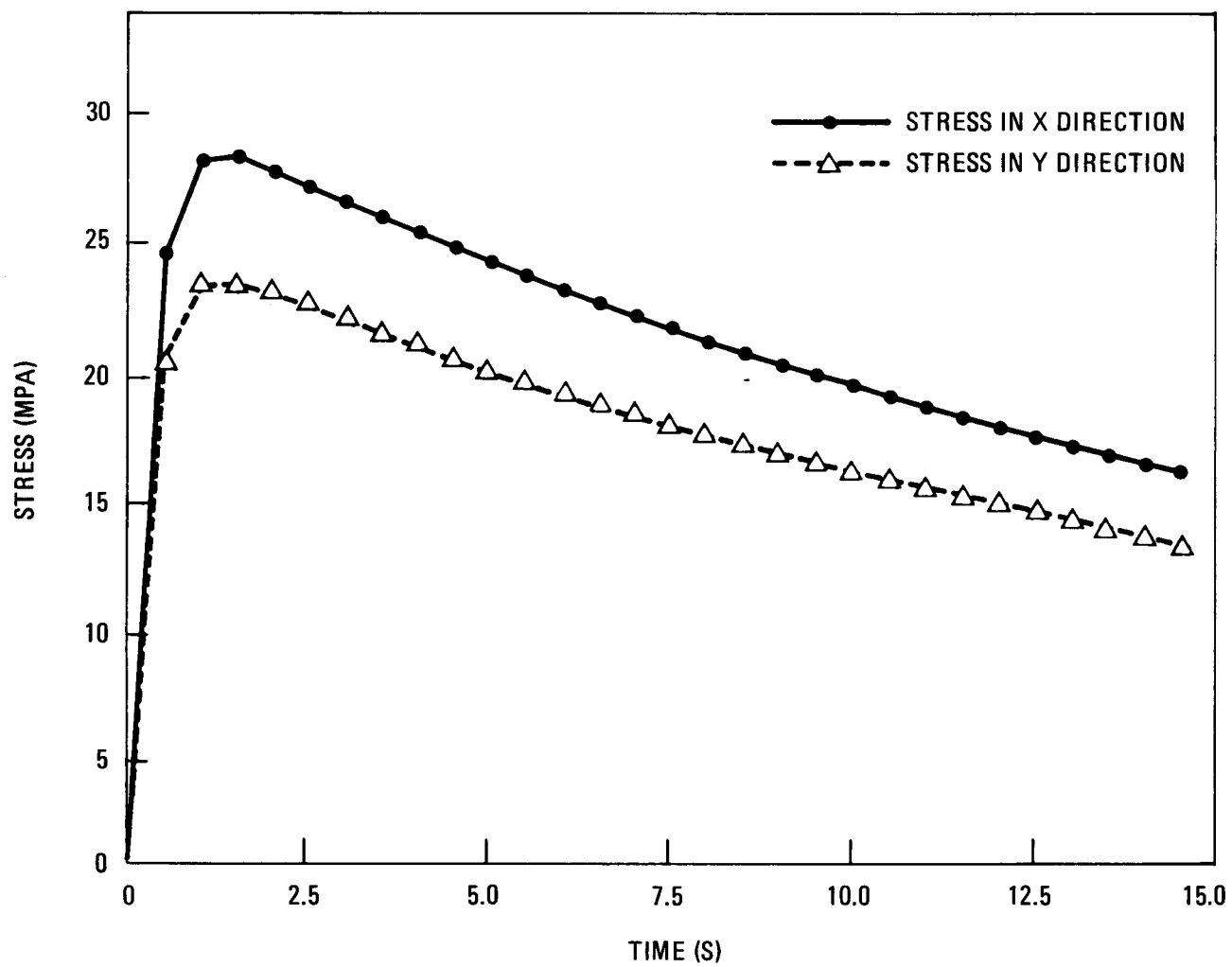


Fig. 2-27. Maximum in-plane stresses during thermal shock

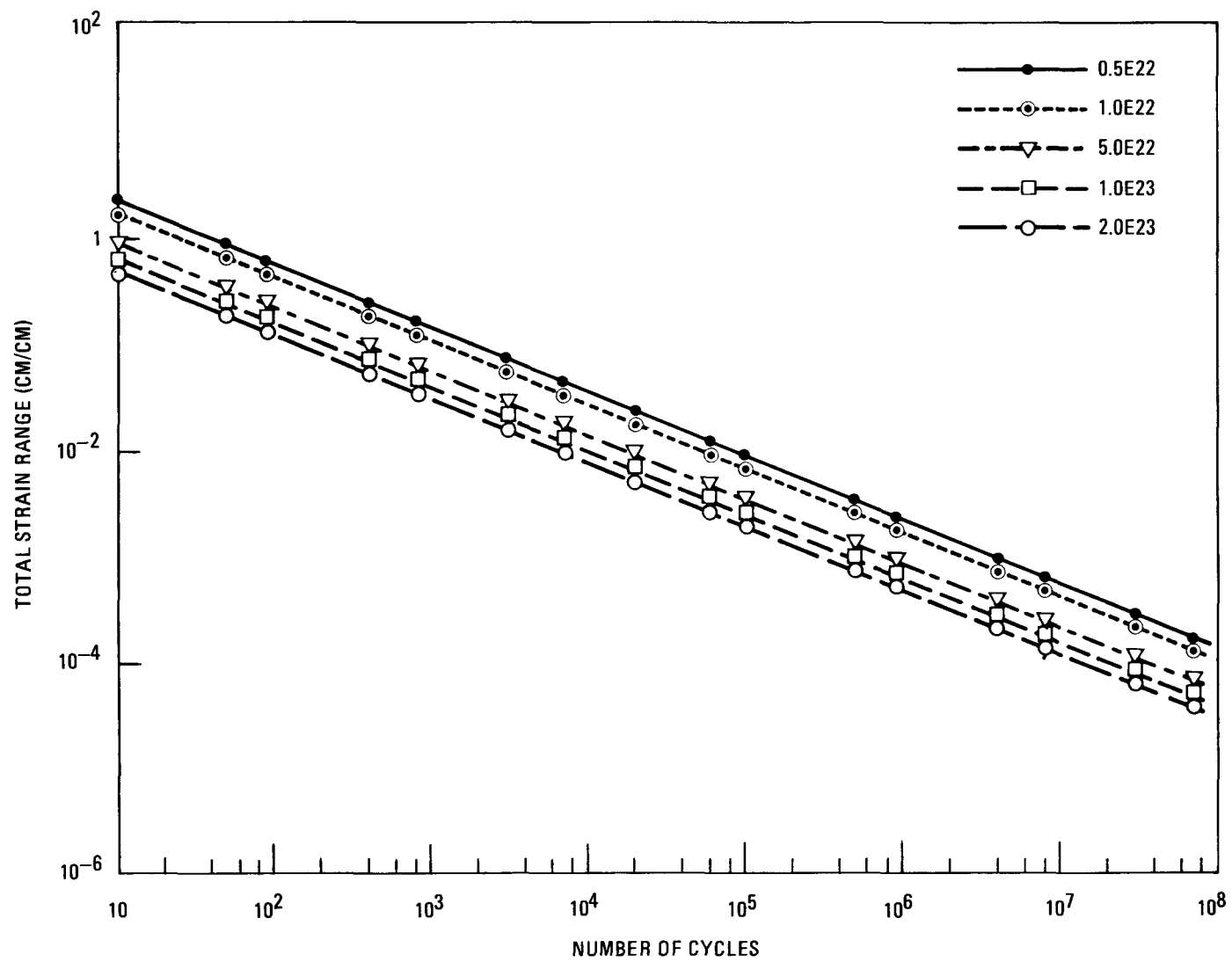


Fig. 2-28. Fatigue life for irradiated 20% cold-worked 316 stainless steel ($T = 538^{\circ}\text{C}$)

2.2.3. Interassembly Gap for Hex and Round 300-MW(e) GCFR Fuel Assemblies

During the previous quarter, the criteria for selecting the gap between fuel assembly ducts were reviewed, and a criterion of no "two-sided" duct to duct contact resulting from duct dilation was proposed. The values of the interassembly gap required to satisfy this criterion were developed for hex ducts of various wall thicknesses and for circular ducts. An extension of the current static adhesion tests was proposed to establish the adequacy and need for the proposed criterion. The conclusions of this work are as follows:

1. At the present time, the interduct gap should be sized to prevent two-side duct to duct contact resulting from duct dilation.
2. The minimum interassembly gap that could be used if any duct contact were neglected would be dictated by refueling considerations. Values less than about 5 mm are probably impractical.
3. The proposed criterion can be met by the current 316 stainless steel hex duct assembly using various combinations of duct wall thickness and interduct spacing. For a 4-mm wall, a 10-mm gap will satisfy the criterion with some margin for uncertainties.
4. For circular assembly designs, the interassembly gap is dictated by irradiation swelling of the duct, which is independent of thickness. Duct wall thickness is, however, constrained by handling considerations, with a minimum practical value of about 2 mm. For a circular assembly design equivalent to the current reference hex assembly design, an interassembly gap of 7.5 mm would be required to satisfy the proposed criterion.
5. To ascertain whether the proposed criterion is acceptable or necessary, it is recommended that the GCFR core assembly seal static adhesion test be extended to evaluate the consequences (from a materials standpoint) of interduct contact.

2.2.3.1. Interassembly Gap for Hex and Round Assemblies. The selection of an interassembly gap for the GCFR must be based on several considerations, including, but not limited to, the following:

1. Duct dilation.
2. Duct bowing.
3. Material behavior and consequences of interduct contact.
4. Fuel handling and management.
5. Reactor performance.
6. Reactor safety (duct melting and fallaway).

At the present time, item 3, material consequences of contact between adjacent fuel assemblies, is a source of major uncertainty. Unfortunately, it is also probably the most important consideration when selecting a criterion for setting the interassembly gap. Current liquid metal fast breeder reactor (LMFBR) and GCFR design criteria specify that there should be no contact whatsoever between ducts, reflecting the concern that when two ducts come in contact at these high temperatures and fluences, detrimental material behavior such as galling and self-welding may occur. These criteria appear to have little quantitative justification for either the LMFBR or the GCFR. In fact, in the LMFBR case, the criteria can be called into question by the many years of successful operation of EBR-II, where duct-to-duct contact occurs routinely throughout the core. Nevertheless, these criteria persist. Unfortunately, although it was possible to design to meet these criteria several years ago, with today's understanding of the creep and swelling behavior of 316 stainless steel, such design becomes a difficult, if not impossible, task. In the case of the GCFR, interassembly gaps of 50 mm or more would be required to meet such criteria. Although such a design might be feasible, it surely would not have the performance characteristics of a plant designed to less restrictive criteria.

Interduct contact can be classified into two distinct types, each of which can be expected to have somewhat different consequences. The first type, which is called single-point contact, results from tolerance

stack-ups and irradiation bowing of the assembly. Loads due to this kind of contact are limited by the bending flexibility of the duct and are a concern primarily from a refueling standpoint. The second type of interduct contact, termed two-sided contact, results from duct dilation (especially the swelling components). In this case, the free expansion of the duct is constrained by its neighbors, which could possibly result in the highly undesirable situation of a cluster of assemblies jamming tightly together.

It is proposed that the duct to duct contact criterion allow one-sided contact such as that which results from assembly bowing. For current materials, prevention of one-sided contact would require unrealistic values of the interassembly gap, and the effects of such contact are believed to be relatively benign. However, two-sided contact, such as that arising from duct swelling, should continue to be prohibited. Although significant material problems are not expected, the propensity for problems appears to be much greater in this case, and, since no contraindicating experimental evidence exists for helium-cooled fast reactor environments, it appears prudent to assume, at least until such time as appropriate testing can be done, that such contact would be detrimental.

2.2.3.2. Required Interduct Gap With Proposed Criterion. Interassembly gaps required to prevent two-sided duct contact were determined for hex assemblies employing various duct wall thicknesses and for equivalent circular assemblies. The current fuel management scheme specification that no two adjacent fuel assemblies be of the same age was taken into account, and it was found that this meant that the gap needed to be 50% greater than the peak radial dilation in the older assembly rather than 100% greater, as would be the case if the assemblies were the same age. The results for the hex duct are given in Fig. 2-29, which shows the required interassembly gap plotted as a function of duct wall thickness. It can be seen that there is a greatly diminished return for increased duct wall thickness for walls greater than about 4 mm thick. This is because duct wall thickness limits only the creep portion of the duct dilation, which for a 4-mm duct is less than one-half of the swelling component. In fact, as

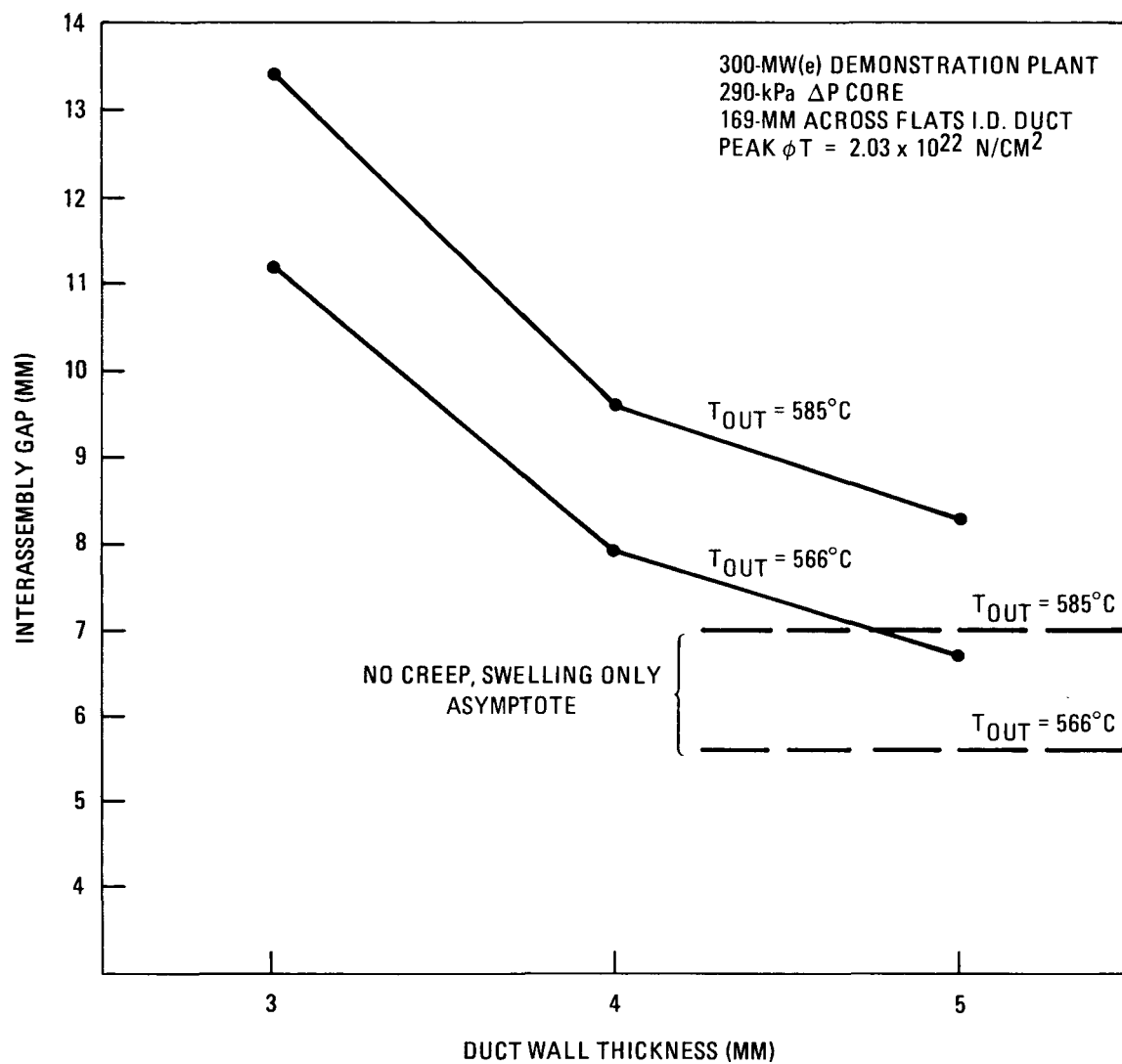


Fig. 2-29. BOL interassembly gap required to prevent duct to duct contact for various duct wall thicknesses

shown in Fig. 2-29, even for a hex duct design which has no creep component whatsoever, swelling alone dictates a 7-mm interassembly gap. This latter result is applicable to circular assemblies. For a circular assembly equivalent to the reference design hex assembly, a 7.3-mm gap would be required. Although this value is independent of duct wall thickness, handling considerations indicate a minimum practical wall thickness of about 2 mm. Without a duct contact criterion, the minimum interassembly gap which could be used with either hex or round ducts would primarily be dictated by refueling operations. Gaps of less than about 5 mm are probably impractical.

2.2.3.3. Suggested Verification Test. To reduce the uncertainty of whether duct contact in the GCFR is acceptable from a materials standpoint, it is recommended that appropriate verification tests be conducted. These tests could be simple extensions of ongoing core assembly seal static adhesion tests. Although irradiation effects would not be simulated, it is anticipated that the data generated would nevertheless be very useful in establishing a rational criterion for the required interduct gap.

2.2.4. Nonproliferation Core Study

A study has been performed to determine the required interassembly gap for a large assembly, advanced material nonproliferation design. The free parameter was the wall thickness, and input data of Table 2-5 determined the required gap dimension. The model used was the built-in, viscoelastic, cantilever beam model used successfully in previous dilation studies. The material model, however, was unique. In an effort to approximate the characteristics of an advanced alloy which might be available from the ongoing material development program, a modified version of the 20% cold-worked 316 stainless steel correlation was used. The N-lot irradiation creep and swelling correlation (longest incubation and lowest swelling) was reduced by 30%; this equation effectively formed a lower bound on all the existing data points for 20% cold-worked 316 stainless steel. The results are presented in Fig. 2-30 and show the minimum required interassembly gap necessary to prevent EOL duct contact as a function of duct wall thickness.

TABLE 2-5
INPUT PARAMETERS FOR
NONPROLIFERATION CORE STUDY

Assembly flat to flat i.d. (cm)	20.78
Assembly pressure drop (MPa)	0.290
Fast flux (n/cm ² -s)	2.71×10^{15}
EOL fast fluence (n/cm ²)	1.95×10^{23}
Outlet temperature (°C)	585
Inlet temperature (°C)	321

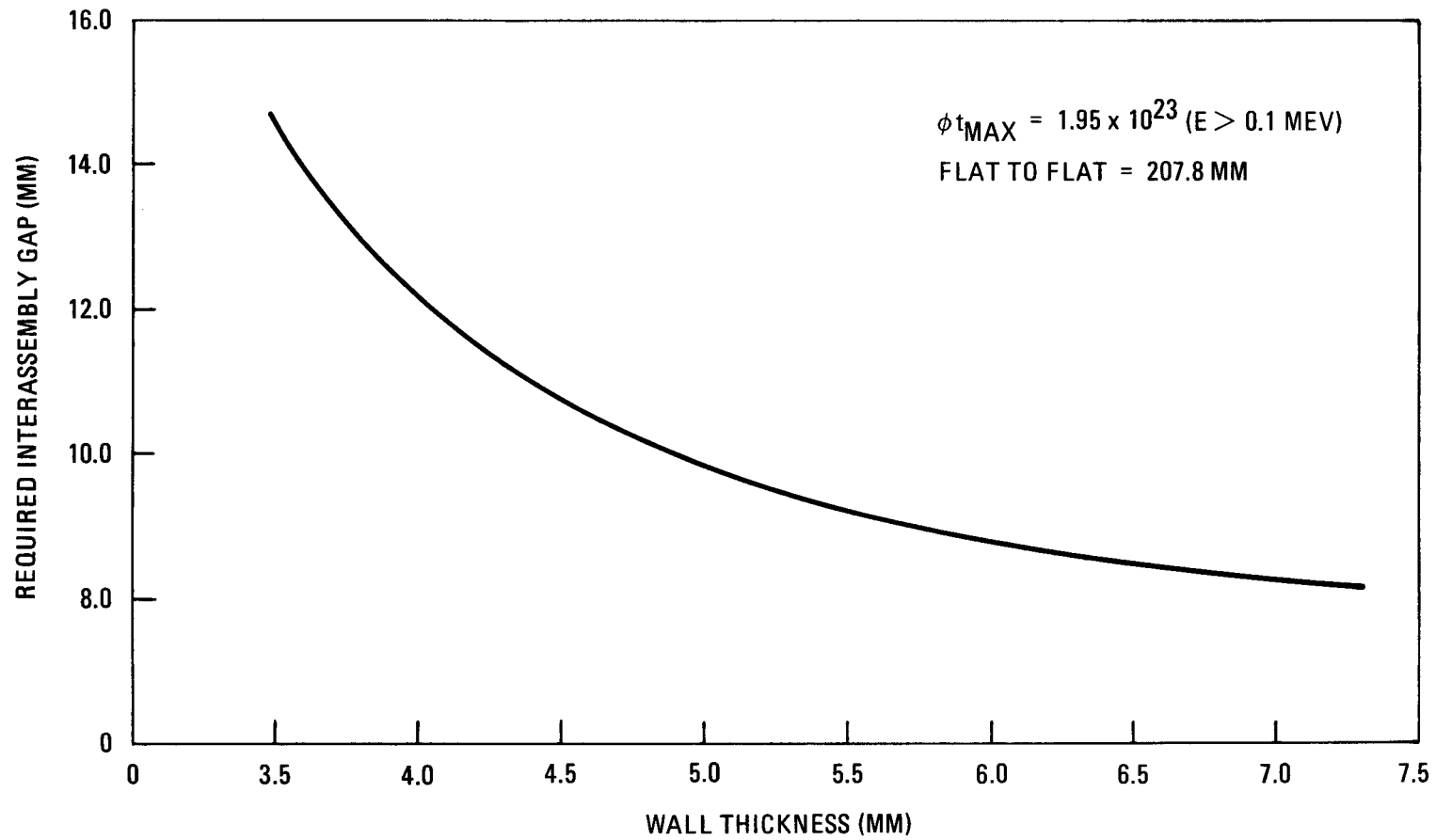


Fig. 2-30. Required interassembly gap for proliferation-resistant core

2.3. CORE ASSEMBLY STRUCTURAL DESIGN CRITERIA

The analytical methods of the structural design criterion have been reviewed. Although fatigue curves will be issued at a later date, they have been constructed using the methods described in the criterion for interim in-house use. In addition, they have been applied to core outlet thermal shock due to reactor trips (Section 2.2).

2.4. CORE ASSEMBLY MECHANICAL TESTING

The objective of this task is to conduct mechanical tests of core assembly components and subassemblies to simulate the mechanical loads anticipated during normal and abnormal reactor operating conditions. The current phase of this program involves testing of fuel assembly components.

Rod spacer interaction tests were emphasized during this quarter. The current phase of interaction tests have been designed to evaluate the effect of angular misalignment of spacer cells during axial rod movement. It has been postulated that deflection of core components due to assembly tolerances, rod bowing, and swelling could cause angular misalignment on the order of a few degrees rotation. The hardware for these tests has been fabricated and is now being assembled, and instrumentation is concurrently being installed in the test rig.

2.5. HEAT TRANSFER AND FLUID FLOW TESTING

There was no activity on this subtask during this quarter. It is anticipated that flow testing of the revised fuel assembly inlet section model will be initiated during the next quarter.

REFERENCES

- 2-1. Wheeler, C. L., et al., "COBRA-IV-1: An Interim Version of COBRA for Thermal-Hydraulic Analysis of Rod Bundle Nuclear Fuel Elements and Cores," Battelle Northwest Laboratory Report BNWL-1962, March 1976.

- 2-2. Rowe, R. S., and C. W. Angle, "Cross Flow Mixing Between Parallel Flow Channels During Boiling, COBRA-Computer Program for Coolant Boiling in Rod Arrays," Battelle Northwest Laboratories Report BNWL-371, 1967.
- 2-3. Baxi, C. B., "Reference Thermal Hydraulic Correlations for Turbulent Flow Analysis of GCFR Fuel Assembly," General Atomic Company, unpublished data.
- 2-4. Hudina, M., et al., "Benchmark Calculations III, Task Definition," Swiss Federal Institute for Reactor Research Report AN-IN-684, November 1977.
- 2-5. Barroyer, P., et al., "Benchmark Calculations III, Summary of Results," Swiss Federal Institute for Reactor Research Report AN-IN-720, March 1978.
- 2-6. "Gas-Cooled Fast Breeder Reactor Quarterly Progress Report for the Period February 1, 1978 through April 30, 1978, "DOE Report GA-A14928, General Atomic Company, May 1978.
- 2-7. Lee, G. E., "Edge Rod Bowing in GCFR Fuel Assemblies," General Atomic Company unpublished data.
- 2-8. Lee, G. E., "Rod Bowing in GCFR Fuel Assemblies: Evaluation of Effects of Duct Dilation," General Atomic Company, unpublished data.
- 2-9. Rowe, D. S., "COBRA-IIIC: A Digital Computer Program for Steady State and Transient Thermal-Hydraulic Analysis of Rod Bundle Nuclear Fuel Elements," Battelle Northwest Laboratory Report BNWL-1695, March 1973.
- 2-10. Rao, S. B., "Temperature Gradients in the Edge Rod Due to Duct Dilation," General Atomic Company, unpublished data.
- 2-11. Rao, S. B., "Spacer Design to Reduce Edge Rod Bowing," General Atomic Company, unpublished data.
- 2-12. Veca, A. R., "Core Assembly Design for a 42 psi ΔP Core (127 Core Assembly Design)," General Atomic Company, unpublished data.
- 2-13. Heisser, D. J., "Rod-Spacer-Duct-Interaction Due to Volume Swelling," General Atomic Company, unpublished data.
- 2-14. Rodgers, A. F., "Effect of Tolerances on the Edge Channel Gap of Hex and Round GCFR Fuel Assemblies," General Atomic Company, unpublished data.

- 2-15. Schlueter, G., "Thermal-Hydraulic Limits to Fuel Rod Bowing," General Atomic Company, unpublished data.
- 2-16. "Structural Design Guideline for FBR Components," V. I, RDT Standard F9.7, General Electric Company, unpublished data.

3. PRESSURE EQUALIZATION SYSTEM FOR FUEL (189a. No. 00582)

3.1. CORE ASSEMBLY AND PES SEALS

A third set of static adhesion tests was started to evaluate several candidate material pairs for adhesion under GCFR core oxidation conditions. The results of the tests will indicate which material pairs are suitable for use in the fuel assembly to grid plate interface. This test is being run under the following simulated reactor coolant conditions (as were the previous tests):

Temperature = $350^{\circ} \pm 10^{\circ}\text{C}$,

Atmosphere = helium gas with hydrogen and oxygen impurities

The materials being tested in the current phase are listed in Table 3-1. The samples will remain in the atmosphere-controlled furnace for 3000 hr. At the end of this time period, the samples will be examined for oxidation and photographed, and the force required to separate them will be measured. Scheduled testing of PES vent port-seal tests have been postponed owing to impending design changes.

3.2. ANALYSIS, MODELS, AND CODE DEVELOPMENT

The development of the transient PES flow code continued during this quarter. Programming of the input section of the code and processing of input data for computation were completed for problems without mass or heat sources, and a previously developed flow resistance subroutine (including choking effects) was included in the computation section. These modifications are being evaluated with a test problem, and debugging is in progress. Programming of the output section and inclusion of source terms are also in progress.

TABLE 3-1
TEST ASSEMBLY ARRANGEMENT

Rod No.	Load (Newtons)	Assembly	Grid	No. of Couples	Serial Number
1	1334	316 stainless steel	Cr_2O_3 (LC-4)	2	1 through 4
1	1334	Stellite 6B	Cr_2O_3 (LC-4)	2	49 through 52
1	1334	316 stainless steel	Cr_3C_2 (LC-1H)	2	9 through 12
1	1334	Stellite 6B	Cr_3C_2 (LC-1H)	2	57 through 60
2	1334	316 stainless steel	Al_2O_3 (SAO)	2	17 through 20
2	1334	Stellite 6B	Al_2O_3 (SAO)	2	65 through 68
2	1334	316 stainless steel	Stellite 6 (SST)	2	25 through 28
2	1334	Stellite 6B	Stellite 6 (SST)	2	73 through 76
3	1334	316 stainless steel	Stellite 6B	2	33 through 36
3	1334	Stellite 6B	Stellite 6B	2	81 through 84
3	1334	316 stainless steel	316 stainless steel	2	41 through 44
3	1334	Stellite 6B	316 stainless steel	2	89 through 92
4	1779	316 stainless steel	Cr_2O_3 (LC-4)	2	5 through 8
4	1779	Stellite 6B	Cr_2O_3 (LC-1H)	2	53 through 56
4	1779	316 stainless steel	Cr_3C_2 (LC-1H)	2	14 through 17
4	1779	Stellite 6B	Cr_3C_2 (LC-1H)	2	61 through 64
5	1779	316 stainless steel	Al_2O_3 (SAO)	2	21 through 24
5	1779	Stellite 6B	Al_2O_3 (SAO)	2	69 through 72
5	1779	316 stainless steel	Stellite 6 (SST)	2	29 through 32

TABLE 3-1 (Continued)

Rod No.	Load (Newtons)	Assembly	Grid	No. of Couples	Serial Number
5	1779	Stellite 6B	Stellite 6 (SST)	2	77 through 80
6	1779	316 stainless steel	Stellite 6B	2	37 through 40
6	1779	Stellite 6B	Stellite 6B	2	85 through 88
6	1779	316 stainless steel	316 stainless steel	2	45 through 48
6	1779	Stellite 6B	316 stainless steel	2	93 through 96

3.3. PLATEOUT AND PLUGGING

Volatile fission products, particularly cesium and iodine, vented from the core assemblies and also produced by gaseous precursor decay of fission products vented from the core assemblies may plate out on the walls of the monitor lines. These fission products are swept through the monitor lines into the helium purification system (HPS) traps by helium entering at the core subassembly vent connections. Accumulation of deposited material may constrict the sweep gas flow passages and could potentially lead to plugging of the lines. The conditions under which plateout or plugging could occur in the GCFR, the means of minimizing or eliminating these conditions, and the methods for removing deposits, should they form, are being investigated. A small high-pressure loop has been built and is being used for this purpose. Components for injection, control, and measurement of impurities (i.e., H_2 and H_2O) and sources for simulating venting of volatile fission products and their compounds have been developed.

Several runs were carried out using the improved cesium source described previously (Ref. 3-1), i.e., an evacuated sealed glass ampoule containing elemental cesium within a stainless steel tube that can be compressed to break the ampoule and introduce cesium into the flowing helium of the loop. The initial run used a source positioned below the flow of the loop such that the cesium laid in a pool at the bottom of the stainless steel finger. After a one-week run and subsequent analysis of plateout in the loop, virtually all cesium was found to be in the source region, again indicating lack of transport. Apparently because of the large size of the cesium atom and the high helium pressure existing in the loop, it is necessary to position the liquid cesium directly in the main helium flow of the loop to induce significant downstream transport. Two attempts to introduce cesium vapor by cracking a vial positioned directly in line with the main stream flow failed owing to blockage of the flow by (1) the unbroken ampoule and (2) a combination of glass fragments and adsorbed liquid cesium.

A recent design change allowed liquid cesium to drop into a slight recess in the main loop tubing while the ampoule and glass fragments were isolated by a screen above the main loop flow. In the two instances in which this method was used, a plug resulted within ~15 min from the time the vial cracked. Apparently the liquid cesium was transported downstream by flowing helium or quickly wetted the walls of the source region of the loop, resulting in a nonrepresentative plug in either case. In view of the difficulty encountered using a liquid cesium source, the possibility of using cesium sorbed on graphite or cesium produced by thermal decomposition (CsNO_3) is being studied.

3.4. FISSION PRODUCT RELEASE AND TRANSPORT

The purpose of this subtask is to obtain experimental data on the interdiffusion and gas phase and the surface back diffusion of gaseous and volatile fission products. The diffusion coefficient data will be used to validate or improve the SLIDER code (Ref. 3-2), a one-dimensional model for fission gas diffusion transport (including radioactive decay). Surface transport and back diffusion data will be used to establish a model for predicting the importance of these mechanisms to contamination of the reactor coolant system.

Tests to measure the diffusion coefficient of krypton in the blanket region of a GCFR fuel rod were continued with six open-tube runs. These were carried out to firm up the data base gathered during earlier open-tube runs, and the results are currently being used to provide predictions of fission gas release from the completed HELM-2 test. The next series of diffusion experiments will utilize depleted UO_2 pellets in the test section, providing a diametral gap of 250 μm .

3.5. MONITOR STATION AND INSTRUMENTATION

Research and development is being conducted to establish design criteria and to acquire design information which will permit the design of monitor stations for the proposed GCFR 300-MW(e) plant and subsequent commercial GCFR power plants. Design studies conducted in FY 77 indicate that the complexity of the monitor stations can be greatly reduced and the design and equipment greatly simplified if it is possible to use a small gamma photon detector with a large dynamic range ($>10^5$). Such a detector must also have the capability to resolve photopeaks in a manner comparable to NaI(Tl) in the photon energy range between 80 and 500 keV without the need for cryogenic cooling. During the previous quarter the design and construction of a CdTe(Cl) radiation detector assembly for the helium loop, Mol (HELM) monitor station were completed and calibration and testing begun.

The HELM monitor station will be used on the vent line of HELM, where a GCFR vented and pressure-equalized, 12-rod fuel assembly is being irradiated in the cadmium-filtered flux of the BR-2 reactor. Complimentary and more prototypical data will be acquired to augment data previously obtained using a Ge(Li) monitor station on the sweep gas line of irradiation capsule GB-10 at the Oak Ridge research reactor (ORR).

During this quarter the HELM radiation detector was calibrated without the collimator using Ba-133 and Se-75 solutions in an 8-mm o.d. x 6-mm i.d. stainless steel tube. A Cs-137 point source was used for system alignment. The detector is unsatisfactory because of the Compton scattering component and inadequate resolution. Further work will be deferred until the start of FY 79, since HELM-3, which started its irradiation in April 1978, is planned to go to high burnup over a period of years. Consultation with the detector vendor is in progress to determine if redesign of the detector to optimize resolution in the photoenergy region of interest will be adequate.

REFERENCES

- 3-1. "Gas-Cooled Fast Breeder Reactor Quarterly Progress Report for the Period February 1, 1978 Through April 30, 1978," DOE Report GA-A14928, General Atomic Company, May 1978.
- 3-2. Jadhov, K. B., and B. W. Roos, "SLIDER, A FORTRAN-V Program for the Computation of Release of Fission Products From One-Dimensional Multi-Layered Fuel Configurations," USAEC Report GA-8566, Gulf General Atomic Company, August 1969.

4. CORE ASSEMBLY DESIGN VERIFICATION (189a No. 00582)

Core assembly design verification consists of planning and executing the principal GCFR test programs to demonstrate satisfactory performance of the GCFR core assemblies. Model assemblies will be tested over a range of simulated GCFR environments, and the resultant performance data will be used to establish the GCFR design data base, to verify design analyses and predictive design computer codes, and to explore for design faults. The goals of these activities are to provide assurance of design performance and to generate the information necessary to support licensing. The current task scope includes

1. Core flow test loop (CFTL).
2. Prototype assembly tests.
3. Helium flow tests.
4. Depressurized accident condition tests.

The principal activities for this quarter were as follows:

1. Completion of amendment 1 (Ref. 4-1) to the CFTL program plan (Ref. 4-2).
2. Revised selection of a temperature instrumentation location scheme for the CFTL 37-rod fuel simulator bundle.
3. Comparison of thermal-hydraulic analyses (two codes) on CFTL 37-rod fuel simulator test bundles.
4. Thermal-hydraulic and duct bowing analyses on a 61-rod blanket simulator test bundle.

5. Completion of the Resource Evaluation and Control System (RECS) input for the prototype test.
6. Evaluation of vendor price quotations and technical specifications for a helium compressor/driver assembly for the helium flow test rig.
7. Development of requirements for depressurized accident condition tests (DACT).

4.1. CORE FLOW TEST LOOP PROGRAM

A series of out-of-pile simulation tests will be performed to (1) demonstrate the ability of the GCFR fuel, control, and blanket assembly designs to meet design goals and (2) verify predictions of analytical models which describe design operation and accident behavior. The emphasis of the tests will be on obtaining thermal-structural data for steady-state, transient, and marginal conditions using electrically heated rod bundles in a dynamic helium loop. Final margin tests will be progressively extended to the highest possible temperature until the heater elements fail. The CFTL program plan (Ref. 4-2) describes the requirements for the test program to be conducted in the CFTL, which will be constructed and operated by Oak Ridge National Laboratory (ORNL).

4.1.1. Program Planning

4.1.1.1. Program Plan. An amendment (Ref. 4-1) to the CFTL program plan (Ref. 4-2) was prepared to document changes that have occurred since its initial issue. When the GCFR core design definition is complete, the operating and transient test conditions will be revised.

4.1.1.2. Quality Assurance Program. Quality assurance planning between GA and ORNL has resulted in the agreement that all GCFR-related programs will be conducted in accordance with applicable sections of ANSI N45.2. Compliance with this standard will assure that CFTL data are acceptable for support of GCFR licensing.

4.1.2. Test Analysis and Prediction

4.1.2.1. Test Predictions and Instrumentation. During the previous quarter a temperature-pressure instrumentation location scheme for the 37-rod bundle was described (Ref. 4-3). It was based on the use of four thermocouples per rod, eight axial thermocouple locations, four different rod types, and two thermocouples for each axial location. Two types of thermocouple location schemes, shown in Fig. 4-1, were described: scheme A has the two allotted thermocouples at a given axial position on one rod, thereby permitting a direct measure of temperature differential across a rod; scheme B locates the two allotted thermocouples at a given axial position on two rods, which has the advantage of greater commonality of thermocouple locations among the rods and hence requires fewer spare rods for the same coverage. Scheme B was recommended for use in the CFTL based primarily on the latter consideration.

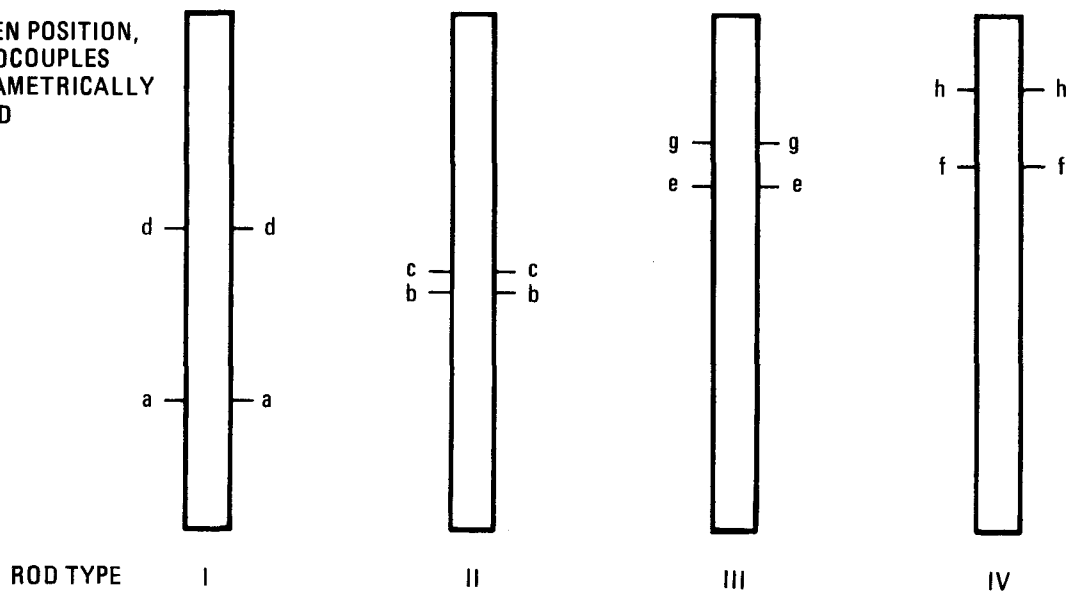
However, reconsideration of the two schemes, with emphasis on obtaining the most accurate measurement of diametral temperature difference on a rod, for structural and bowing analysis and COBRA code (Ref. 4-4) verification led to the decision to use scheme A instead of B.

There is no difference between the two schemes with respect to the number of spare rods needed if one spare rod of each type (four rods) is required. If two spare rods of each type are necessary, then scheme A would require eight spare rods whereas scheme B, which has some degree of commonality among the rods, might require seven spare rods. Thus, for a small increase in the cost of spare rods, it becomes possible to gain the considerable advantage of direct measurement of diametral temperature difference on a rod (scheme A). Figure 4-2 shows the thermocouple arrangement for scheme A using the same location for the rods (types I, II, III, IV) as used for scheme B. Table 4-1 identifies the axial thermocouple locations in the edge channels and those adjacent to a hanger rod sleeve; it can be seen that the distribution of the locations is quite good.

NOTE:

AT GIVEN POSITION,
THERMOCOUPLES
ARE DIAMETRICALLY
OPPOSED

SCHEME A



SCHEME B

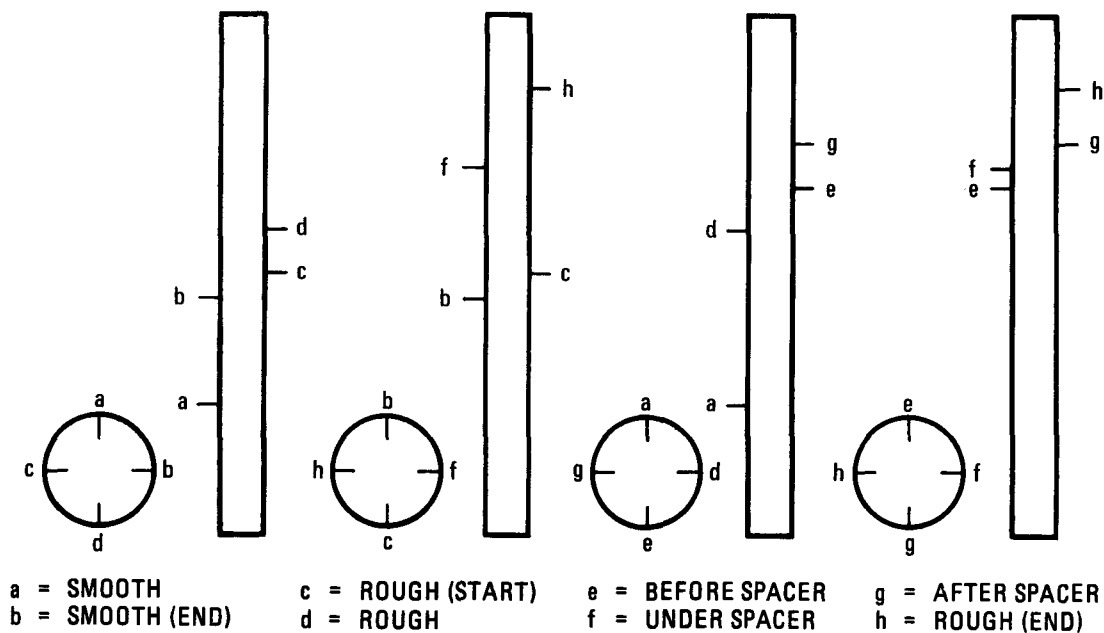


Fig. 4-1. Rod thermocouple location schemes (each letter denotes a thermocouple)

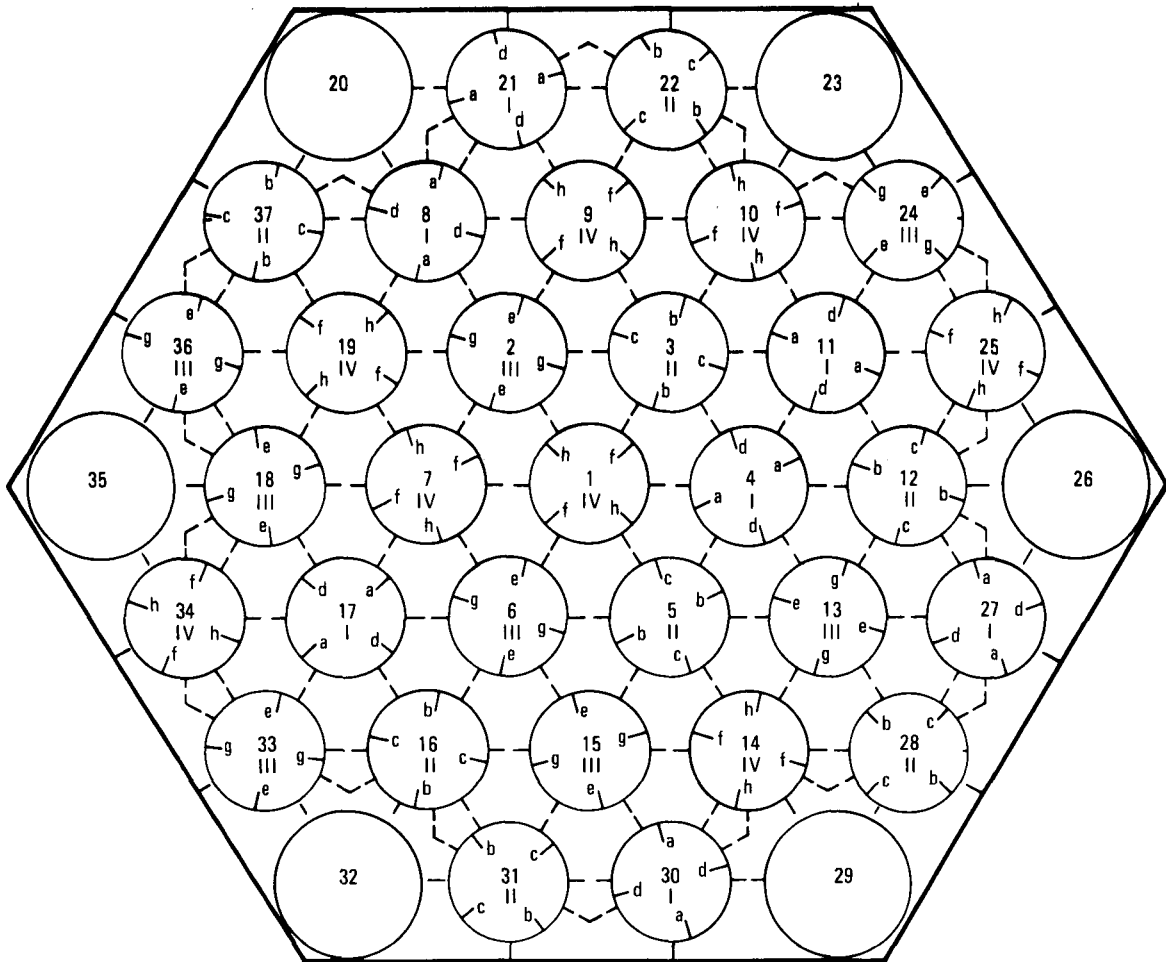


Fig. 4-2. CFTL thermocouple location diagram, scheme A

TABLE 4-1
37-ROD CFTL CHANNEL/THERMOCOUPLE AXIAL
LOCATION ARRANGEMENT FOR SCHEME A

Channel	Thermocouple Axial Location
5, 11	a, 2b, c, d, e, 2g
12, 15	a, 2b, 2c, 2d, 2e, f, g, h
13	a, 2b, c, f, g, h
14	a, c, d, f, g

4.1.2.2. Application of COBRA Code. Comparison of the 37-rod bundle thermal-hydraulic results obtained with COBRA-SIDE 2 (a modification of COBRA III) and the latest COBRA-IV code (Ref. 4-5) indicates significant differences. For the design using a shaped hanger rod sleeve and two types of spacers, COBRA-IV shows much larger diametral temperature differences in the smooth region and generally higher cladding temperatures in the bundle than the previous code. This results from the use of lower heat transfer coefficients in the smooth and rough regions and different spacer loss coefficient correlations in the latest code (Ref. 4-6). As a result, the 37-rod bundle mechanical design is more complex to limit the diametral temperature difference across an edge/hanger-rod-sleeve rod and an interior rod adjacent to a hanger rod sleeve.

Figure 4-3 shows the channel layout for a one-sixth section of a CFTL 37-rod bundle used in COBRA analysis. Note that for an edge/hanger-rod-sleeve rod (No. 8) it is possible to have five diametral temperature differences: in channels 8-12, 9-12, 9-13, 10-13, and 11-14. Also, for an interior rod (No. 6) adjacent to a hanger rod sleeve, several diametral temperature differences are possible; that existing in channel 2-11 was the most severe. Thus, the objective of the thermal-hydraulic design was to limit the diametral temperature difference on rods 6 and 8 to about 14°C.

Although a number of designs were investigated, the smallest rod temperature difference resulted from a design which incorporated different features for the smooth and rough rod regions. This design comprises (1) two types of hanger rod sleeves (circular for the smooth region, shaped for the rough region) and (2) two types of spacers in which the blockage for all interior channels is greater (increased strut thickness or extra local material) in the smooth region than in the rough region, the blockage in channel 13 is much higher in the rough region compared with the smooth region, and the blockage in channels 5 and 11 is less in the smooth region than the rough region. Figure 4-4 shows the cladding temperature in the

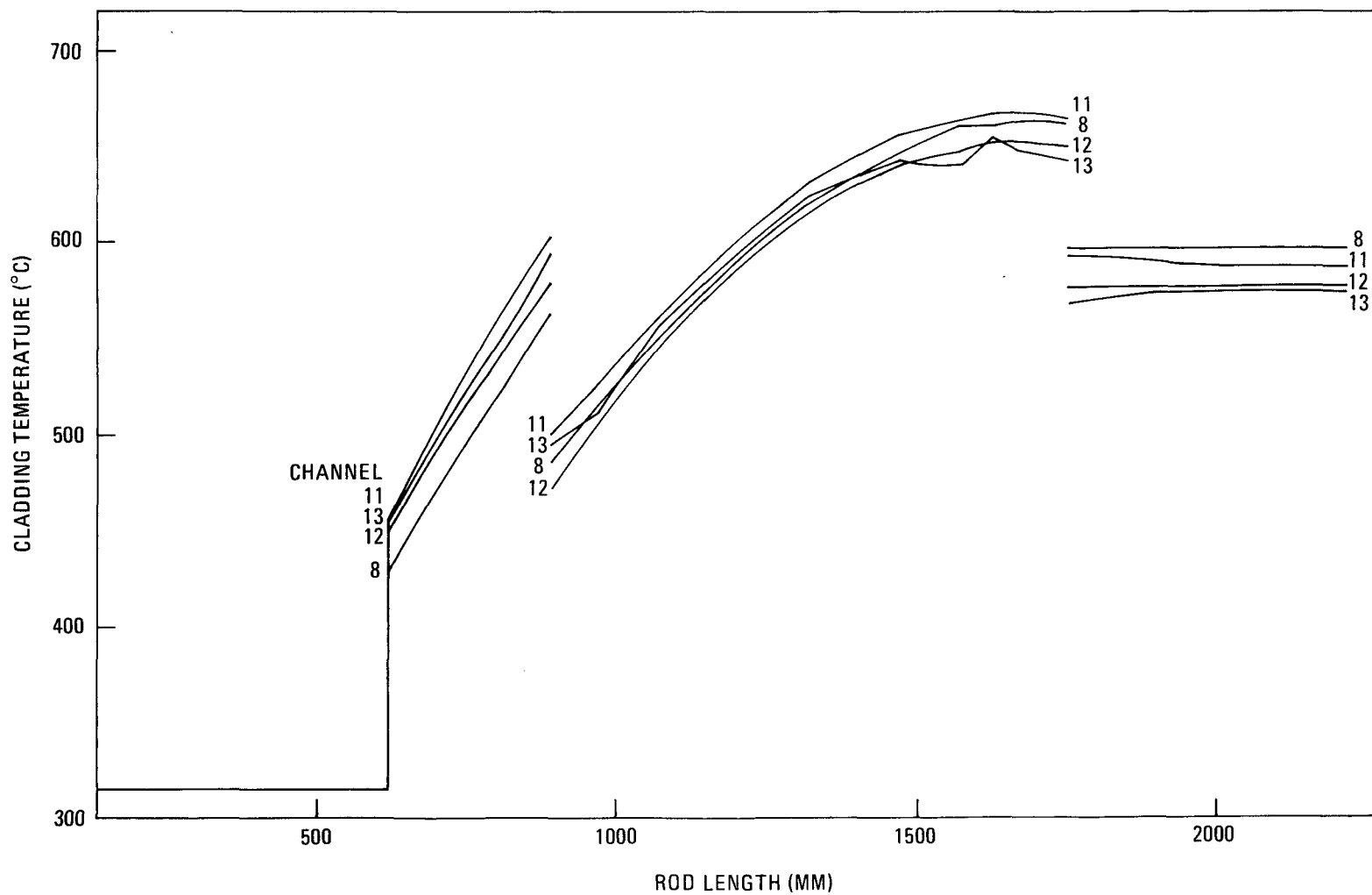


Fig. 4-4. Cladding temperature vs rod length (rod 8) for CFTL 37-rod bundle [2 spacer types, 2 hanger rod types, increased interior channel blockage (smooth), COBRA-IV]

various channels as a function of rod length for the design described above. The maximum diametral temperature differences and their locations are

<u>Channels</u>	<u>Temperature Difference (°C)</u>	<u>Location</u>
Rod 8		
8-2	17-23	Smooth region
9-12	17-23	Smooth region
9-13	31	Smooth region
10-13	26	Smooth region
11-14	42	Smooth region
Rod 6		
2-11	41	Smooth region

In contrast to previous designs, which showed a change of direction of the diametral temperature difference (channel 8-12) between the end of the smooth section and the end of the rough section, the present design indicates that this reversal occurs over a very short distance, i.e., between the end of the smooth section and the beginning of the rough section. The maximum temperature drops which occur in the smooth region can be further reduced by increasing the spacer blockage in the interior channels. Although this design illustrates the methods which may cause the rod temperature drop to approach the desired value, the practicality of such a complex design must be further examined.

4.1.2.3. Application and Modification of the TSPEC Code. The TSPEC code (Ref. 4-7), which provides a simplified analysis of thermal-hydraulic characteristics and duct bowing due to differential thermal expansion, was used to predict duct bowing in CFTL fuel and blanket rod assemblies. Since the code presently treats only fuel assemblies, approximations were required to analyze the blanket assemblies. Duct bowing ranged from 11.9 to 17.1 mm for fuel ducts and from 15.7 to 16.5 mm for blanket ducts. Since the most severe steady-state skewed power conditions were used, this preliminary analysis resulted in the dimensions of the maximum envelope for CFTL vessel design purposes.

The TSPEC code was modified to provide thermal-hydraulic and duct bowing analyses for blanket rod assemblies. The principal differences between the fuel and blanket rod assemblies are that the former consists of smooth and roughened rod sections and utilizes grid type spacers, whereas the latter is composed of smooth rods with wire-wrapped spacers. Furthermore, a CFTL fuel assembly has six unheated rods, and a blanket assembly has only one unheated rod. To account for these differences, the friction factor and Stanton number for wire-wrapped rod bundles were incorporated into the code, and the current blanket heat flux distribution, which differs from that of the fuel in having a higher peak-to-average heat flux ratio and an essentially complete rather than chopped cosine flux distribution, was closely approximated. The results from this modified version of the code have been compared with previous results from TSPEC in which the blanket assembly heat flux distribution was approximated by using that of the fuel assembly. The modified code version shows about 2 mm greater bowing and about 70°C reduction in maximum cladding temperature.

4.1.2.4. Heat Loss Criterion. The generic criterion for CFTL hex duct heat loss is that to the extent practical, the heat loss shall simulate GCFR hex duct heat loss such that there is adequate structural-thermal-flow modeling to accomplish program objectives. Although heat loss information will be developed during analytical activities leading to design definition of the GCFR core, conservative estimates for a past GCFR design and design basis depressurization accident (DBDA) condition indicate less than 0.05% and 0.5% heat loss for the 100% and 10% power levels, respectively. Future requirements may dictate a need for duct heating and/or insulation, but based on current information, GA has recommended that the reference bundle design not include duct insulation nor a provision for duct heating.

4.1.3. Test Bundle Design

The preliminary ORNL sketches of inlet areas for the fuel, control, and blanket rod simulator test bundles were reviewed. These included alternate methods for sealing between bundle and attemperation flows, and

all concepts appeared to be feasible. However, two of the three concepts appeared to be better suited to physical modeling of the GCFR down-flow core concept. Radial flow of coolant into the test bundle was shown in all cases (Fig. 4-5). Although DOE has requested that ORNL investigate separating the tube sheet from the pressure boundary, because this may simplify the design and analysis, require only one pressure head, and result in axial flow through the tube sheet into the test bundle, GA has accepted the radial inlet flow concept.

4.2. GCFR PROTOTYPE ASSEMBLY TEST PROGRAM

Program planning for testing of the prototype core assemblies is continuing. The tests will be conducted on full-size core assemblies to ensure that they meet design qualification requirements prior to fabrication of the demonstration plant initial core. The prototype assemblies will be the same as the GCFR demonstration plant core assemblies except that the $\text{PuO}_2\text{-UO}_2$ fuel in the GCFR fuel rods will be simulated by depleted UO_2 . The assemblies will be subjected to maximum GCFR helium flow conditions to closely simulate the reactor core environment; however, there will be no radiation. One assembly of each type (fuel, control, and blanket) will be subjected to the equivalent of approximately one year each of reactor operation in a hot helium test loop. The helium test loop temperature will be maintained external to the test section, since fuel rod heating will not be simulated in these tests.

The test facility and operation are planned as part of the German contribution to the GCFR program. Several other options are also being considered, including (1) a new facility which would most likely be situated in Germany; (2) the CARMEN-2 loop at Saclay, France; and (3) upgrading of the proposed GCFR core assembly helium flow test rig. The Commissariat à l'Energie Atomique (CEA) has indicated that with minimal loop changes, the CARMEN-2 loop can provide a helium flow of 8 kg/s with a test assembly Δp of 290 kPa at 75 bars and 550°C. With additional changes, particularly upgrading of the recuperator/reheater and test vessel, the loop would meet the desired prototype test condition requirements of 8 kg/s with a Δp of 290 kPa at 90 bars and 450°C.

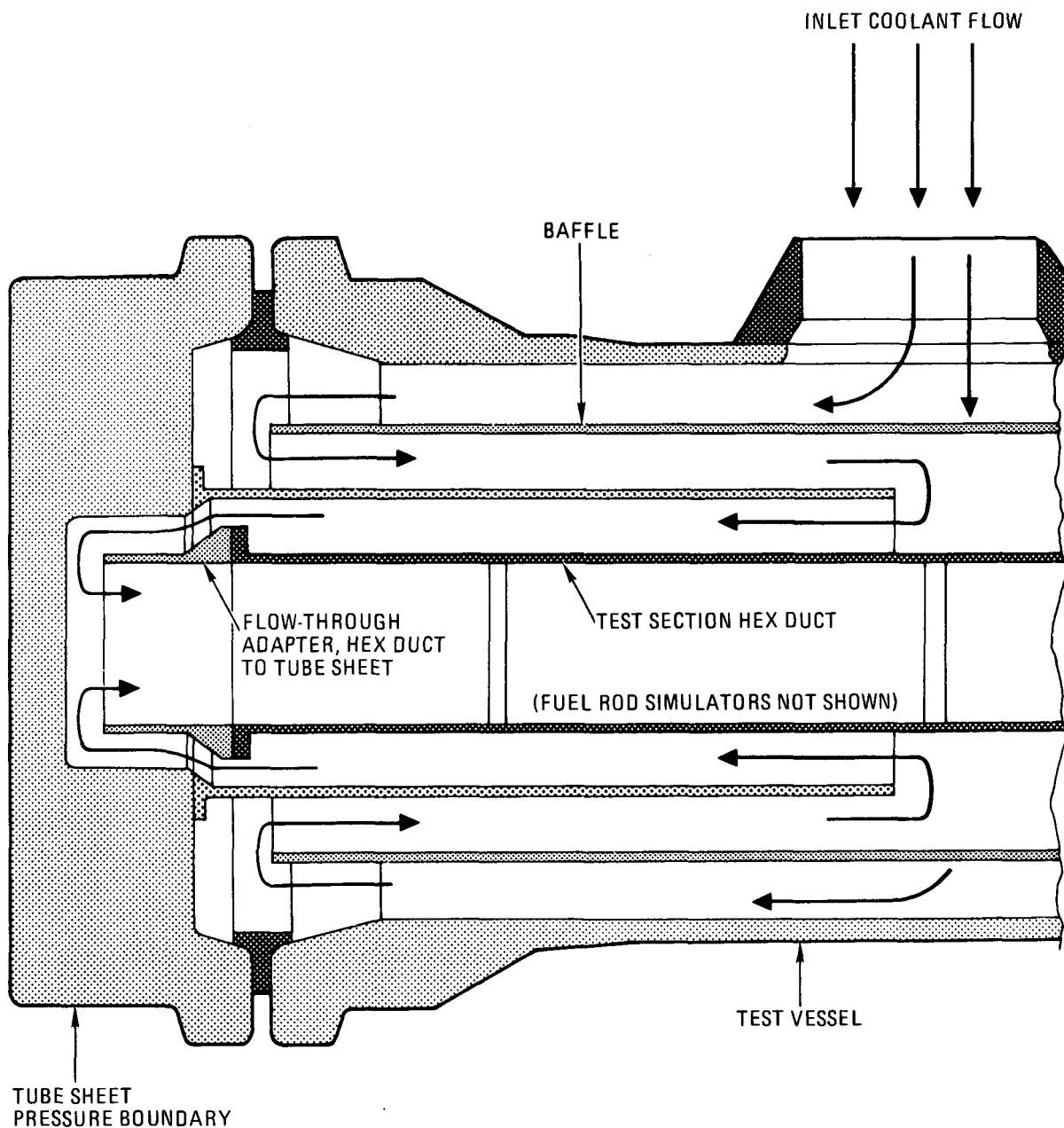


Fig. 4-5. Simplified sketch of ORNL proposed coolant inlet area design for CFTL fuel rod simulator test bundle

A feasibility study on upgrading the proposed GCFR core assembly helium flow test rig to permit prototype testing has been initiated. The proposed helium flow test rig (which will be located in the U.S.) will be capable of relatively low pressure and temperature test conditions and will be used for design evaluation testing of full-size core assemblies. The design evaluation work will include vibration, acoustic, and pressure drop testing. Upgrading will include raising the system pressure from 6 to 9 MPa and the test assembly temperature from 175° to 450° or 550°C. This may be accomplished by using the same circulator used for the helium flow tests, but a helium heater and a regenerator/recuperator would have to be added to the system to attain the desired prototype assembly test conditions.

Germany has agreed to complete an engineering design assessment of the prototype facility by the end of 1978 so that the funding and schedule requirements necessary for the program can be established. Implementation requirements will be defined by the Fuels, Materials, and Core Components Technical Coordinating Committee under the umbrella agreement, and a test site will be selected. The prototype test schedule is being interfaced with other GCFR program schedules to assure its compatibility with the overall GCFR schedule. The prototype RECS test has been completed, and the summary task definition has been issued.

4.3. HELIUM FLOW TEST

Helium flow testing is planned for the design evaluation of full-size core assemblies; Ref. 4-8 describes the need for conducting flow tests on the GCFR core assemblies. The objectives of the tests are to determine local and overall pressure drops and local flow distributions and to explore for acoustic excitation phenomena and flow-induced vibration characteristics. The results of these tests will be used in establishing satisfactory flow performance of the core assemblies. Therefore, it is required that these tests be accomplished during the preliminary design phase of the core assembly to provide input for completion of the preliminary core design effort. Figure 4-6 is a simplified flow diagram of the test assembly and includes the major test parameters.

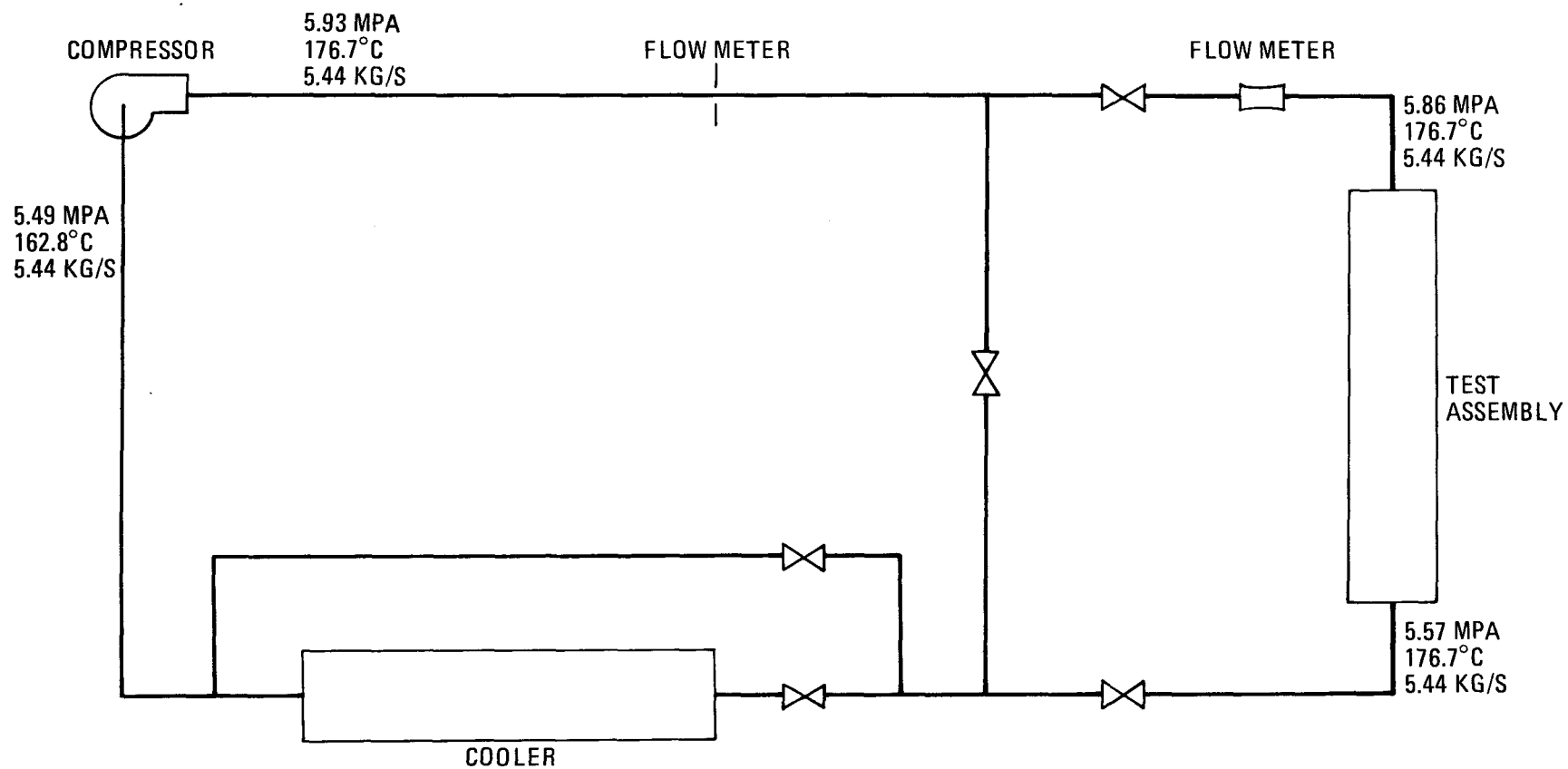


Fig. 4-6. Helium flow rig

The specification for the helium compressor/driver assembly for the helium flow test rig was prepared and was sent out with a request for proposal to compressor manufacturers. The responses are being evaluated. One manufacturer suggested that the helium circulator fabricated for the EBOR facility at Idaho Nuclear Engineering Laboratory (INEL) very nearly matches the circulator described in the request for proposal and that refurbishment of this circulator appears to be the most economical way to obtain a circulator system for the helium flow test rig. (The EBOR circulator was recently removed from the EBOR facility and placed in storage.) The manufacturer of the EBOR circulator has indicated that refurbishment of the EBOR circulator is feasible.

Conducting the GCFR core assembly tests in the GCFR helium circulator facility was evaluated, and it was concluded that this is not feasible for several reasons: (1) there is a 4-yr schedule difference; (2) operating costs will be much higher; program conflict problems, although not defined as yet, will be great; and (3) a full-core assembly pressure drop using only approximately 3% of the GCFR circulator output is not practical.

4.4. DEPRESSURIZED ACCIDENT CONDITION TEST

The depressurized accident condition test (DACT) will provide core cooling safety margin information under low-pressure, pseudo-steady-state DBDA conditions. The test is designed to provide experimental information on potential GCFR core assembly edge channel problems and flow and temperature distributions within the bundles under depressurized conditions. In addition, DACT will be capable of investigating natural convection effects within the core assemblies. Since recently developed experimental equipment for the duct melting fallaway test (DMFT) can be adapted for DACT, it is proposed that DACT be conducted at Los Alamos Scientific Laboratory (LASL), which is including the DACT requirements in the design of the DMFT test vessel. DACT will be run in FY 79 and 80 and thus will be about 4 yr ahead of the CFTL DBDA tests and will provide the near-term information required for licensing reviews.

The requirements for the DACT tests are being developed. Since LASL is in the process of designing the test fixtures that may be used for both the DMFT and DACT, the emphasis has been to develop the information required by LASL to incorporate the DACT requirements into the test fixture design. The two DMFT requirements which affect the LASL fixture design are instrumentation and bundle requalification. It appears that DACT will require about twice the amount of instrumentation as the DMFT. Bundle requalification may be achieved either by establishing a certain moderate temperature test condition data point and then periodically returning to this test condition data point during testing to ensure that the bundle and/or the bundle instrumentation are still within specified test limits or by disassembling the test fixture to inspect and requalify the bundle.

REFERENCES

- 4-1. Hopkins, H. C., Jr., "Amendment I: Program Plan for the GCFR Core Flow Test Loop," General Atomic Company, unpublished data.
- 4-2. Hopkins, H. C., Jr., "Program Plan for GCFR Core Flow Test Loop," USAEC Report GA-A13080, General Atomic Company, August 9, 1974.
- 4-3. Gas-Cooled Fast Breeder Reactor Quarterly Progress Report for the Period November 1, 1977 Through January 31, 1978, DOE Report GA-A14771, General Atomic Company, February 1978.
- 4-4. Rowe, D. S., "COBRA IIIC: A Digital Computer Program for Steady State and Transient Thermal-Hydraulic Analysis of Rod Bundle Nuclear Fuel Elements," Battelle Northwest Laboratory Report BNWL-1695, March 1973.
- 4-5. Wheeler, C.L., et al., "COBRA-IV-I: An Interim Version of COBRA for Thermal-Hydraulic Analysis of Rod Bundle Nuclear Fuel Elements and Cores," Battelle Northwest Laboratory Report BNWL-1962, March 1976.
- 4-6. Schlueter, G., "Third GCFR Heat Transfer Benchmark Meeting, EIR, March 13-14, 1978," General Atomic Company, unpublished data.

- 4-7. Hopkins, H. C., Jr., "TSPEC - A Computer Program to Predict Approximate Model Performance in the Core Flow Test Loop," ERDA Report GA-A14057, General Atomic Company, November 1976.
- 4-8. "Core-Element Development Program Plan for the Gas-Cooled Fast Breeder Reactor Demonstration Plant," ERDA Report GA-A13312, General Atomic Company, May 6, 1975.

5. FUELS AND MATERIAL ENGINEERING (189a No. 00582)

5.1. OXIDE FUEL AND BLANKET TECHNOLOGY

The purposes of this subtask are to (1) maintain liaison with and surveillance of other DOE and non-DOE programs, especially the LMFBR program, to enable the GCFR program to utilize information on fuel and blanket materials from these programs for the GCFR design, (2) to participate in the Fast Breeder Reactor Fuel Element Development Working Group and its subsidiary task groups, and (3) to guide the laboratory and irradiation test programs for the GCFR.

During this quarter, GCFR program representatives reviewed the status of the entire fuel assembly development effort. The most recent fuel-cladding chemical interaction (FCCI) correlation for i.d. cladding wastage developed by Hanford Engineering Development Laboratory (HEDL) was examined, and demonstration that the oxygen to metal (O/M) correlation is temperature dependent was discussed. It appears that this dependence is accounted for in the cross-product of the O/M and temperature terms of the correlation equation. The methods used to calculate burnup in EBR-II irradiations and the thermophysical properties of mixed oxide fuel are being studied.

5.2. CLADDING TECHNOLOGY

5.2.1. ANL Mechanical Testing Program

The objective of the ANL mechanical testing program is to determine the effects of the following factors on the behavior and properties of

GCFR ribbed and smooth cladding:

1. Ribs, rib geometry, fabrication technique, and stress state.
2. Helium impurity levels typical of the environment expected in the GCFR demonstration plant.

These tests are biaxial creep rupture tests with a hoop to axial tensile stress ratio of 2. Tests at a hoop to axial tensile stress ratio of unity and pure tensile tests are planned in support of the irradiated cladding test program. Two tests at 650°C and a hoop stress of ~ 238 MPa in a purified helium atmosphere (i.e., O_2 partial pressure of $<10^{-1}$ Pa) have been completed at ANL. In general, ribs increase rupture life and do not affect strain at failure.

The final results of the third biaxial creep rupture tests at ANL (ANL III) have been received. These tests were performed in a helium environment containing 300 Pa of H_2 and 30 Pa of H_2O at 650°C. Various hoop stress levels were used, but all at a hoop to axial tensile stress ratio of 2. The types of cladding used in this test were (1) as-received smooth (ASRD), (2) mechanically ground smooth (MGS), (3) mechanically ground ribbed (MGR), (4) electrochemically etched ribbed (ECER), and (5) electrochemically ground ribbed (ECGR). Tables 5-1 and 5-2 present the results for average rupture life, average strain at failure, and strain at the failure site. Creep rupture life versus midwall hoop stress for various types of specimens is shown in Fig. 5-1. The test results have been analyzed by ANL, and the following observations have been made:

1. Mechanically ground smooth and as-received specimens have comparable rupture lives at comparable stress levels; i.e., surface finish does not appear to affect creep rupture behavior.

TABLE 5-1
COMPLETE RUPTURE TIME RESULTS FROM THE
ANL-III BIAXIAL CREEP RUPTURE TEST

Midwall Hoop Stress Level (MPa)	Heat No.	Rupture Time ^(b) (hr)					
		ASRD	MGS	MGR	ECER	ECGR	WMC
234.4	90216	106.1 (235.4)	98.9 (233)	271.8 (236.5)	128.2 (236.5)	219.1 (236.5)	187.3 (232.4)
262	90216	45.42 (265)	40.3 (261.9)	134.3 (265)	47.3 (265)	85.2 (265)	79.5 (260.6)
282.7	90216			62.6 (283.4)	17.95 (281.3)	35.7 (284)	
317	90216			20.5 (319)	9.45 (316.5)	13.0 (319.2)	
193	90216	277.32 (196.5)					
220.6	90216	190.3 (220.6)					
255	88229		203.4 (254.4)				
269	88229		24.0 (271)				

(a) ASRD = as-drawn, as-received; MGS = mechanically ground smooth; MGR = mechanically ground ribbed from Kraftwerk Union (KWU), Germany; ECER = electrochemically etched ribbed (from GA); ECGR = electrochemically ground ribbed from Swiss Federal Institute for Reactor Research (EIR); WMC = mechanically ground ribbed from WMC Corporation.

(b) Numbers in parentheses indicate root stress in MPa.

TABLE 5-2
STRAIN AT FAILURE OF VARIOUS TYPES
OF SPECIMENS USED IN ANL-III CREEP-RUPTURE TEST^(a)

Type of Cladding ^(b)	Hoop Stress Level (MPa)	Average Strain at Failure ^(c) (%)	Strain at Failure Site ^(d) (%)	Typical Failure Mode ^(e)
ASRD	193	3.8	6.3	2
	221	4.8	>10	2
	234	4.6	9.7	2
	262	4.8	>10	2
MGS	234	4.5	6.1	1
	262	4.4	>10	2
MGS ^(f)	248	3.3	>10	2
	269	3.5	>10	2
MGR	234	2.7	5.1	1
	262	3.7	5.6	2
	276	3.5	--	2
	310	3.4	5.6	2
ECER	234	3.03	3.8	1
	262	2.6	3.1	1
	283	5.1	>10	2
	317	5.3	>10	2
ECGR	234	3.4	5.0	1
	262	3.2	6.6	2
	283	3.6	6.2	2
	317	3.6	>10	2
WMC	234	3.4	4.6	1
	262	4.3	6.3	1

(a) All specimens from heat 90216 except as noted.

(b) See footnote (a), Table 5-1.

(c) Average of all specimens (15 measurements per specimen).

(d) Average of all specimens (one measurement per specimen at failure location).

(e) 1 = small intergranular cracks; 2 = large ductile rupture.

(f) Heat 88229.

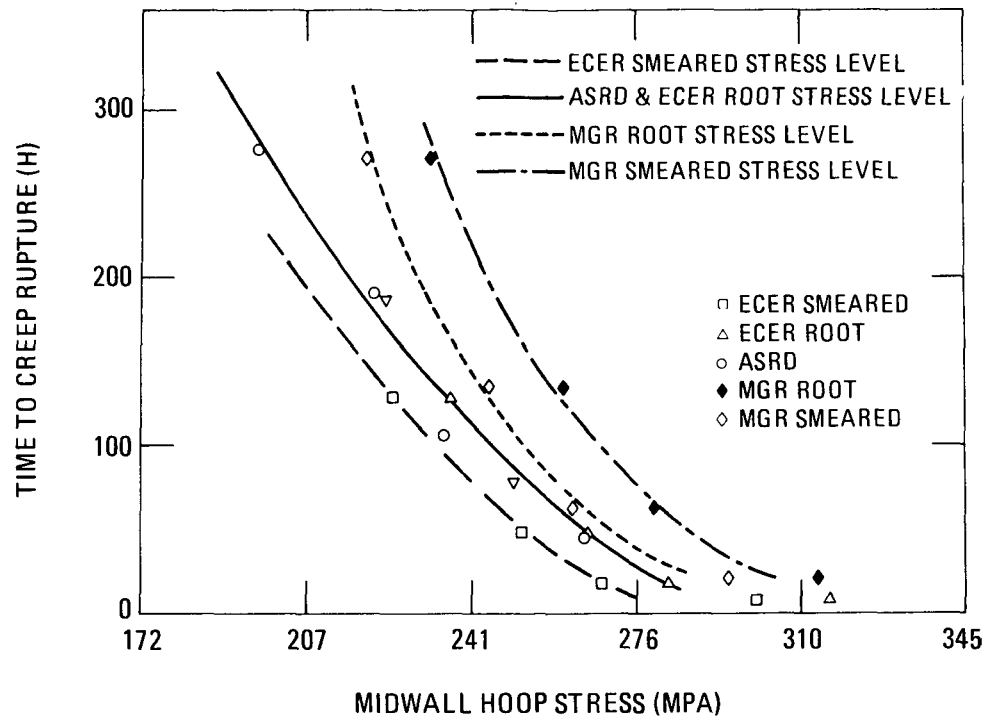


Fig. 5-1. Creep rupture test results

2. A comparison of ribbed specimens with smooth specimens of the same minimum wall thickness, where stress is computed on the basis of the root diameter (minimum wall thickness), indicates that the effect of adding the ribs is equivalent to an apparent reduction of the midwall hoop stress of the mechanically ground ribbed specimens by about 10%. In one of the models considered, the rib volume was uniformly smeared over the tube surface to obtain an apparent thickness. The smeared density model can only account for about a 5% strengthening effect; but the actual strengthening appears to be twice as much.
3. The smeared density model is appropriate for the electro-chemically ground ribbed specimens.
4. The etched ribbed specimens had the shortest rupture lives, and it appears that the strengthening effect of the ribs was negated by the weakening effect of the fabrication technique.

A test matrix for a creep rupture test (ANL-IV) has been designed and transmitted to ANL for comments. The design is based on the following specific objectives.

1. To determine the effect of heat of material on rupture life.
2. To determine the effect of environment.

Concern with separation of the effects of environment and material heat arose from the results of the third test at ANL (ANL-III), which was conducted in a typical GCFR atmosphere. The results of ANL-III indicate that the ribs have a strengthening effect, but the rupture lives were low compared with data from Ref. 5-1. The rupture lives can vary by a factor of ~ 5 between different heats of material, and thus the low rupture lives could be specific to this particular heat of material. However, it must be established that the low rupture lives are not due to the GCFR environment.

The fourth test matrix is specifically designed to determine the effect of heat of material. Two different heats will be tested: 90216 and 90400. The cladding from these heats has slightly different dimensions, but the difference can easily be accommodated by adjusting the internal pressure. This test will be conducted in a potential GCFR environment with an H_2/H_2O ratio of 100/400 Pa, which is more oxidizing than the environment used for test ANL-III ($H_2/H_2O = 10$). Direct comparison of the two heats of cladding will indicate the effect of heat of material in a GCFR environment.

To determine the effect of the environment, it is essential to compare the rupture lives in the GCFR environment with those in the environment of the baseline data (static helium). It was originally thought that baseline data could be obtained using a purified helium atmosphere. The second test (ANL-II), was performed in this fashion, but another heat of material was used, and hence this test cannot be directly compared with test ANL-IV. Another possibility would be to use the HEDL testing technique, in which a static helium atmosphere is employed. The available oxidants in the static helium are rapidly exhausted by oxidation, and the test is actually conducted in very pure helium from then on. Comparison of static helium test results with those of test ANL-IV should then yield valid conclusions on the effect of environment.

In addition to the above tests, some specimens will be tested at a more prototypical stress state for the GCFR, i.e., a hoop to axial tensile stress ratio of unity. The material for this test has been shipped to ANL, and sample fabrication is being initiated.

5.2.2. Helium Loop Test Program

The primary objective of the Pacific Northwest Laboratory (PNL) helium loop test program is to compare the mechanical properties in recirculating helium determined at PNL with those in quasistatic helium determined at ANL. The loop was modified for unattended operation, and the first test was initiated. Significant problems in measuring and controlling impurity

levels caused this test to be terminated at the end of 100 hr. Additional equipment was purchased and installed to rectify these problems.

A new gas chromatograph was calibrated, and an attempt was made to operate the loop. This attempt was partially successful; after one day of operation the loop circulator drive system failed. The circulator drive was repaired, and the loop was operated for about 4 days, at which time the drive system once again failed. During this time, unusual and unexplainable instrument observations were made. Unless the loop circulator drive system is satisfactorily repaired, it may be impossible to continue the test program as planned. An evaluation is under way to determine a future course of action.

5.3. F-1 (X094) FAST FLUX IRRADIATION EXPERIMENT

The encapsulated fuel rods in the F-1 (X094B) experiment received burnup exposures to ~ 13.0 at. % (121 MWd/kg, 8×10^{26} n/m² total fluence) and 6.1×10^{26} n/m² ($E > 0.1$ Mev). Postirradiation examination of the seven fuel rods removed at the termination of the experiment is continuing at ANL, and examination of special components (dosimetry and charcoal traps) continues at GA.

Initial measurements on charcoal from the fission product traps of F-1 fuel rods irradiated to 71 MWd/kg indicate Cs-134 loadings in the range 80 to 600 ppm and Cs-137 loadings in the range 10 to 1000 ppm. The highest cesium loadings are associated with rods G-11 and G-13. Based on these initial measurements (which require confirmation), it appears that the cesium content of the charcoal traps will be 10 to 50 times higher than the loadings in the rods removed at 25 and 51 MWd/kg. Cesium transport to charcoal appears to have been particularly enhanced in rod G-11, which had an O/M ratio of 1.947. Enhancement of cesium release from the fuel for low-O/M rods was expected.

Cesium loading and distribution in the G-13 rod trap, which is the high-temperature lead rod, are as high as those in the G-11 trap. Since the O/M ratio is 1.973, the enhanced cesium transport in this rod is attributed to the higher rod temperature. The irradiation parameters for the F-1 rods are given in Table 5-3. The unexpectedly high cesium loadings in charcoal traps from the F-1 rods coupled with the high trap loading in vented rod experiment GB-10 (Ref. 5-2) makes (the proposed) elimination of charcoal traps from the individual rods in the GCFR highly unlikely. This statement must be qualified with the caveat that both the F-1 design (a sealed rod from which fission gases, and therefore daughter cesium, cannot escape) and the GB-10 design (a vented rod with a sweep flow) are not prototypical of the GCFR vented rod. A final determination on retention of charcoal traps in the rods will be made after the results of the prototypical HELM experiments are received.

Fission gas was extracted from fuel rods G-4 and G-13 by drilling the plenum region and quantitatively transferring the gas to an evacuated pressure/volume measuring and sampling apparatus. Samples of these gases were isotopically analyzed using a mass spectrometer, and the amount of fission gas collected was compared with the amount of xenon and krypton generated in fission. These data, obtained at ANL-E, are shown in Table 5-3 and in Fig. 5-2. Comparable data for previously analyzed fuel rods G-1, G-2, and G-6 are shown in Table 5-4.

All the rods containing active charcoal traps, i.e., charcoal in communication with volatile fission products, characteristically exhibit fission gas recovery values in the range 64% to 87%. In contrast, quantitative recovery (100%) was observed for previously analyzed fuel rods G-1 and G-2 containing sealed charcoal traps, i.e., charcoal not in communication with the fuel or fission products. These fuel rods had experienced an irradiation exposure to 56 MWd/kg. However, rod G-8, which also contained a sealed charcoal trap and was irradiated to a burnup of 96 MWd/kg, had a fission gas recovery of 66%. This anomaly is not understood because it is not clear which factors (i.e., fuel temperature, power history, porosity, fission density) control fission gas release. Additional detailed postirradiation examination is required to determine the fission inventory and distribution.

TABLE 5-3
RESULTS OF GAS SAMPLING AND ANALYSIS

	G-4	G-8	G-9	G-10	G-11	G-12	G-13
Fission gas volume (cm ³)	264	176	13	160	174	157	175
% fission gas released to plenum ^(a)	87	66	64	75	80	76	79
Peak burnup [MWd/kg (at. %)]	125 (13.4)	100 (10.8)	75 (8.0)	75 (8.0)	75 (8.0)	75 (8.0)	75 (8.0)
Fuel O/M	1.983	1.985	1.968	1.968	1.947	1.976	1.973
Cladding i.d. maximum temperature (°C)	680	672	702	712	712	690	770
Calculated centerline fuel temperature ^(b) (°C)	1926	1926	2090	2150	2150	2150	2284

(a) Percent release data are based on code calculated burnup and fission product inventory values and are subject to revision following experimental determination. G-8 contains a sealed, i.e., inactive trap; the remaining fuel rods contain active charcoal traps which may effectively sorb a significant volume of fission gas at standard temperature and pressure.

(b) The centerline fuel temperatures are based on current LIFE-III code calculations (Ref. 5-3). These values are related to axial rod section 3, i.e., the quadrant adjacent to and above the reactor midplane, and represent average maximum temperature at the beginning of irradiation. These values decline by 20% to 30% of the initial calculated temperature with time because of reduction of power with burnup and closure of the fuel to cladding gap.

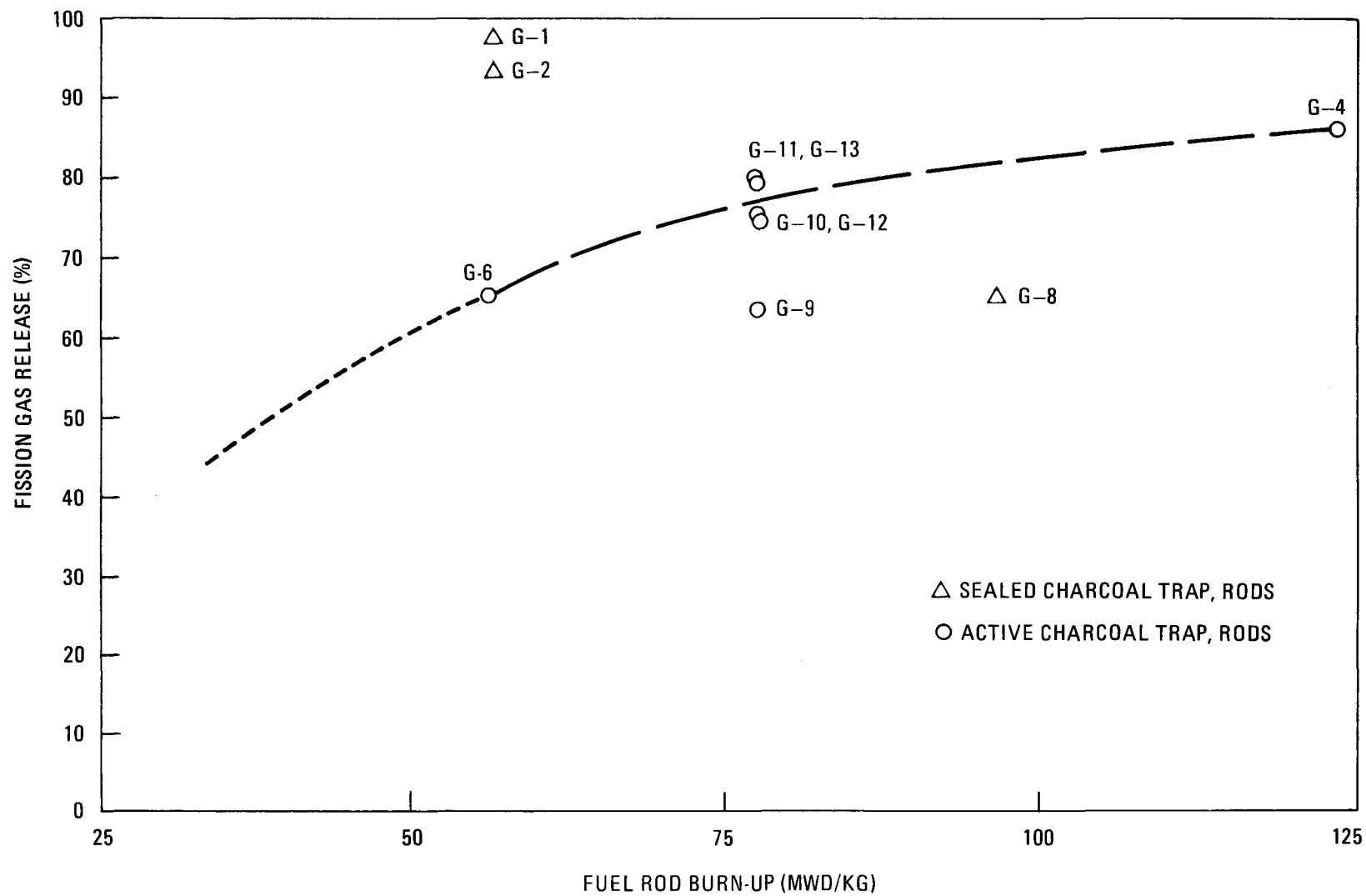


Fig. 5-2. Fission gas release vs fuel rod burn-up

TABLE 5-4
RESULTS OF GAS SAMPLING AND ANALYSES FOR
PREVIOUSLY ANALYZED F-1 FUEL RODS

	G-1	G-2	G-6
Fission gas volume (cm ³)	117	112	64
% fission gas released to plenum ^(a)	104.8	100.4	66.4
Peak burnup [MWd/kg (at. %)]	50 (5.4)	50 (5.3)	50 (5.3)
Fuel O/M	1.992	1.971	1.972
Cladding i.d. maximum temperature (°C)	760	738	670
Calculated centerline fuel temperature (°C)	2487	2485	2260

(a) Percent release data are based on measured burnup and fission product inventory values.

(b) The centerline fuel temperatures are calculated based on the run-by-run power data used for Ref. 5-4. The definition and accuracy of these calculations are unknown at this time. These values are related to the region above the reactor midplane. Note the difference in the values of rods listed in Table 5-3.

5.4. F-3 (X206) FAST FLUX IRRADIATION EXPERIMENT

There was no activity on this subtask during this quarter.

5.5. F-5 (X317) GRID-SPACED FUEL ROD BUNDLE FAST FLUX IRRADIATION EXPERIMENT

As reported previously (Ref. 5-5), the F-5 experiment was designed to study the performance of fuel rods irradiated under simulated GCFR conditions to determine (1) the reliability of the GCFR design and (2) the effect of a step power increase which simulates a 180-deg rotation of a subassembly at the core-blanket interface in the proposed GCFR demonstration plant. The irradiation of the F-5 (X317) grid-spaced bundle was begun in EBR-II run 93 in January 1978. At the end of EBR-II run 94, the experiment had achieved a peak exposure of 12 MWd/kg (1.42 at. %) burnup [1.4×10^{26} n/m² (E > 0.1 MeV)], with a goal of 50 MWd/kg in the first phase of its irradiation at 400 W/cm and a cladding midwall temperature of 700°C. Approval has been granted by the EBR-II project for irradiation of F-5 (X317) to 50 MWd/kg burnup prior to the first scheduled interim examination. Based on the current schedule, the start of the first interim examination will occur in the latter part of May 1979.

The design of the 19-rod grid-spaced bundle for the low-power portion of the irradiation of the F-5 (X317) irradiation experiment has been completed and submitted to ANL and the EBR-II project for review. The low-power run (240 W/cm, 540°C peak cladding midwall temperature) will have a duration of two EBR-II cycles and an additional exposure of 8 MWd/kg. The thermal-hydraulic calculations for the F-5 19-rod, low-power irradiation phase have been completed and show that bypass flow will not be needed to maintain the mixed-mean subassembly outlet below the row 7 limit of 526°C. This is due to the fact that the mean cladding midwall temperature goal for the low-power phase of operation is 530°C, based on the GCFR fuel rod temperature in the outer row of a GCFR fuel assembly located next to the radial blanket assemblies. The radial rod temperature gradients in the F-5 low-power (240 W/cm) phase are significantly less than those for the first high-power

(400 W/cm) phase, and thus there should be no mechanical interaction problems for the low-power, two-cycle EBR-II irradiation of the 19-rod bundle.

The preliminary design drawings for the 19-rod grid-spaced bundle hardware have been completed and approved by ANL and EBR-II. The 19-rod subassembly will have solid corner tie rods replacing the corner bypass tie tubes in the present assembly. The outer flow channels will be filled with a hexagonal spacer which will axially position the grid spacers. A dosimeter will be located in a hole in the middle of two opposed flats of the spacers and spacer grids and will be captured between the top and bottom spacer grid. The attachment ring will be thickened between the hex corners.

The lower shield will be cut below the upper hex section and a 29.5-mm hole bored in the circular section; there will be a sodium drain hole at the bottom. This cavity will contain two molybdenum capsules which will contain ceramic materials being evaluated as a lens or window for GCFR temperature monitoring. The upper and lower sections of the shield will not be welded together, since the shield piece is not a structural member (in tension) once the upper and lower preassembly hex ducts are welded to the upper hex part of the shield piece. The entire assembly will be within a hex duct (without load pads) which will be attached to the upper part of the shield piece by three Allen head stainless button head machine screws. This arrangement allows handling of irradiated and fresh fuel rods in the hot fuel examination facility (HFEF) during remote loading. The top end fixture can be attached to the duct at EBR-II. This hardware (including dummy roughened rods) can be used for flow testing of the assembly. Upon completion of the flow tests and when the dummy roughened rods are removed, the upper preassembly (upper handling socket and upper flow duct) to be used for X317A will be checked for fit to the grid assembly and lower shield piece. The dummy roughened rods will not be used for remote loading of fuel rods; instead, solid stainless rods with a reduced diameter at the upper end will fill the grid positions until they are removed one at a

time and replaced with fuel rods. Filler rods will be fabricated by the HFEF. The lower shield piece which will hold the lens and shield material samples has been received from EBR-II and will be machined by GA.

5.6. GB-10 VENTED FUEL ROD EXPERIMENT

During this quarter destructive examination of GB-10 vented fuel rod GA-21, which achieved an exposure of 112 MWd/kg in the ORR, continued at ANL. Postirradiation examination of the charcoal from the GB-10 fission product trap is continuing. Duplicate charcoal samples were shipped to INEL along with F-1 charcoal samples for isotopic cesium and rubidium analyses.

5.7. GCFR CLADDING IRRADIATION PROGRAM

The objectives of the GCFR cladding irradiation test program are as follows:

1. To determine whether the in-reactor creep behavior of ribbed cladding is different from that of smooth cladding.
2. To determine the effect of stress state on the in-reactor creep behavior.
3. Using a statistically designed test program, to determine the quantitative effect of ribs and stress state on creep behavior.

Some unstressed ribbed cladding specimens have already been irradiated and were to be studied in postirradiation creep rupture tests. An "approval in principle" had been granted for irradiation of additional unstressed test specimens; however, since recent data indicate that in-reactor creep is more prototypical compared with postirradiation testing and does not deleteriously affect cladding, the test program is being converted to an in-reactor creep test. The task of modifying the test matrix has been undertaken by ANL.

REFERENCES

- 5-1. Nuclear Systems Materials Handbook, Hanford Engineering Development Laboratory (TID-26666).
- 5-2. Gas-Cooled Fast Breeder Reactor Quarterly Progress Report for the Period February 1, 1978 Through April 30, 1978, DOE Report GA-A14928, May 1978.
- 5-3. Chang, K. H., "F-1 Fuel Centerline Temperatures," General Atomic Company, unpublished data.
- 5-4. Strain, R. V., and C. E. Johnson, "Postirradiation Examination of Fuel Pins at About 5.5 at % Burnup," Argonne National Laboratory Report ANL-76-129, to be published.
- 5-5. "Gas-Cooled Fast Breeder Reactor Quarterly Progress Report for the Period August 1, 1976 Through October 31, 1976," ERDA Report GA-A14112, General Atomic Company, November 1976.

6. FUEL ROD ENGINEERING (189a No. 00583)

The objective of this task is to evaluate the steady-state and transient performance of the fuel, blanket, and control rods for determination of performance characteristics, operating limits, and design criteria. To this end, analytical tools such as the LIFE code (Ref. 6-1) are being adapted and/or developed and applied to the analysis of GCFR prototypical rods and experimental rods. In addition, continuous surveillance of the LMFBR fuels and materials development program and technology is maintained to maximize the use of development technology and material properties. Support is also given to planning and designing of irradiation experiments.

6.1. FUEL, BLANKET, AND CONTROL ROD ANALYTICAL METHODS

6.1.1. Gas-Cooled Version of LIFE Transient Code

A preliminary version of the transient LIFE code (LIFE-T) and its steady-state counterpart (LIFE-SS) has been placed in the Berkeley Computer Facility by the LIFE Working Group. These codes are designed for analysis of fuel rod irradiation behavior during transient conditions. Both LIFE-T and LIFE-SS are needed for the transient analysis because the initial conditions for LIFE-T are, in general, obtained from a restart tape of the LIFE-SS code. LIFE-SS is essentially the same as the LIFE-III code, which is the current validated master version of LIFE steady-state, except for programming changes made so that LIFE-SS and LIFE-T are compatible. The LIFE-T code is derived from LIFE-III by changing the steady-state heat transfer formulation to the transient heat transfer formulation. Oxygen migration and dependence of fuel thermal conductivity on O/M have been added. Since LIFE-T and LIFE-SS were developed for LMFBR fuel rod analysis, they are not directly applicable to GCFR fuel rod analysis. To adapt them to GCFR fuel rod

transient analysis, the following needs to be done.

1. The sodium coolant heat transfer formulation must be changed to a helium coolant heat transfer formulation.
2. The gas mixture in the fuel to cladding gap must be changed to reflect the gas composition of the vented fuel rod for the gap conductance calculation.

The change of the sodium coolant heat transfer formulation to a helium coolant heat transfer formulation has been completed, and the gas-cooled version of LIFE-T is being compiled and tested. Modification of the gap conductance due to the gas mixture in the vented GCFR fuel rod requires further study of the fuel rod "breathing" phenomenon during transient conditions.

It should be noted that LIFE-T is an interim version of LIFE-IVT, which is intended to be the final version of the LIFE transient code based on the steady-state version LIFE-IV. LIFE-IVT and LIFE-IV are still being developed and will probably be available toward the end of this year. In its ultimate form, LIFE-IV will include not only the transient heat transfer formulation currently available in LIFE-T but also transient fuel behavior (such as fission gas release, swelling, and melting) and transient cladding mechanical deformation (such as high strain rate deformations). When LIFE-IV and LIFE-IVT are available, they should be adapted for GCFR fuel rod transient analysis.

6.1.2. Comparison of FRAP and LIFE Codes

Application of the FRAP codes (Refs. 6-2, 6-3) to analysis of GCFR fuel rod irradiation performance was reviewed, and FRAP-S3 and LIFE-III were compared based on Refs. 6-1 through 6-6. The FRAP codes consist of a pair of computer programs, FRAP-S3 (for steady-state analysis) and FRAP-T3 (for transient analysis), developed for the purpose of analyzing the fuel rod irradiation behavior for light water reactors (LWRs) [either boiling water

reactor (BWR) or pressurized water reactor (PWR)]. Initial conditions for FRAP-T3 are obtained from FRAP-S3. FRAP-T3 is capable of analyzing routine operating transients, hypothetical off-normal transients (such as loss of coolant accident, power-cooling mismatch accident, reactivity insertion accident), and anticipated transients without scram. With suitable modifications these capabilities could be useful in determining GCFR fuel rod transient behavior during, for example, a depressurization accident. The LIFE system is a series of codes including steady-state (LIFE III and IV), transient (LIFE-IVT), blanket (LIFE-B), and two-dimensional (R- θ) finite element structural analysis (TWOD) codes for pellet-cladding mechanical interaction.

Both the FRAP and the LIFE codes include virtually all the current important thermomechanical behavioral models in one form or another. However, since the development of each code family has been based on different reactor types, their emphasis on the material behavioral models is different. For example, cladding irradiation-induced swelling, which is important in fast breeder reactors but not in LWRs, is treated in the LIFE codes but not in the FRAP codes. LIFE-III does not include time-independent plasticity for cladding deformations because it has been found that the state of the cladding stresses never exceeds the elastic region during steady-state operation. Time-independent plasticity is treated in the FRAP codes owing to severe PCMI in LWR fuel rods.

6.1.2.1. Thermal Analysis. Both FRAP-S3 and LIFE-III perform an axisymmetrical steady-state heat transfer without axial heat flow. Their approaches are essentially the same except for

1. Cladding to fuel gap conductance. In addition to the Ross and Stoute model used in LIFE-III, FRAP-S3 can model cracked pellet and fuel relocation to allow for partial fuel to cladding contact even when the gap is still open.

2. Plenum gas temperature calculation. FRAP-S3 calculates plenum gas temperature based on energy transfer between the top of the pellet stack and the plenum gas, between the coolant and the plenum gas, and between the holddown spring and plenum gas. In LIFE-III, calculation of plenum gas temperature is based upon the volumetric average temperature of the plenum, fuel to cladding gap, and central void.

6.1.2.2. Fission Gas Release. The fission gas release model used in FRAP-S3 is strictly empirical. Gas release is determined by escape of gas from the fuel matrix and release of trapped gas from grain boundaries or dislocations which are arbitrarily set in proportion to gas production rate and amount of gas being trapped. The proportional constants are evaluated from available data. In the LIFE code, the fission gas release approach is semiempirical and considers the mechanisms of diffusion of gas atoms to free surfaces and migration of gas bubbles up the temperature gradient to the central void. The bubble velocity is assumed to obey the Nichols equation, with certain parameters determined by available data.

6.1.2.3. Fuel Restructuring. Both FRAP-S3 and LIFE-III compute grain growth in the columnar and equiaxed fuel regions. Pore migration, which has an important effect on fuel temperature distribution, is treated in LIFE-III but not FRAP-S3.

6.1.2.4. Cladding Mechanical Model. FRAP-S3 treats the cladding as a cylindrical thin shell, and LIFE-III considers the cladding in the state of generalized plane strain. FRAP-S3 includes time-independent plasticity and LIFE-III does not, and FRAP-S3 does not consider cladding irradiation-induced swelling; LIFE-III does. Both FRAP-S3 and LIFE-III include creep deformation.

6.1.2.5. Fuel Mechanical Model. FRAP-S3 calculates fuel deformation as a function of thermal expansion, irradiation swelling, and densification. There are no pellet deformations from stresses induced by PCMI or thermal

stresses, and there is no pellet creep. LIFE-III treats pellet deformation using the finite deformation theory of time-dependent plasticity. FRAP-S3 considers fuel densification, which is an irradiation-induced sintering phenomenon, and LIFE-III deals with pressure sintering, or hot pressing. FRAP-S3 treats fuel swelling by an empirical approach based on LWR data; gaseous swelling in LIFE-III is based upon ideal gas behavior.

6.1.2.6. Fuel Cladding Mechanical Interaction. Slip between fuel and cladding is not permitted in FRAP-S3; LIFE-III permits axial slipping by assigning a coefficient of friction. In FRAP-S3 the fuel is assumed to be rigid; it is assumed to be deformable in LIFE-III.

6.1.2.7. Code Modifiability. Both FRAP-S3 and LIFE-III are modular codes and are easy to modify without impacting the unmodified portion of the code.

6.1.2.8. Application of the FRAP Codes to GCFR Fuel Rod Analysis. In view of the above discussion, it is obvious that the FRAP codes need to be modified before they can be applied to GCFR fuel rod analysis. These modifications include incorporating cladding irradiation-induced swelling, improving the fission gas release model, changing fuel behavior from rigid to deformable, and upgrading the fuel swelling model. More importantly, since the FRAP codes have been based upon LWR fuel rods, where the irradiation environment (e.g., fast flux and power density) is considerably different from that of fast breeder fuel rods, the empirical constants used in the material models need to be recalibrated using the fast breeder reactor fuel rod data basis. This could be a time consuming process. On the other hand, there is generally no such problem associated with applying the LIFE codes to GCFR fuel rod analysis because LIFE is being developed based on fast breeder reactor fuel rod irradiation test data.

Table 6-1 lists the items which must be modified in the FRAP codes for GCFR fuel rod analysis, and Table 6-2 gives the advantages and disadvantages of the FRAP codes compared with the LIFE codes. Since the review is preliminary, these tables are by no means complete.

TABLE 6-1
ITEMS TO BE MODIFIED IN FRAP CODES
FOR GCFR FUEL ROD ANALYSIS

Coolant to cladding surface heat transfer
Fission gas release model
Fuel swelling model
Pore migration
Cladding irradiation-induced swelling
Fuel mechanical model
Plutonium and oxygen migration in fuel
Material property correlations
Model calibration with fast breeder reactor fuel rod irradiation data
Slipping between fuel and cladding when they are in contact

TABLE 6-2
ADVANTAGES AND DISADVANTAGES OF FRAP
CODES COMPARED WITH LIFE CODES

Advantages

Cracked pellet gap conductance model to enhance heat transfer
Two dimensional R- θ heat conduction option
More mechanistic approach in plenum gas temperature calculation
May be able to deal with more kinds of transient fuel rod thermo-mechanical responses

Disadvantages

Most of the fuel behavior models are empirical approaches contrasted with the combined use of first principles and test data in the LIFE codes
No plutonium and oxygen migration model
No pore migration model
No slipping permitted when fuel and cladding are in contact
Unsophisticated fuel mechanical model
Data based on LWR fuel rod irradiation tests

Although LIFE-III is more suitable for steady-state GCFR fuel rod analysis, the transient version of LIFE is not generally available at the current time. In view of the versatile application of FRAP-T3 to transient analysis, it would be prudent to follow the development of the FRAP codes as a future alternative means of GCFR fuel rod analysis.

6.2. ANALYSIS OF IRRADIATION TESTS

6.2.1. F-1 Irradiation Test Analysis

The irradiation performance analysis of the 13 fuel rods from the F-1 experiment was completed. The analysis was performed using the LIFE-III code. The analytical and experimental results were compared in the areas where postirradiation examination data were available. Figure 6-1 compares the calculated and measured values for fission gas release. The LIFE code seems to overpredict fission gas release (~15%). This could have arisen because the measured data did not account for the fission gas retention in the active charcoal and the measured percentage was based on the ratio of the fission gas recovered from the plenum to the calculated total fission gas generation. Therefore, the fission gas release comparison will be revised once fission gas retention in active charcoal and fuel is measured. The calculated and measured cladding diameter change $\Delta D/D$ is shown in Figs. 6-2 through 6-5. It can be seen that the calculated LIFE values are at about the mean value of the experimental results.

In addition, an investigation was performed to evaluate the effect of the fuel-cladding mechanical interaction (FCMI) on cladding plastic strain. Fuel swelling was artificially suppressed in the LIFE code to eliminate the FCMI and yet maintain the same plenum pressure history as in the case with fuel swelling (see Fig. 6-6). Five rods were analyzed: G-1 represents the low-burnup rod, G-4 the highest-burnup rod, G-7 the lowest-temperature rod, G-8 the medium-burnup rod, and G-13 the highest-temperature rod. The cladding total creep strain with and without the PCMI effect is plotted against time in Figs. 6-7 to 6-11 for these

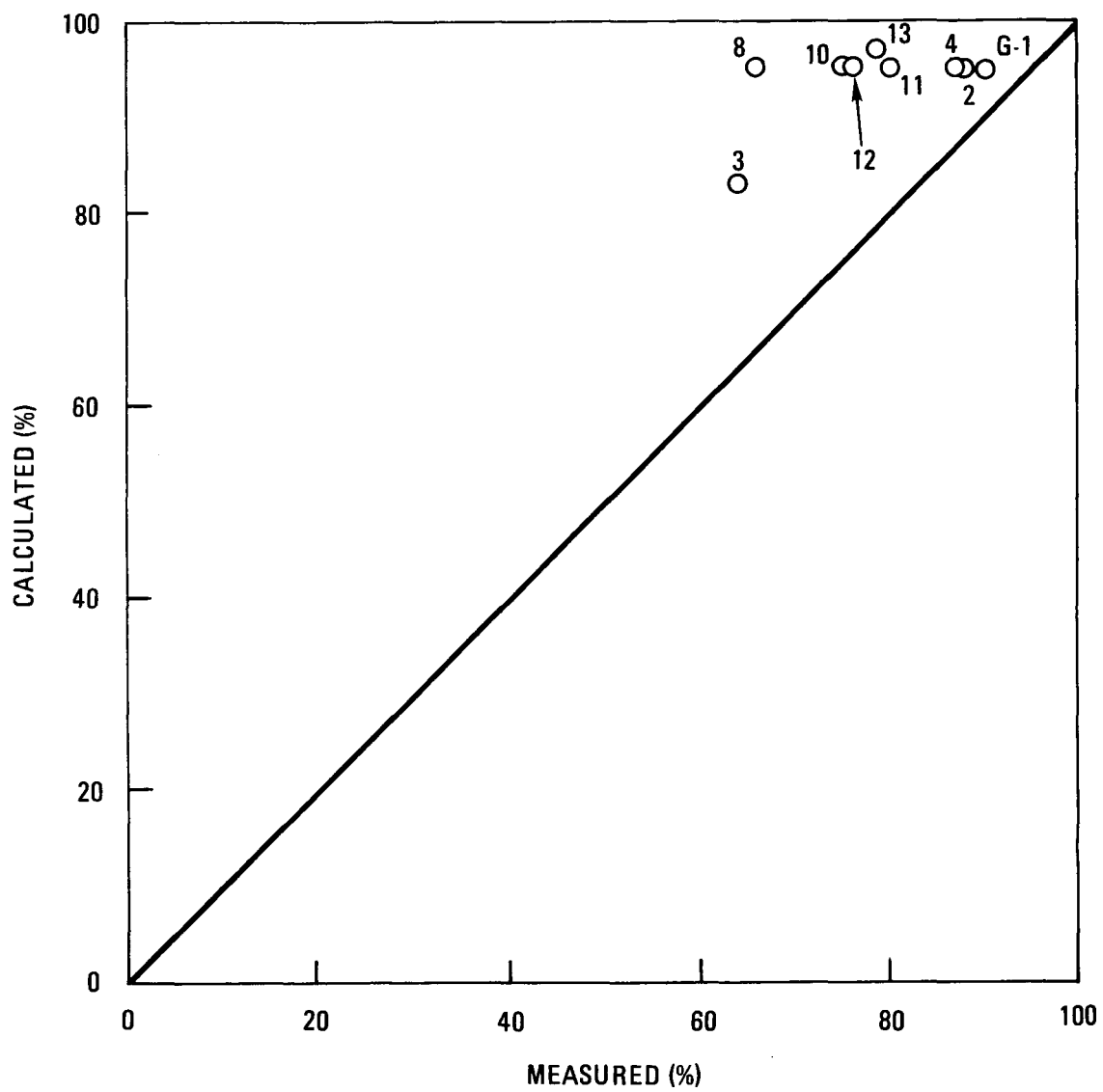


Fig. 6-1. Comparison of calculated and measured fission gas release (F-1 experiment)

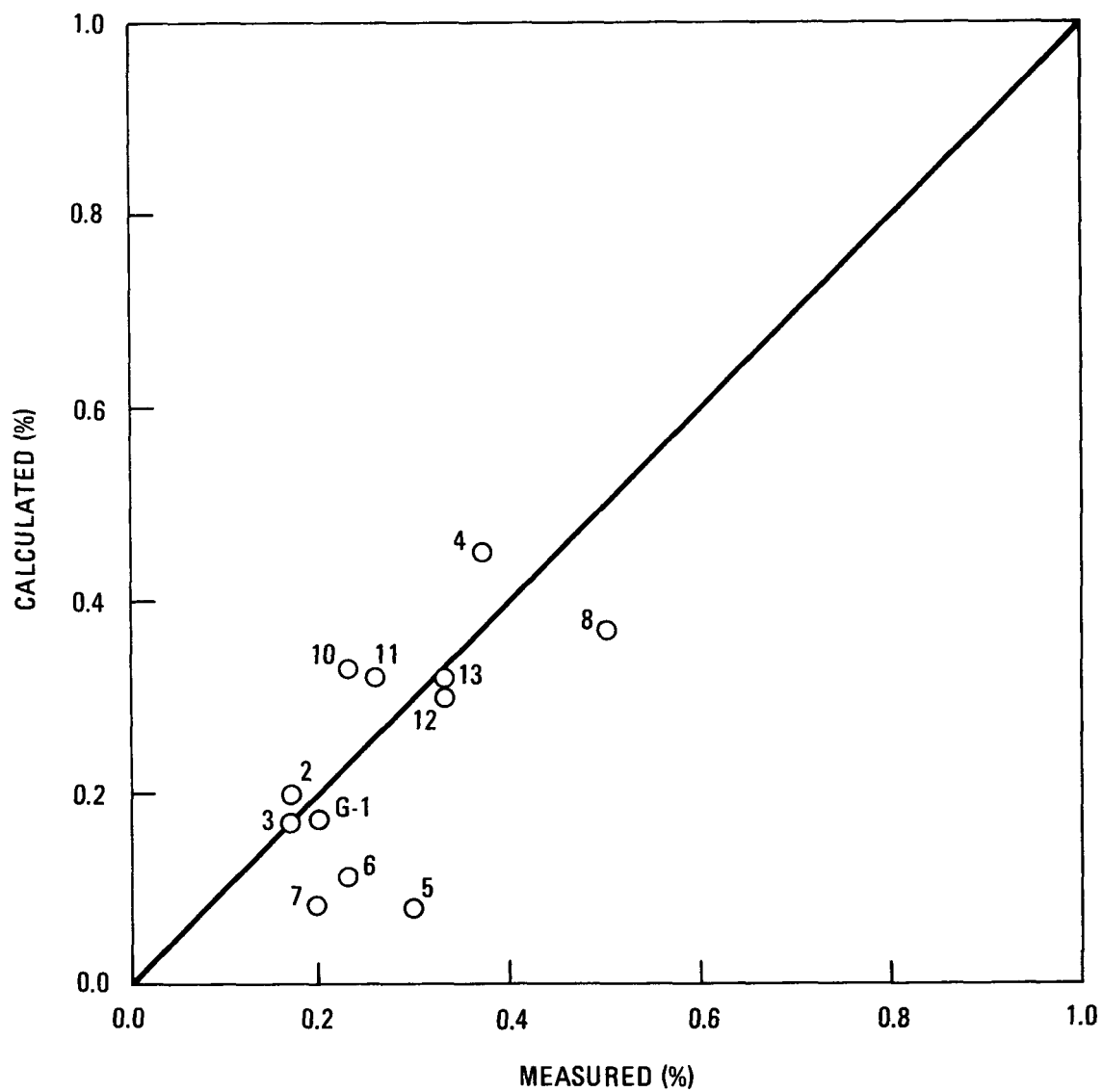


Fig. 6-2. Comparison of calculated and measured cladding diameter change $\Delta D/D$, axial section I (F-1 experiment)

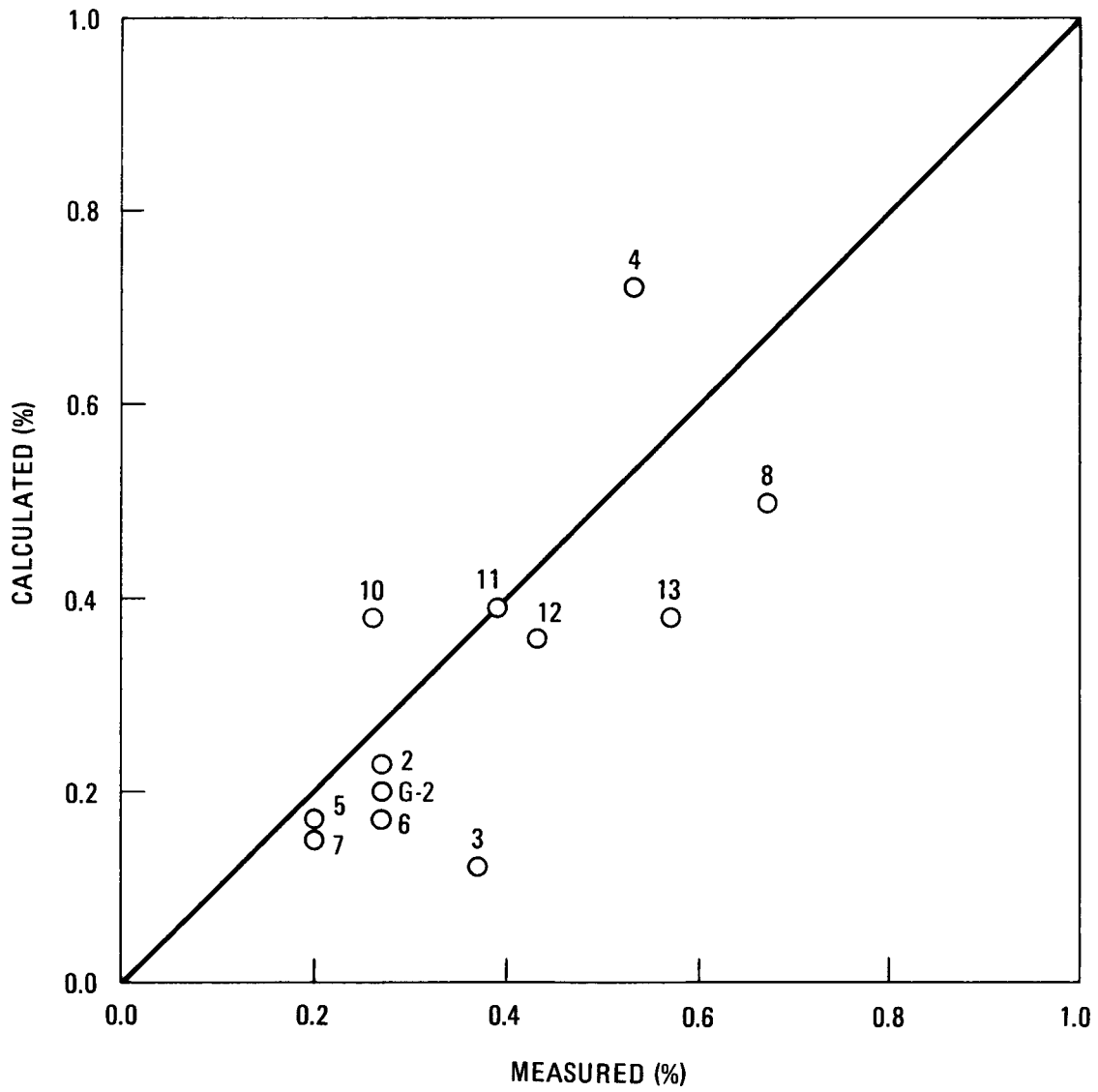


Fig. 6-3. Comparison of calculated and measured cladding diameter change $\Delta D/D$, axial section II (F-1 experiment)

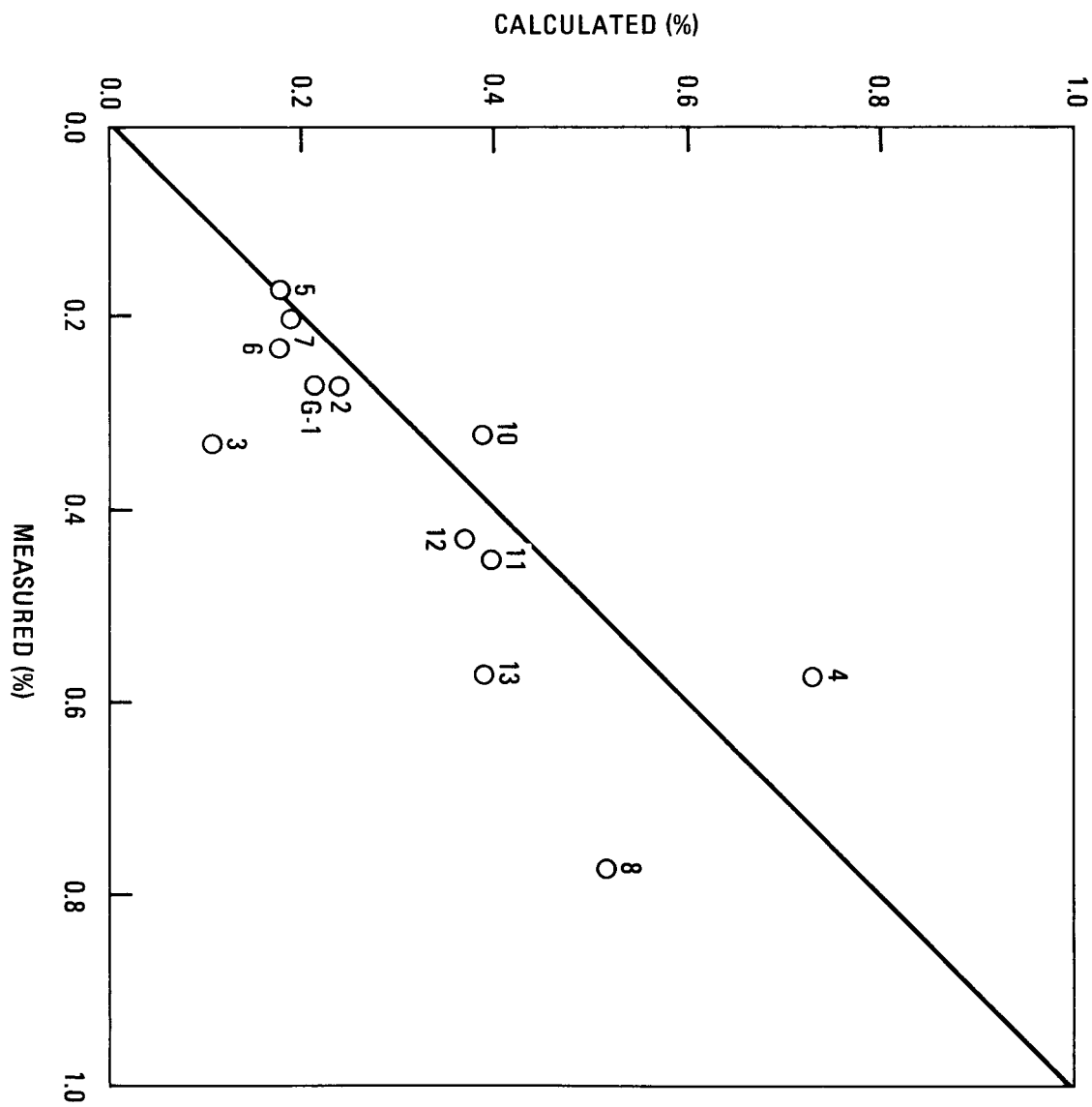


Fig. 6-4. Comparison of calculated and measured cladding diameter change $\Delta D/D$, axial section III (F-1 experiment)

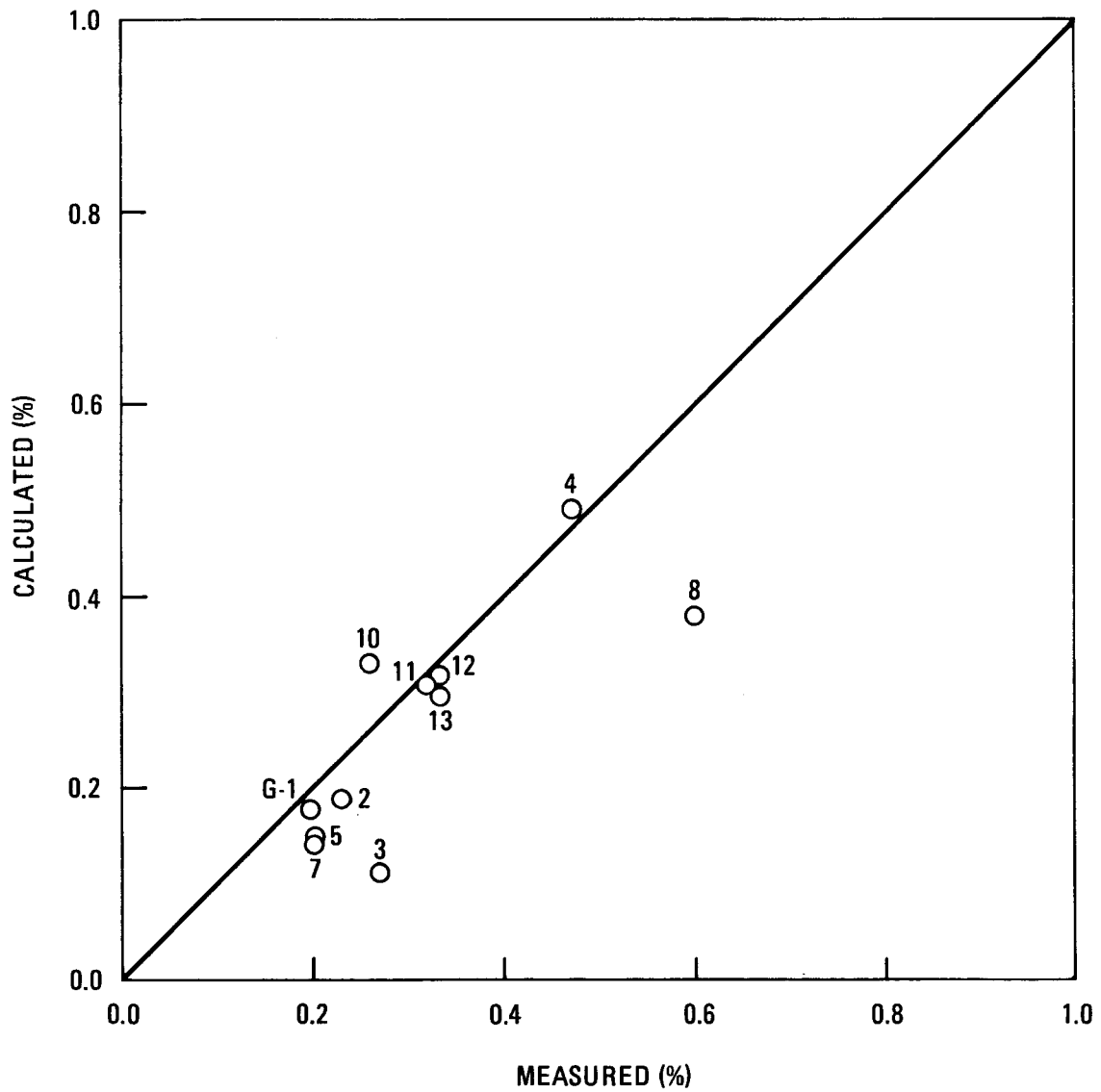


Fig. 6-5. Comparison of calculated and measured cladding diameter change $\Delta D/D$, axial section IV (F-1 experiment)

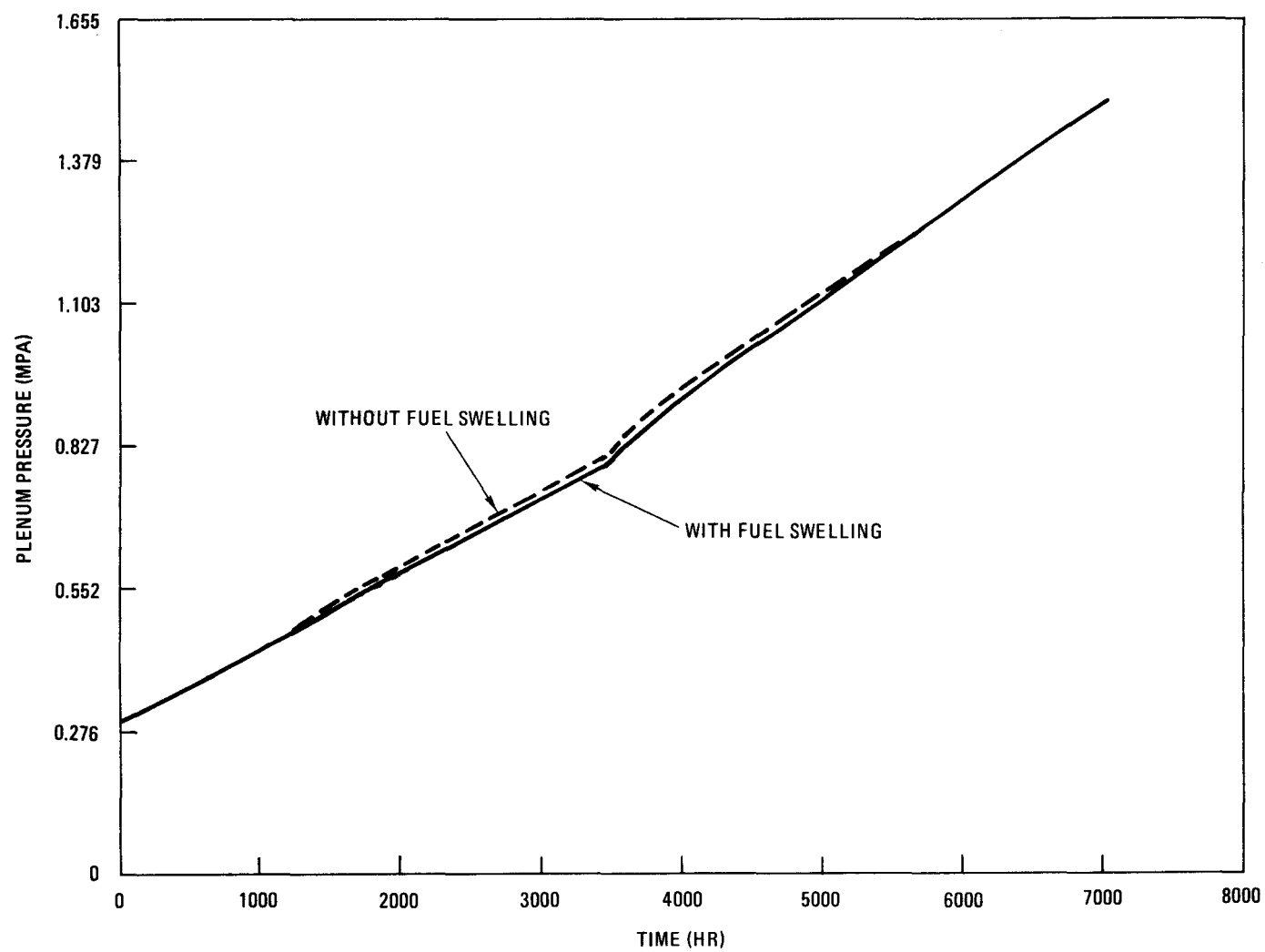


Fig. 6-6. G-1 plenum pressure (F-1 experiment)

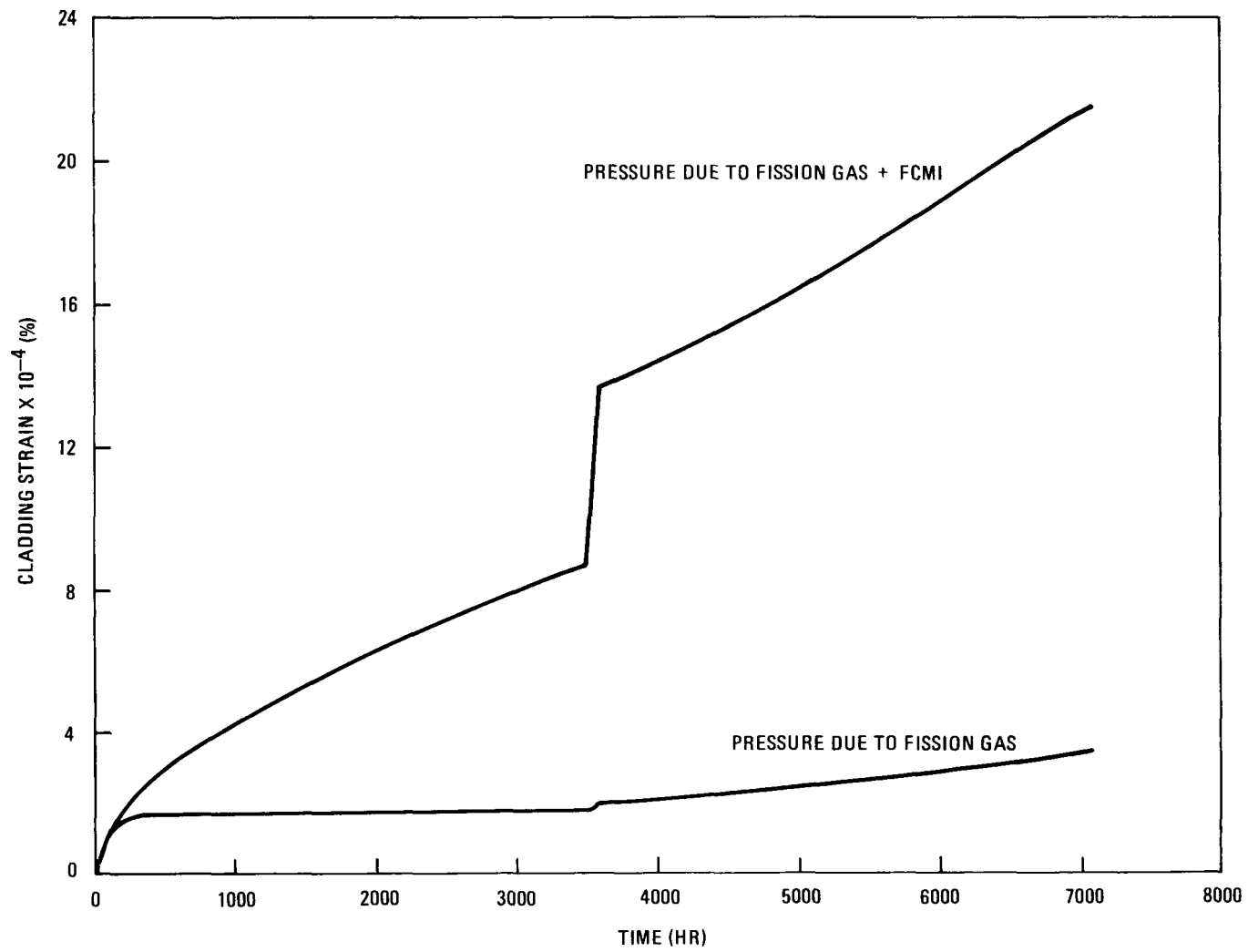


Fig. 6-7. G-1 total cladding creep strain, axial section III (F-1 experiment)

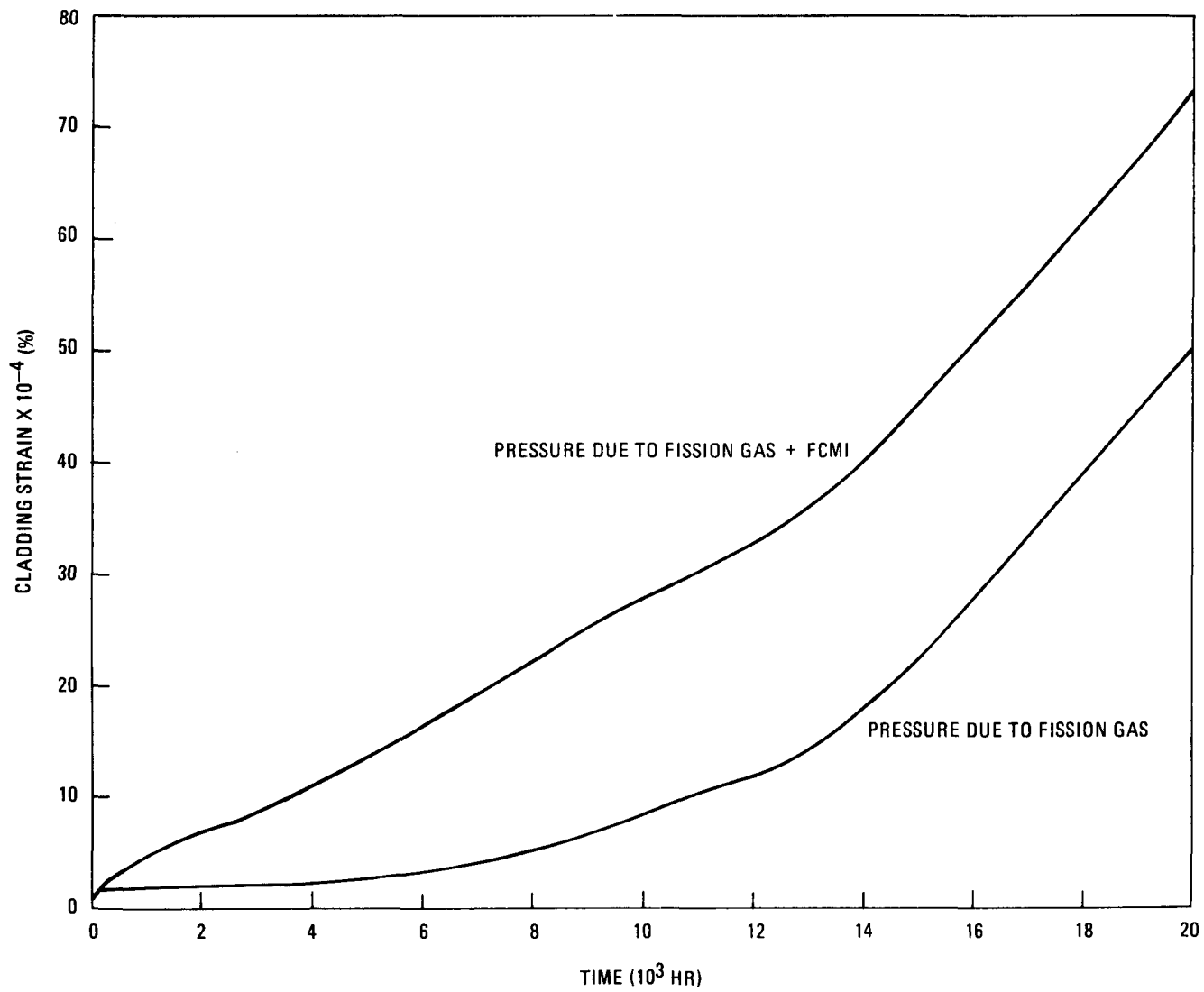


Fig. 6-8. G-4 total cladding creep strain, axial section III (F-1 experiment)

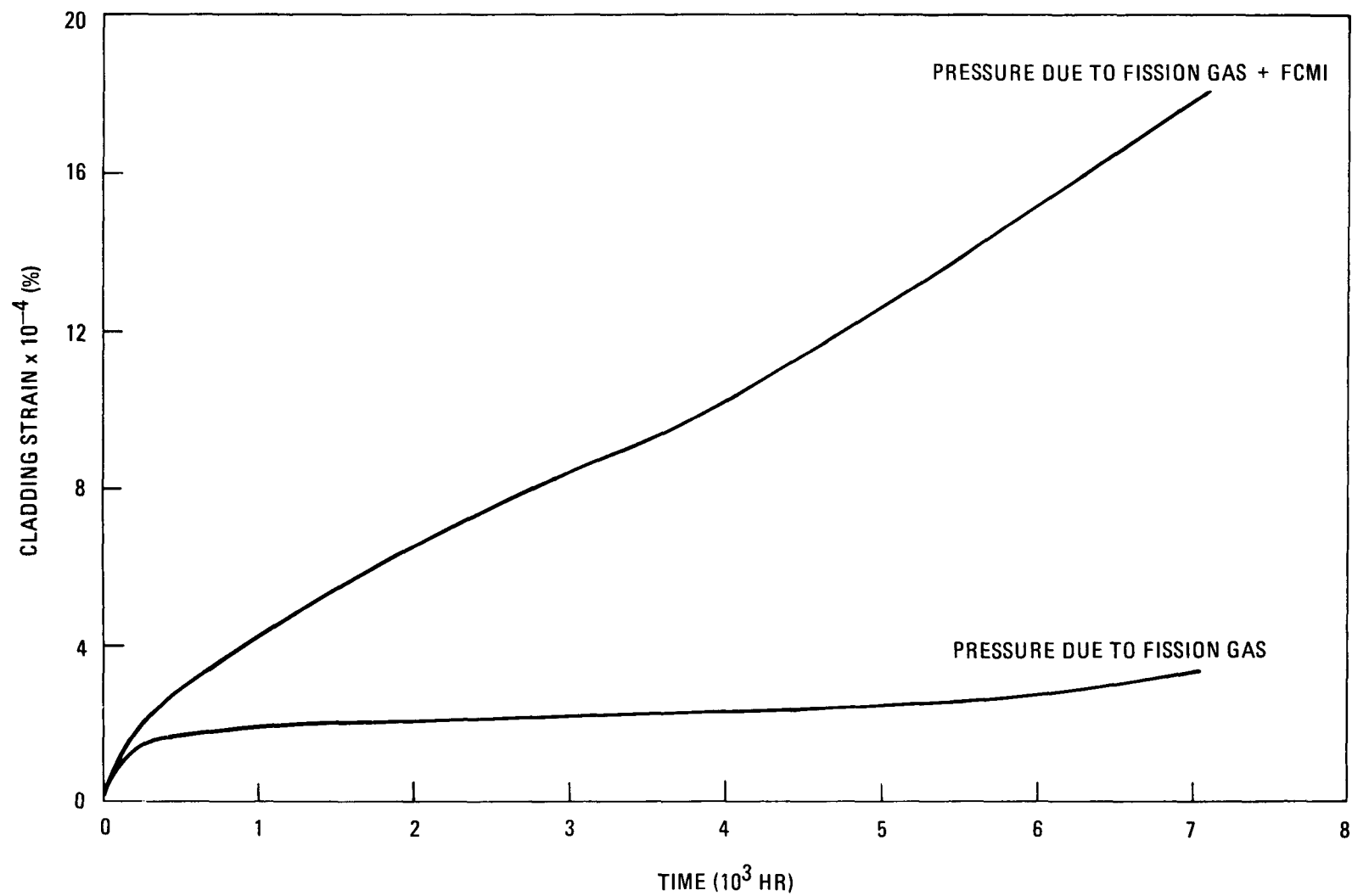


Fig. 6-9. G-7 total cladding creep strain, axial section III (F-1 experiment)

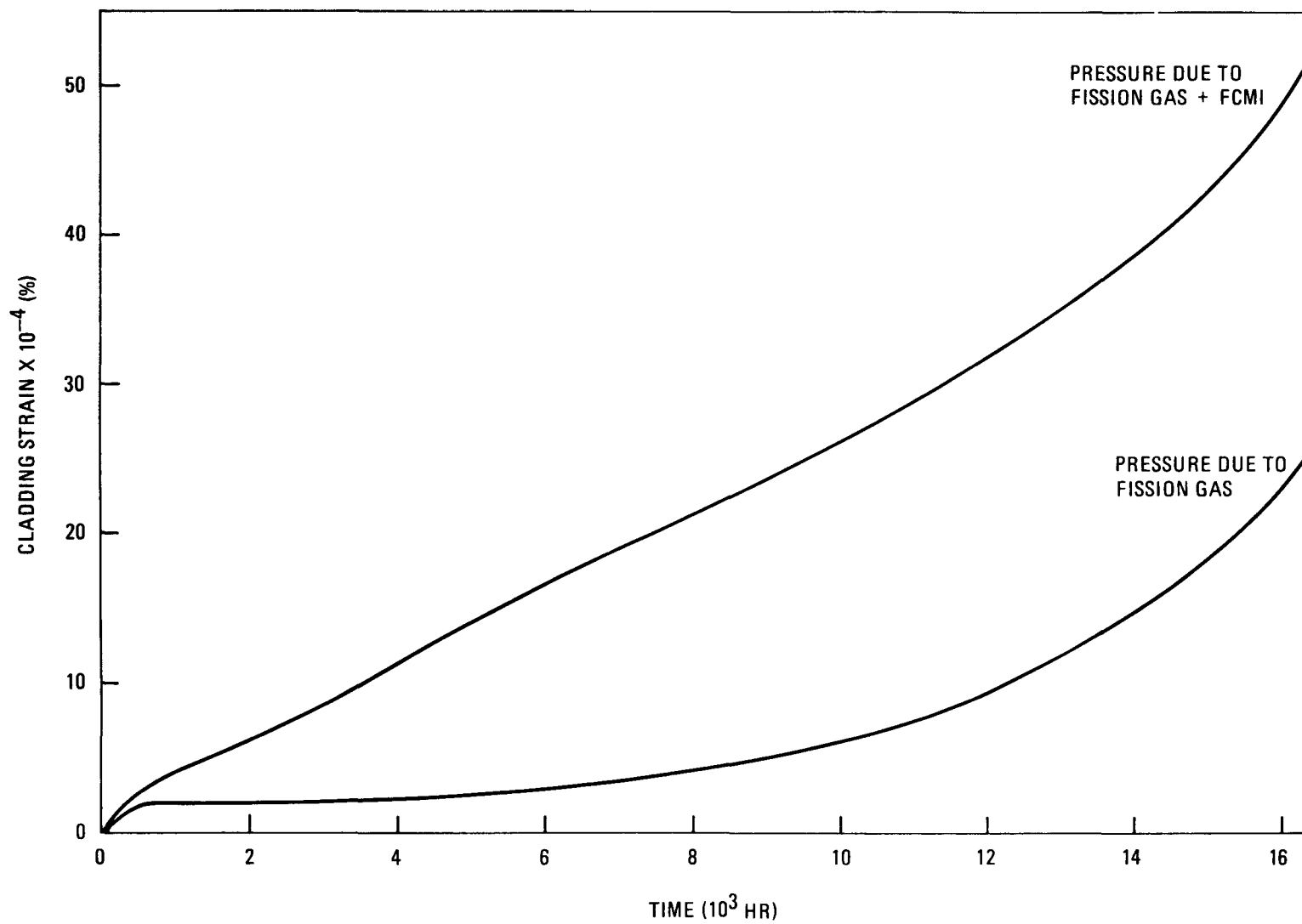


Fig. 6-10. G-8 total cladding creep strain, axial section III (F-1 experiment)

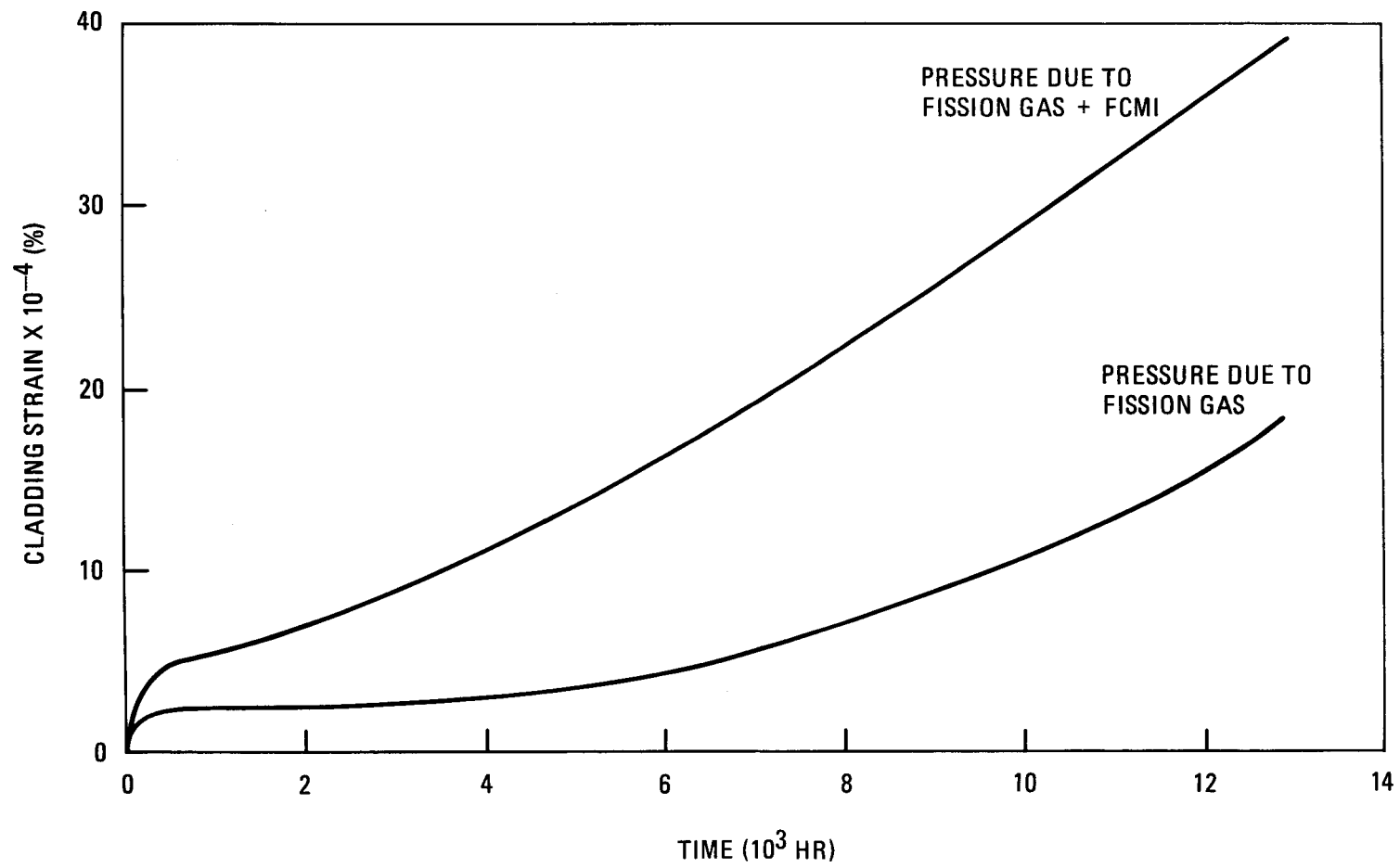


Fig. 6-11. G-13 total cladding creep strain, axial section III (F-1 experiment)

five rods. It is seen that the FCMI contributes significantly to the cladding total creep strain in the F-1 fuel rods, especially for rod G-1, which was subject to a large power jump, and rod G-7, which was the lowest-temperature rod.

6.2.2. GB-10 Irradiation Test Analysis

The analysis of the GB-10 sweep gas capsule irradiation test was completed. The analysis was performed using a combination of the LIFE code and the isotopic fission gas release subroutine (GAREL). The modeling of GB-10 included the flux depression effect resulting from irradiation in the ORR. The isotopic fission gas release rates are being compared with the measured release data to determine the diffusion coefficients of krypton and xenon in the fuel.

6.3. ROD ANALYSIS AND PERFORMANCE

There was no activity on this subtask during this quarter.

6.4. FUEL ROD MECHANICAL TESTS

All hardware necessary for beginning mechanical testing of fuel rods has been obtained. Initial testing will include tensile, compressive column buckling), flexure, and Hertzian testing. Tensile testing will be done at temperatures up to 1000°C; all other testing will be done at room temperature. The matrix for the tests is shown in Table 6-3. Testing cladding material at both the root diameter and the rib height diameter will give insight on the strengthening value of the ribbing on the actual fuel rods. Special fixtures have been fabricated for these tests, and preliminary tests have been run to determine their suitability for the planned tests.

TABLE 6-3
FUEL CLADDING MECHANICAL TEST MATRIX

Test	Number of Tests			Remarks
	Ribbed	Smooth No. 1 ^(a)	Smooth No. 2 ^(b)	
Flexure	8	8	8	4 tests each at lengths of 10 and 20 cm
Compression	4	4	4	Sample length = 50 cm
Tensile Room temperature	9	9	9	3 tests at length = 20 cm
300°C	3	3	3	Procedure ASTM E-21-70 used for all tests at length = 20 cm
600°C	3	3	3	
1000°C	3	3	3	
Hertzian stress	3	3	3	All tests at length = 20 cm

(a) Smooth No. 1 tubing ground to root diameter = 7.33 mm o.d.

(b) Smooth No. 2 o.d. = 7.46 mm.

REFERENCES

- 6-1. Billone, M.C., et al., "LIFE-III Fuel-Element Performance Code," ERDA Report 77-56, Argonne National Laboratory, July 15, 1977.
- 6-2. Dearin, J. A., et al., "FRAP-S3: A Computer Code for the Steady-State Analysis of Oxide Fuel Rods," v. 1, Rev. 2, EG&G Report TFBP-TR-164, October 1977.
- 6-3. Dearin, J. A. et al., "FRAP-T3: A Computer Code for the Transient Analysis of Oxide Fuel Rods," EG&G Report TFBP-TR-194, August 1977.
- 6-4. Bohn, M. P., "Deformation and Failure Analysis in the FRAP Fuel Rod Codes," Paper Presented at the Fourth International SMIRT Conference, San Francisco, August 15-19, 1977.
- 6-5. Reymann, G. A. (ed.), "MATPRO-Version 10, A Handbook of Materials Properties for Use in the Analysis of Light Water Reactor Fuel Rod Behavior," EG&G Report TREE-NUREG-1180, February 1978.
- 6-6. Bohn, M. P., "A Comparison of the LIFE-III Code and the FRAP-S and FRAP-T Codes for Application to the Gas-Cooled Fast Reactor," Draft Report, EG&G.

7. NUCLEAR ANALYSIS AND REACTOR PHYSICS (189a No. 00584)

The scope of activities planned under this task encompasses the validation and verification of the nuclear design methods which will be applied to the GCFR core design. This will primarily be done by evaluating the methods using a critical assembly experimental program specifically directed toward GCFR development. Program planning and coordination activities, critical assembly design and analysis, and the necessary methods development will be carried out.

During the previous quarter, detailed XY models of various configurations of the phase II GCFR critical assembly were established for diffusion theory calculations using the 2DB code (Ref. 7-1). The spectrum code improvement resulted in a stand-alone version of the synthetic kernel method (SKM2) which will replace the GAROL resolved resonance code (Ref. 7-2). Testing of ENDF/B-V was carried out for the U-233 bare-sphere "Jezebel" benchmark, with significant eigenvalue improvement. During the present quarter, work continued on the phase II analyses, mostly utilizing the flux profile data from the past 2DB calculations to derive reaction ratios and rate profiles for comparison with measurements.

As a prelude to commencing analysis of the phase III GCFR critical assembly experiments, a review of all past phase II calculations was undertaken to quantify the importance of various methods. In the time frame anticipated for phase III work, it may be necessary to continue using the GGC-5 spectrum code (Ref. 7-3) for cross section generation. Cell heterogeneity corrections will be provided by calculations with the DTFX discrete ordinate transport code (Ref. 7-4); sufficient accuracy can be attained for these cases with less rigorous analyses than previously used, and some of the existing phase II cell factors may be applicable.

It is apparent that the most sensitive method area is generation of leakage parameters. One option is the dilution factors used in GFE-4 (Ref. 7-5) for generating the scattering cross sections used by the GAM routine in GGC-5 and the plate transport cross sections involved in evaluating streaming effects. Using dilution factors appropriate for the ZPR9 core cells, assembly k-values were calculated. These values are about 0.5% lower than those provided by past analyses in which infinite dilution was assumed, i.e., the 1/E flux weighting in the older GFE routine.

Some transport calculations for the phase II assembly in RZ geometry were found to yield 0.75% higher eigenvalues than comparable standard diffusion theory problems. The definition chosen for GGC-5 for generating microscopic transport cross sections is of less importance. The difference between using either definition 1 or 2 is a k value of about 0.1%. Use of the less rigorous definition 2 will continue because it is essential for steam worth analysis.

For phase II, the total heterogeneity effect (the difference from using simple homogeneous cell GGC-5 cross sections and fully heterogeneous analysis) amounted to a k value of about +1.2% without streaming effects. The use of DFTX shielding factors for the resolved-resonance groups instead of the inherent factors provided by the GAROL routine in GGC-5 accounted for only about 0.1% Δk and will be discontinued for phase III in view of the considerable time and work involved in deriving the in-plate cross sections required for this method.

REFERENCES

- 7-1. Little, W. W., Jr., and R. W. Hardie, "2DB, A Two-Dimensional Diffusion-Burnup Code for Fast Reactor Analysis," USAEC Report BNWL-640, Battelle Northwest Laboratory, January 1968.
- 7-2. Stevens, C. A., and C. V. Smith, "GAROL, A Computer Program for Evaluating Resonance Absorption Including Resonance Overlap," General Dynamics, General Atomic Division Report GA-6637, August 24, 1965.

- 7-3. Mathews, D. R., et al., "GGC-5, A Computer Program for Calculating Neutron Spectra and Group Constants," Gulf General Atomic Report GA-8871, September 27, 1971.
- 7-4. Mathews, D. R., General Atomic, private communication.
- 7-5. Mathews, D. R., General Atomic, private communication.

8. SHIELDING REQUIREMENTS (189a No. 00584)

The purposes of the shielding task are to verify the adequacy of the methods and data for the nuclear design of GCFR shields, to evaluate the effectiveness of various shield configurations, and to provide an interface for mechanical and nuclear shield design activities. This task also coordinates and provides liaison with the analytical and experimental shielding program at ORNL.

During the last quarter, physics input for the material service environment was provided for an assessment of material irradiation effect data requirements for out-of-core GCFR structural components. A reference EOEC RZ core and blanket model was determined for use in GA and ORNL shielding analyses. Several design alternatives for lower plenum shielding were examined, and a study of methods to reduce lower plenum streaming was initiated. The DOT 3.5 code (Ref. 8-1) was made operational. During this quarter, generic studies for radial shielding material arrangement and thickness were completed, and a full reactor RZ model of a candidate revised shield was determined for forthcoming analyses at ORNL. Preliminary results from the lower plenum streaming study were obtained, and a preliminary study of heating rates in a proposed depleted uranium core catcher was completed.

8.1. GENERIC STUDIES FOR RADIAL SHIELDING

8.1.1. Analysis

One-dimensional 36-group (26 neutron and 10 gamma) discrete ordinates calculations were carried out using S_8 quadrature for the 7 radial shield configurations shown schematically in Fig. 8-1. Cases 1 through 4

CASE

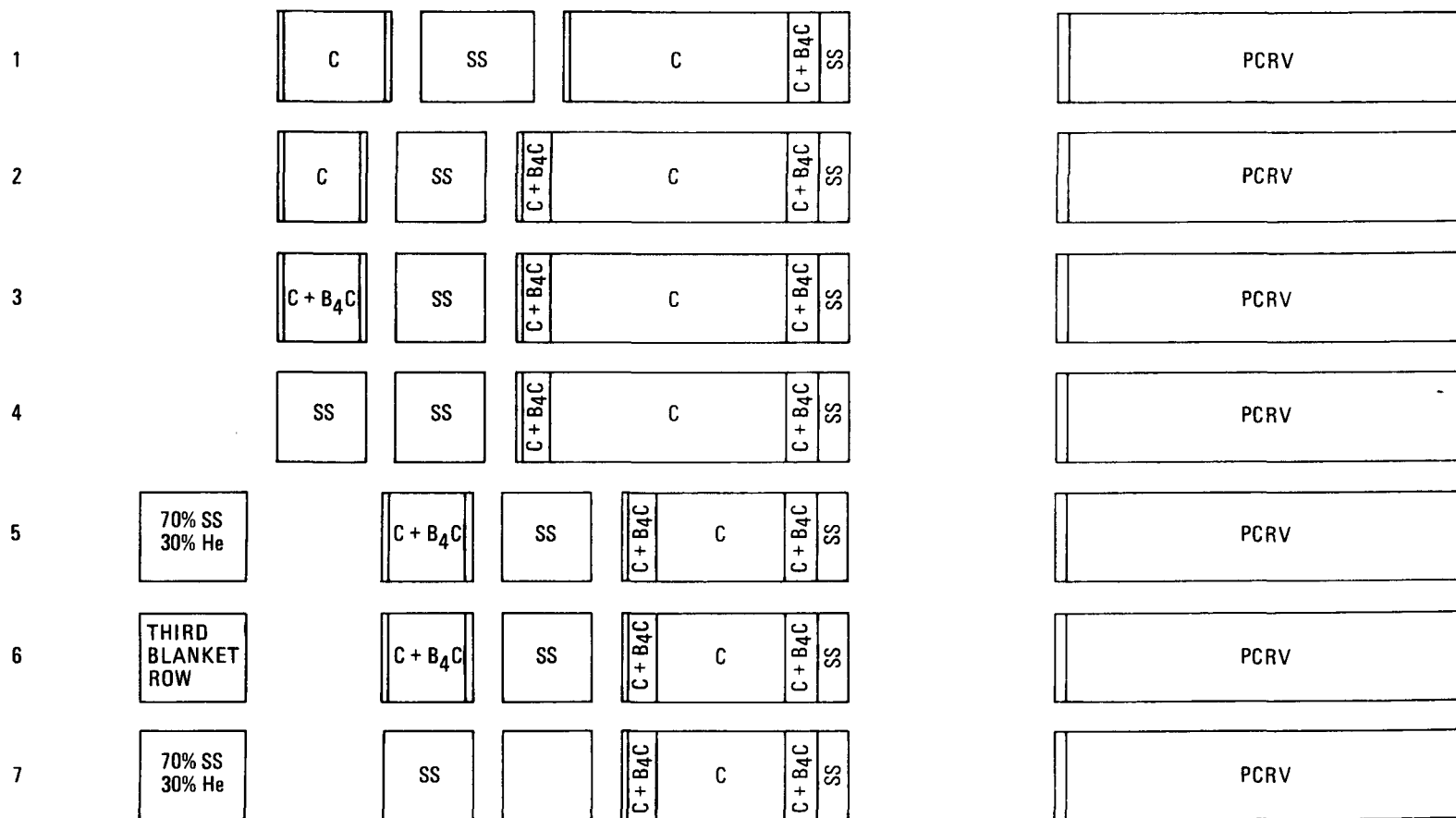


Fig. 8-1. Alternative radial shielding configurations (SS = stainless steel)

correspond to radial shield configurations consisting of two rows of removable radial shield assemblies supported by a permanent outer radial shield. For cases 5 through 7, a third row of blanket or reflector-shield assemblies is suspended on the grid plate. The outside radius of the outer shields is identical for all cases (i.e., same radial envelope). Full material densities (e.g., no allowance for graphite or boronated graphite packing fraction or gaps) were assumed for the shielding. A 0.30 helium volume fraction for the stainless steel reflector assemblies (cases 5 and 7) was assumed to allow for core inlet helium cooling of these assemblies. The core and blanket distributed fission sources were obtained from one-dimensional eigenvalue calculations using core midplane nuclide densities corresponding to reference EOEC shielding core Model A (Ref. 8-2). Reference EOEC core model A simulates a core with the following general characteristics:

1. Pressure drop = 0.290 MPa.
2. Number of assemblies = 127.
3. Radial blanket management scheme = in-out shuffling with rotation (Fig. 8-2).
4. Assembly geometry given in Ref. 8-2.
5. End of cycle six (EOC6) described in Ref. 8-3.

Case 1, consisting of a canned graphite row 1 inner shield, a solid 316 stainless steel row 2 inner shield, and a laminated graphite, boronated graphite and 316 stainless steel outer shield approximates the current reference design except that the total shield is thicker to allow for lower primary coolant system boundary (PCSB) radiation exposure and heating limits and to attain larger exposure margins at the PCSB. Case 1 is an "optimum" arrangement in terms of reducing PCSB exposure without the use of boronated graphite in high-flux locations. The alternative configurations (cases

FEVER TIME POINT CYCLE RELOAD			BLANKET ASSEMBLY RESIDENCE HISTORY												TIME POINT DESIGNATION	
			ROW 1			ROW 2										
			0	1	2	0	0	0	0	0	0	0	1	2	2	2
CYCLES RESIDENT IN			0	0	0	0	1	2	3	4	5	6	0	0	1	2
0	1	1	42			48										BOL
1	1			42			48									EOC1
2	2	2		42*			48									BOC2
3	2				42				48							EOC2
4	3	3	42						6				42**			BOC3
5	3			42						6				42		EOC3
6	4	4		42*						6				42		BOC4
7	4				42						6				42	EOC4
8	5	5	42							6			42**			BOC5
9	5			42							6			42		EOC5
10	6	6		42*							6			42		BOC6
11	6				42							6			42	EOC6
12	7	7	42				6						42**			BOC7
13	7			42				6						42		EOC7
14	8			42*					6					42		BOC8
15	8				42					6					42	EOC8

*ALL ROW 1 ASSEMBLIES ROTATED 180 DEG

**ALL ROW 1 ASSEMBLIES SHUFFLED TO ROW 2

Fig. 8-2. Radial blanket management scheme

2 through 7) generally differ from case 1 by the replacement of some graphite with either boronated graphite or stainless steel to achieve one or more of three objectives:

1. To significantly reduce the low-energy flux component which is primarily responsible for helium gas generation in the structural steel.
2. To reduce the gamma ray heating in the removable inner radial shield assemblies.
3. To reduce backscattering of neutrons into the lower reactor plenum.

8.1.2. Radiation Exposure Criteria

A review of the national program on the effects of irradiation on the mechanical properties of fast breeder reactor structural materials (Ref. 8-4) revealed that a more conservative approach to defining radiation exposure limits for permanent GCFR out-of-core components is required. This is because an adequate irradiation effects data base for defining component fluence limits (especially for GCFR high-temperature alloy components in the lower plenum) is not currently available, and much of the needed data will not be available until the mid-1980s. Moreover, sufficient data to validate energy-dependent damage correlations such as effective damage functions will not be available for several years. As a result, radiation exposure limits for GCFR permanent out-of-core components have been proposed which are based on LMFBR [fast test reactor (FTR) and Clinch River breeder reactor (CRBR)] criteria (Ref. 8-3). The status of the radiation exposure criteria for GCFR lower plenum components is outlined below.

1. Removable structural components

a. Metal alloys

- (1) Physical criterion: 5% residual ductility based on total elongation
- (2) Exposure limit: insufficient irradiation effects data base

b. Graphite (H-451)

- (1) Physical criterion: zero expansion in radial direction (related to extrusion axis)
- (2) Exposure limit: lifetime fluence $\leq 3.8, 2.5,$ and 1.4×10^{22} effective fission fluence for graphite damage at $400^\circ, 600^\circ,$ and 800°C , respectively (Ref. 8-5)

c. Boronated graphite

- (1) Physical criterion: limit on burnup and temperature
- (2) Exposure limit: not defined

2. Permanent structural components

a. Metal alloys

- (1) Physical criterion: $\geq 10\%$ residual ductility based on total elongation (Refs. 8-2, 8-6)
- (2) Exposure limit: 10^{21} n/cm^2 total fluence (Ref. 8-2)

b. Graphite (same as item 1b)

c. Boronated graphite (same as item 1c)

3. PCSB

a. PCRIV liner

- (1) Physical criterion: nil ductility temperature shift (NDTS) $\leq 47^{\circ}\text{C}$
- (2) Exposure limit: ~ 1 to 4×10^{19} total fluence by damage function analysis

b. PCRIV concrete

- (1) Physical criterion: bulk concrete temperature (30.5 cm or more from any liner surface) $\leq 68^{\circ}\text{C}$

c. PCRIV tendon lubricant

- (1) Physical criterion: lubricant adherence and corrosion protection
- (2) Exposure limit: 10^9 rad

An additional criterion for metal alloys operating at $\geq 550^{\circ}\text{C}$ is an allowable helium concentration of 1 appm (atomic) of helium (Ref. 8-7). The 1-appm helium limit is only for interim guideline use, and an assessment of available data is required to obtain a more reliable limit.

8.1.3. Exposure Margins

All radiation transport calculations include uncertainties in the computed flux values owing to

1. Analytical method approximations.
2. Geometric modeling simplifications.
3. Nuclear cross section uncertainties.

The state-of-the-art of transport sensitivity analysis techniques is not sufficiently advanced so that statistical uncertainties and confidence levels can be calculated. In some cases, uncertainties in raw nuclear data and processed multigroup cross sections are generally unknown for specific applications. For these reasons, the estimation of uncertainties in computed fluxes and spectra due to methods, modeling, and data must be based on good engineering judgment and experience. Quantification of total (combined) uncertainties requires shielding experiments which simulate the actual system as closely as possible.

Uncertainties in computed flux values due to cross section uncertainties increase with increasing penetration of the shield structures. Since the GCFR demonstration radial shielding attenuates the total neutron flux by about five orders of magnitude between the outside of the radial blanket and the PCRV liner, it is easy to understand how an error of a few percent in the cross sections is manifested as a substantial flux uncertainty at the PCRV liner.

To evaluate the radial shielding requirements at the core midplane level, the following uncertainties were assigned to the computed flux values:

Inner radial shields	= 50%,
Outer radial shield	= 100%,
PCSB	= 500%.

Therefore, the desired exposure margins are 0.5, 1.0, and 4.0 for the inner shields, outer shield, and PCSB, respectively, where margin is defined by

$$\text{Margin} = \left(\text{exposure limit} / \text{calculated exposure} \right) - 1$$

Thus, for a PCRV heating rate limit of 1 MW/cm^3 , the desired calculated heating rate is $\leq 0.2 \text{ MW/cm}^3$, giving the desired margin of $(1/0.2 - 1) = 4.0$.

8.1.4. Results

8.1.4.1. Neutron Exposure Levels and Margins. Tables 8-1 through 8-5 give radiation exposure and damage responses at the inner surfaces of the shield-reflector assemblies (cases 5 and 7 only), the first row of removable inner radial shielding, the second row of removable inner radial shielding, and the permanent outer radial shield and at the PCSB, respectively. Tables 8-6 through 8-9 give exposure margins at the inner surfaces of the first row of removable shielding, the second row of removable shielding, and the permanent outer radial shield and at the PCSB, respectively, based on the exposure criteria from Section 8.1.2.

Table 8-2 shows that the single row of shield reflector assemblies (cases 5 and 7) or a third row of blanket assemblies (case 6) reduces total flux and atom displacement rates by factors of about 3 to 5 compared with cases 1 through 4, which have only two rows of removable shield assemblies. Table 8-6 shows that in all cases the margin for graphite exposure exceeds the desired value of 0.5 for the minimum desired 3-yr assembly lifetime. However, an adequate margin of 0.5 for the 1-appm helium limit is only met in cases 3, 5, 6, and 7. Tables 8-3 and 8-7 present similar comparisons for exposure levels and margins for the first and second rows of removable shielding.

Tables 8-4 and 8-8 indicate that the permanent outer radial shield 10^{21} -n/cm² total fluence limit criterion is met only for case 6, and for case 6 the margin is only 0.04. The 2.5×10^{22} -n/cm² equivalent fission fluence for graphite damage (EFFGD) exposure criterion assuming 600°C graphite is met for all cases with margins far exceeding the desired margin of 1.0 for the 30 effective power year (EPY) outer radial shield lifetime. The 1-appm helium concentration limit is met at the outer radial shield with a margin greater than the desired margin of 1.0 for all cases except 1 and 4.

Tables 8-5 and 8-9 show that the 47°C NDTs criterion is met by all cases except 7 with a margin exceeding the desired margin of 4.0. The 1-mW/cm³ concrete heating limit is met with a margin exceeding 4.0 in all

TABLE 8-1
RADIATION EXPOSURE AND DAMAGE AT THE INNER SURFACE
OF THE SHIELD REFLECTOR ASSEMBLIES

Parameter	Case						
	1	2	3	4	5	6	7
Fluence ^(a) (n/cm ²)	NA ^(b)	NA	NA	NA		NA	
E > 0.1 MeV					7.18+22 ^(c)		8.29+22
2.38 eV < E < 0.1 MeV					1.60+23		1.69+23
E < 2.38 eV					<u>1.25+20</u>		<u>1.64+20</u>
Total					2.32+23		2.52+23
Lower-bound 5% RUE ^(d) fluence limit ^(e) (n/cm ²)	NA	NA	NA	NA	2.62+22	NA	2.51+22
Time to attain 5% RUE (EPY) ^(a)	NA	NA	NA	NA	3.39	NA	2.99
Stainless steel dpa ^(a,f)	NA	NA	NA	NA	40.2	NA	46.7
Graphite dpa ^(a)	NA	NA	NA	NA	NA	NA	NA
Graphite EFFGD ^(g)	NA	NA	NA	NA	NA	NA	NA

(a) For 30-EPY plant life at 0.8 capacity factor.

(b) NA = not applicable.

(c) 7.18+22 = 7.18×10^{22} .

(d) RUE = residual uniform elongation.

(e) Based on revised upper-bound effective damage function to attain 5% RUE in annealed 316 stainless steel irradiated and tested at 593°C (from Ref. 8-8).

(f) dpa = displacements per atom.

(g) EFFGD = equivalent fission fluence for graphite damage.

TABLE 8-2
RADIATION EXPOSURE AND DAMAGE AT THE INNER SURFACE
OF THE FIRST ROW OF INNER RADIAL SHIELDING

Parameter	Case						
	1	2	3	4	5	6	7
Fluence ^(a) (n/cm ²)							
E > 0.1 MeV	4.91+22 ^(b)	4.91+22	4.77+22	6.64+22	1.60+22	8.67+21	2.42+22
2.38 eV < E < 0.1 MeV	1.43+23	1.44+23	8.58+22	1.40+23	3.31+22	2.82+22	5.35+22
E < 2.38 eV	<u>1.00+22</u>	<u>8.06+21</u>	<u>1.47+17</u>	<u>1.54+20</u>	<u>8.18+19</u>	<u>6.02+17</u>	<u>3.59+20</u>
Total	2.02+23	2.01+23	1.33+23	2.06+23	4.91+22	3.64+22	7.81+22
Lower-bound 5% RUE fluence limit ^(c) (n/cm ²)	1.21+22	1.29+22	3.36+22	2.68+22	2.20+22	3.53+22	1.74+22
Time to attain 5% RUE (EPY) ^(a)	1.80	1.93	7.58	3.90	13.4	29.1	6.70
Stainless steel dpa ^(a)	23.6	23.6	22.6	29.5	7.77	4.95	11.5
Graphite dpa ^(a)	33.6	33.7	31.7	NA ^(f)	11.1	6.50	NA
Graphite EFFGD ^(d)	4.67+22	4.68+22	4.30+22	NA	1.54+22	9.03+21	NA
Helium concentration at 3 EPY (appm) ^(e)	7.08	6.04	0.52	1.14	0.26	0.14	0.63

(a) For 30-EPY plant life at 0.8 capacity factor.

(b) 4.91+22 = 4.91 x 10²².

(c) Based on revised upper-bound effective damage function to attain 5% RUE in annealed 316 stainless steel irradiated and tested at 593°C (from Ref. 8-8).

(d) EFFGD = equivalent fission fluence for graphite damage.

(e) appm = absolute parts per million (atomic).

(f) NA = not applicable.

TABLE 8-3
RADIATION EXPOSURE AND DAMAGE AT THE INNER SURFACE
OF THE SECOND ROW OF INNER RADIAL SHIELDING

Parameter	Case						
	1	2	3	4	5	6	7
Fluence ^(a) (n/cm ²)							
E > 0.1 MeV	4.42+21 ^(b)	7.46+21	7.70+21	2.03+22	2.10+21	1.23+21	7.21+21
2.38 eV < E < 0.1 MeV	3.85+22	5.49+22	1.53+22	4.51+22	5.65+21	3.78+21	1.62+22
E < 2.38 eV	<u>6.78+21</u>	<u>6.11+21</u>	<u>1.42+19</u>	<u>3.46+20</u>	<u>7.83+18</u>	<u>5.59+18</u>	<u>1.51+20</u>
Total	4.96+22	6.84+22	2.33+22	6.57+22	7.76+21	5.02+21	2.36+22
Lower-bound 5% RUE fluence limit ^(c) (n/cm ²)	6.32+21	7.69+21	2.20+22	1.68+22	2.35+22	1.97+22	1.75+22
Time to attain 5% RUE (EPY) ^(a)	3.82	3.37	28.3	7.67	90.7	118	22.3
Stainless steel (dpa) ^(a)	2.23	3.69	3.66	7.71	1.07	0.694	3.29
Graphite dpa ^(a)	NA ^(d)	NA	NA	NA	NA	NA	NA
Graphite EFFGD ^(e)	NA	NA	NA	NA	NA	NA	NA
Helium concentration at 6 EPY (appm)	8.03	7.69	0.22	1.42	0.071	0.054	0.42

(a) For 30-EPY plant life at 0.8 capacity factor.

(b) 4.42+21 = 4.42 x 10²¹.

(c) Based on revised upper-bound effective damage function to attain 5% RUE in annealed 316 stainless steel irradiated and tested at 593°C (from Ref. 8-8).

(d) NA = not applicable.

(e) EFFGD = equivalent fission fluence for graphite damage.

TABLE 8-4
RADIATION EXPOSURE AND DAMAGE AT THE INNER SURFACE
OF THE OUTER RADIAL SHIELD

Parameter	Case						
	1	2	3	4	5	6	7
Fluence ^(a) (n/cm ²)							
E > 0.1 MeV	7.83+20 ^(b)	1.71+21	1.83+21	4.13+21	4.68+20	2.89+20	1.52+21
2.38 eV < E < 0.1 MeV	2.65+21	3.89+21	2.97+21	8.02+21	1.11+21	6.67+20	3.38+21
E < 2.38 eV	<u>4.55+20</u>	<u>6.61+19</u>	<u>6.08+18</u>	<u>2.60+19</u>	<u>4.20+18</u>	<u>2.91+18</u>	<u>1.66+19</u>
Total	3.89+21	5.68+21	4.81+21	1.22+22	1.58+21	9.59+20	4.91+21
Lower-bound 5% RUE fluence limit ^(c) (n/cm ²)	7.82+21	1.72+22	2.44+22	2.63+22	2.00+22	1.85+22	2.22+22
Time to attain 5% RUE (EPY) ^(a)	60.3	91.0	152	64.7	379	579	136
Stainless steel dpa ^(a)	0.310	0.69	0.72	1.53	0.220	0.141	0.691
Graphite dpa ^(a)	0.505	1.10	1.15	2.58	0.136	0.0878	0.422
Graphite EFFGD ^(d)	7.01+20	1.53+21	1.60+21	3.58+21	1.89+20	1.22+20	5.86+20
Helium concentration at 30 EPY (appm)	3.54	0.12	0.24	0.75	0.22	0.065	0.30

(a) For 30-EPY plant life at 0.8 capacity factor.

(b) $7.83+20 = 7.83 \times 10^{20}$.

(c) Based on revised upper-bound effective damage function to attain 5% RUE in annealed 316 stainless steel irradiated and tested at 593°C (from Ref. 8-8).

(d) EFFGD = equivalent fission fluence for graphite damage.

TABLE 8-5
RADIATION EXPOSURE AND DAMAGE AT THE PCSB
(PCRV LINER AND PCRV)

Parameter	Case						
	1	2	3	4	5	6	7
Liner fluence (n/cm) ^(a)							
E > 0.1 MeV	4.88+17 ^(b)	5.35+17	5.72+17	2.82+17	6.81+17	6.46+17	1.44+18
2.38 eV < E < 0.1 MeV	2.00+18	1.57+18	1.64+18	2.10+18	4.57+18	3.08+18	1.37+19
E < 2.38 eV	<u>1.90+17</u>	<u>1.54+17</u>	<u>1.61+17</u>	<u>1.94+17</u>	<u>4.08+17</u>	<u>2.81+17</u>	<u>1.20+18</u>
Total	2.68+18	2.26+18	2.38+18	2.58+18	5.66+18	4.01+18	1.63+19
Lower-bound 47°C NDTs fluence limit ^(c) (n/cm ²)	2.30+19	1.77+19	1.74+19	3.67+19	3.37+19	2.57+19	4.39+19
Time to attain 47°C NDTs (EPY)	257	234	219	427	179	192	80.8
Liner steel dpa ^(a)	1.9-4	2.1-4	2.3-4	1.1-4	3.01-4	3.0-4	6.3-4
Maximum concrete gamma heating rate (MW/cm ³)	0.17	0.15	0.05	0.13	0.06	0.04	0.16
PCRV tendon lubricant gamma dose ^(a) (rad)	2.84+8	2.67+8	9.82+7	2.19+8	1.25+8	8.40+7	3.48+8
Time to attain tendon lubricant dose of 10 ⁹ rad (EPY)	106	112	306	137	240	357	86

(a) For 30-EPY plant life at 0.8 capacity factor.

(b) 4.88+17 = 4.88 x 10¹⁷.

(c) Based on upper-bound effective damage function to attain 75°C NDTs in A212-B steel irradiated and tested at less than 140°C (from Ref. 8-8).

TABLE 8-6
EXPOSURE MARGINS^(a) FOR FIRST ROW
OF REMOVABLE SHIELDING AT 3 EPY

Case	Exposure Criterion		
	5% RUE ^(b)	1-appm Helium Concentration in 316 Stainless Steel	EFFGD, H-451 ^(c) (1.4×10^{22} n/cm ²)
1	-0.40	-0.86	2.00
2	-0.36	-0.83	1.99
3	1.53	0.92	2.26
4	0.30	-0.12	NA ^(d)
5	3.47	2.85	8.09
6	8.70	6.14	14.5
7	1.23	0.59	NA

(a) Margin = (limit/response) - 1.

(b) Based on effective damage function of Ref. 8-8.

(c) Graphite at 800°C.

(d) NA = not applicable.

TABLE 8-7
EXPOSURE MARGINS^(a) FOR SECOND ROW OF REMOVABLE
SHIELDING AT 3 EPY

Case	Exposure Criterion		
	5% RUE ^(b)	1-appm Helium Concentration in 316 Stainless Steel	EFFGD, H-451 ^(c) (1.4×10^{22} n/cm ²)
1	-0.36	-0.88	NA ^(d)
2	-0.44	-0.87	NA
3	3.72	3.55	NA
4	0.28	-0.30	NA
5	14.1	13.1	NA
6	18.6	17.5	NA
7	2.71	1.38	NA

(a) Margin = (limit/response) - 1.

(b) Based on effective damage function of Ref. 8-8.

(c) Graphite at 800°C.

(d) NA = not applicable.

TABLE 8-8
EXPOSURE MARGINS^(a) FOR PERMANENT
OUTER RADIAL SHIELD AT 30 EPY

Case	Exposure Criterion		
	Total Fluence (10^{21} n/cm ²)	1-appm Helium Concentration in 316 Stainless Steel	EFFGD, H-451 ^(b) (2.5×10^{22} n/cm ²)
1	-0.74	-0.72	34.7
2	-0.82	7.33	15.3
3	-0.79	3.17	14.6
4	-0.92	0.33	5.98
5	-0.37	3.55	131
6	0.04	14.4	204
7	-0.80	2.33	41.7

(a) Margin = (limit/response) - 1.

(b) Graphite at 600°C.

TABLE 8-9
EXPOSURE MARGINS^(a) AT PRIMARY COOLANT
SYSTEM BOUNDARY FOR CASES 1 THROUGH 7

Case	Exposure Criterion		
	PCR/V Liner 47°C NDTs	Concrete Heating (1 mW/cm ³)	Tendon Lubricant Dose (10 ⁹ rad)
1	7.6	4.9	2.5
2	6.8	5.7	2.7
3	6.3	19.0	9.2
4	13.2	6.7	3.6
5	5.0	15.7	7.0
6	5.4	24.0	10.9
7	1.7	5.3	1.9

^(a) Margin = (limit/response) - 1.

cases. Only cases 3, 5, and 6 meet the 10^9 -rad tendon lubricant dose limit with a margin of at least 4.0. Note that the minimum margins are 2.5, 2.7, 6.3, 3.6, 5.0, 5.4, and 1.7 for cases 1 through 7, respectively, with either the PCRV liner NDTs criterion or the tendon dose criterion being limiting. A good approximation is that an additional 6-cm thickness of graphite in the outer shield increases the exposure margins for the liner embrittlement, concrete heating, and tendon dose criteria by 1.0. Therefore, the total shield thickness required to attain margins of 4.0 at the PCSB for each material arrangement of the seven configurations considered varies considerably. For example, the case 5 configuration would need to be over 25 cm thicker than the case 3 configuration to achieve margins of at least 4.0 at the PCSB.

8.1.4.2. Nuclear Heating. Figure 8-3 shows the nuclear heating distribution through removable radial shield rows 1 and 2, and Fig. 8-4 gives the nuclear heating distribution through the entire radial shield and into the PCRV concrete for case 6. Table 8-10 compares the maximum nuclear heating rates in the different materials in the inner and outer radial shielding, and Table 8-11 compares the total energy deposition rates (volume integrated) in the inner radial shield assemblies for the shield cases considered. Table 8-11 is especially interesting since a good approximation is that the shielding coolant film temperature rise is roughly proportional to the total energy deposition. It is significant that the energy deposition rate is at least 52% larger in the boronated graphite row 1 inner shield assemblies for case 3 than in the graphite or stainless steel row 1 inner radial shield assemblies for cases 1, 2, and 4; the nuclear heating in the boronated graphite is due to both gamma ray heating and the B-10 (η, α) reaction. However, the total energy deposition in a stainless steel row 2 shield assembly for case 3 is 20% to 50% of the row 2 assembly total energy deposition for cases 1, 2, and 4; this is because the boronated graphite, which sandwiches stainless steel row 2 in case 3, removes much of the lower energy flux and thus greatly reduces capture gamma ray production in the stainless steel row 2 assemblies. In addition, the total energy deposition rates in the row 1 and 2 inner radial shield assemblies for cases 5 and 6 are about one-third to one-fourth the energy deposition in the row 1 and 2

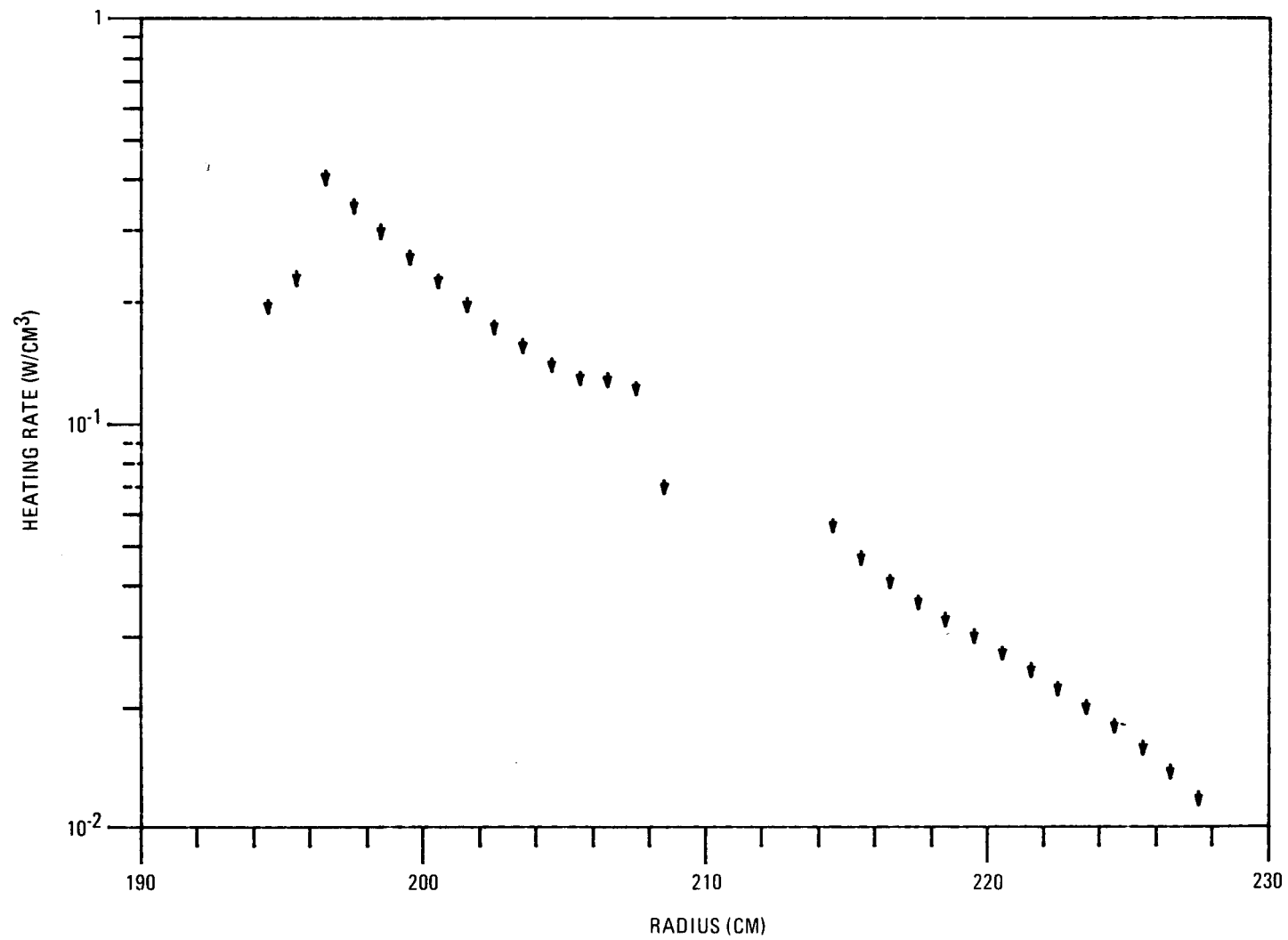


Fig. 8-3. Inner radial shield nuclear heating distribution for case 6

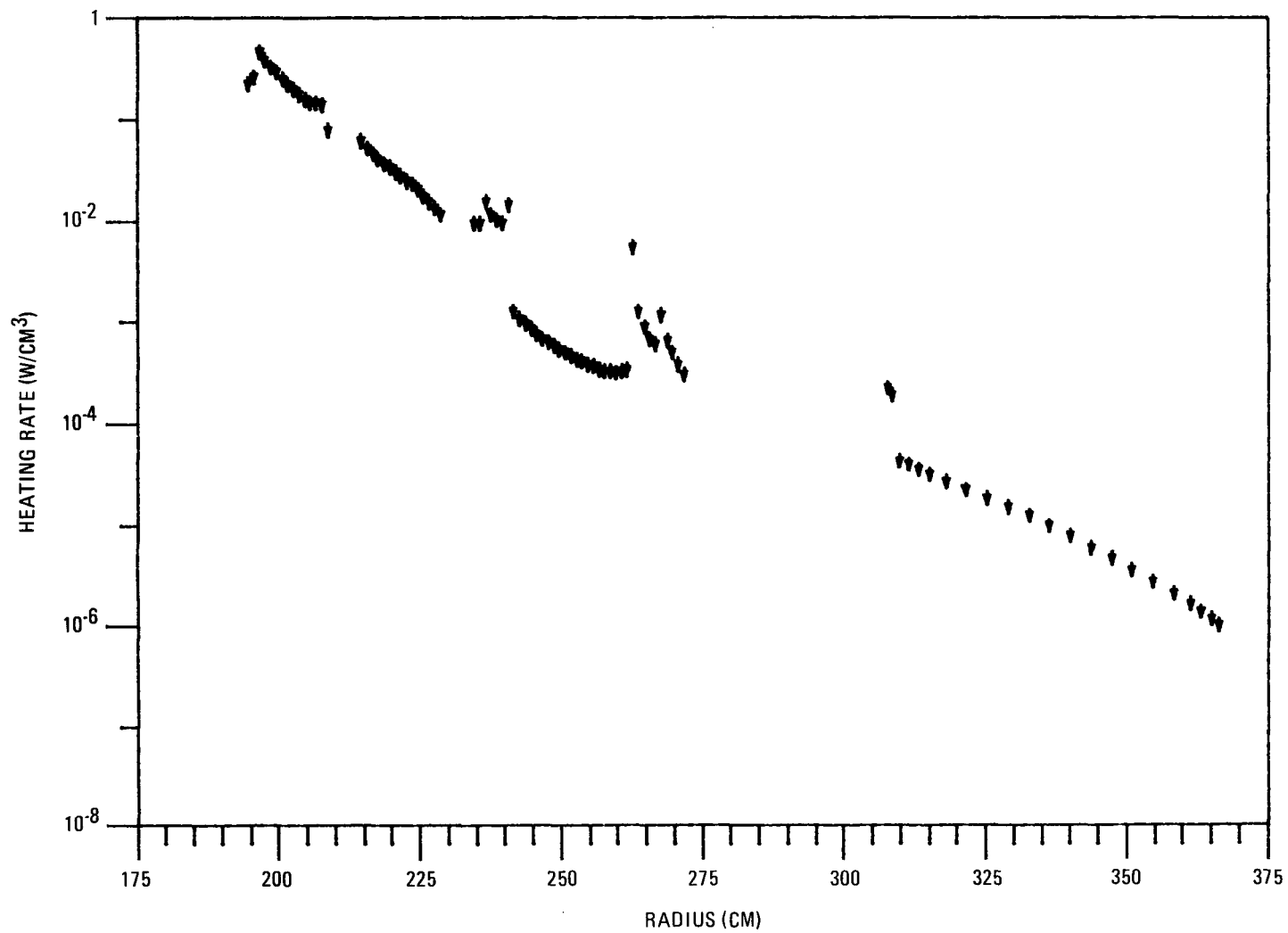


Fig. 8-4. Radial shield and PCRV nuclear heating distribution for case 6

TABLE 8-10
COMPARISON OF MAXIMUM NUCLEAR HEATING RATES ^(a) IN VARIOUS MATERIALS IN
INNER RADIAL SHIELD ASSEMBLIES FOR CASES 1 THROUGH 7

Case	Nuclear Heating Rate (W/cm ³)									
	Inner Radial Shield Row 1			Inner Radial Shield Row 2			Outer Radial Shield			PCRv
	316SS ^(b)	C	C + B ₄ C	316SS	C	C + B ₄ C	316SS	C	C + B ₄ C	4B Concrete
1	2.1	3.3-1 ^(c)	NA ^(d)	1.5	NA	NA	7.8-2	1.1-2	NA	1.7-4
2	2.0	3.2-1	NA	1.5	NA	NA	1.0-1	NA	1.4-1	1.6-4
3	8.8-1	NA	1.5	2.6-1	NA	NA	4.2-2	NA	7.1-2	5.3-5
4	1.2	NA	NA	5.3-1	NA	NA	1.1-1	NA	1.9-1	1.3-4
5	3.6-1	NA	5.9-1	8.7-2	NA	NA	1.3-2	1.7-3	2.1-2	5.7-5
6	2.2-1	NA	3.9-1	5.4-2	NA	NA	8.0-3	1.1-3	1.3-2	3.6-5
7	6.2-1	NA	NA	1.9-1	NA	NA	3.7-2	5.1-3	6.4-2	1.6-4

^(a) Core midplane level.

^(b) 316SS = 316 stainless steel.

^(c) 3.3-1 = 3.3×10^{-1} .

^(d) NA = not applicable.

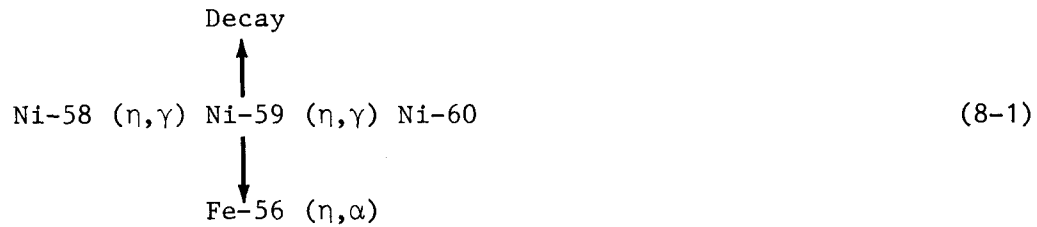
TABLE 8-11
COMPARISON OF TOTAL ENERGY DEPOSITION RATE IN INNER
RADIAL SHIELD ASSEMBLIES FOR CASES 1 THROUGH 7

Case	Relative Total Energy Deposition ^(a)	
	Row 1 Assembly	Row 2 Assembly
1	1.00	1.22
2	0.87	1.26
3	1.52	0.24
4	0.99	0.62
5	0.58	0.08
6	0.41	0.06
7	0.81	0.25

(a) Relative to a row 1 assembly for case 1.

assemblies for case 3 owing to attenuation in the reflector and third blanket rows for cases 5 and 6, respectively.

8.1.4.3. Helium Production. It is generally recognized that helium formed by various (n, α) reactions can severely limit the ductility of austenitic stainless steel at temperatures greater than 0.4 to 0.5 of the absolute melting temperature. Accelerator and reactor experiments have shown that the influence of helium can become significant once the concentration exceeds about 1 appm (Ref. 8-7). The major sources of helium generation in stainless steel are high-energy ($>1\text{MeV}$) threshold reactions, (n, α) reactions in impurities such as boron, and the two-stage nickel transmutation reaction:



The appropriate rate equations for the important helium-producing reactions can be combined and integrated over time to yield the following equation for evaluating helium concentrations:

$$\begin{aligned}
 \frac{N_{\text{He}}}{N_{\text{o}}^{\text{ss}}} = & \bar{\sigma}_t \psi t + \frac{N_{\text{o}}^{58}}{N_{\text{o}}^{\text{ss}}} \frac{\bar{\sigma}_{\alpha}^{59}}{\left(\bar{\sigma}_{\gamma}^{59} + \bar{\sigma}_{\gamma}^{59} \right) \left(\bar{\sigma}_{\gamma}^{59} + \bar{\sigma}_{\gamma}^{59} - \bar{\sigma}_{\gamma}^{58} \right)} \\
 & \cdot \left[\left(\sigma_{\gamma}^{59} + \sigma_{\alpha}^{59} \right) \left[1 - \exp \left(-\sigma_{\gamma}^{59} \psi t \right) \right] \right. \\
 & \left. - \sigma_{\gamma}^{58} \left[1 - \exp \left(\sigma_{\gamma}^{59} + \sigma_{\gamma}^{59} \psi t \right) \right] + \frac{B_{\text{o}}^{10}}{N_{\text{o}}^{\text{ss}}} \left[1 - \exp \left(-\sigma_{\gamma}^{10} \psi t \right) \right] \right] ,
 \end{aligned} \quad (8-2)$$

where

- N_o^{ss} = atom density of stainless steel (atoms/cm³),
- $\bar{\sigma}_t$ = composite spectral-averaged threshold helium production cross section (cm²),
- N_o^{58} = initial atom density of Ni-58,
- $\bar{\sigma}_\gamma^{58}$ = spectral-averaged Ni-58(η, γ)Ni-59 cross section,
- $\bar{\sigma}_\gamma^{59}$ = spectral-averaged Ni-59 (η, γ)Ni-60 cross section,
- $\bar{\sigma}_\alpha^{59}$ = spectral-averaged Ni-59(η, α)Fe-56 cross section,
- $\bar{\sigma}_\alpha^{10}$ = spectral-averaged B-10(η, α)Li cross section,
- ψ = neutron flux,
- t = irradiation time,
- N^{He}/N_o^{ss} = atoms helium/atoms stainless steel.

Considerable uncertainties persist in the energy-dependent (η, α) cross sections, especially the Ni-59 cross sections.

The cross sections used for this work were in 75-fine-group form and were normalized to agree with the empirical data available from thermal and fast spectra irradiations (Ref. 8-9). The one-group spectral-averaged cross sections for evaluating helium production in the GCFR radial shielding were obtained by the following procedure:

1. The 75-fine-group cross sections were collapsed to 26 groups with the appropriate GGC-5 (Ref. 8-10) fine-group spectra.
2. The 26-group cross sections were collapsed to one group by folding the cross sections with the 26-group fluxes at each space point of interest.

A computer program, HEPROD, was written to perform the burnup calculation with Eq. 8-2.

Table 8-12 gives the relative stainless steel helium concentrations at nominal exposure for row 1 and 2 removable inner radial shield assemblies at the assumed minimum desired lifetime of 3 EPY and at 30 EPY for the outer radial shield. Table 8-13 presents the calculated helium concentrations at various exposure times at the same locations. All calculations assumed a 5-appm B-10 impurity concentration in stainless steel. The largest helium concentration occurs for case 1, which contains no boronated graphite in the inner shields or in the inner part of the outer radial shield. Figure 8-5 is a plot for case 1 of the helium concentration at the inner surface of the inner radial shield as a function of exposure time. Note that the tentative 1-appm helium concentration limit is exceeded in about 3 months, and B-10 burns out rapidly (2.6 appm in the first year). Nickel initially contributes a small fraction to the total helium concentration, but is the primary contributor when sufficient Ni-59 is transmuted after about 5 yr of exposure. Figure 8-6 is a plot for case 1 of the helium concentration at the inner surface of the outer radial shield as a function of exposure time. Note that the 1-appm helium concentration limit is exceeded in about 8 yr with B-10 burnout dominating helium generation until over 100 yr of exposure. It is significant that the time to reach the helium concentration limit could be considerably extended by careful control of boron impurity in the steel melts.

Tables 8-12 and 8-13 show a considerable reduction in helium concentrations for cases 6 through 9 in comparison with case 1 for shield locations in close proximity to boronated graphite regions. This is because of the significant reduction of the thermal and epithermal fluxes, which are in the spectral range where the B-10 (η, α) and nickel cross sections are largest. For case 2, which has a 5-cm-thick boronated graphite region at the inner surface of the outer shield, the helium concentration is 0.3% of the helium concentration in case 1. The helium concentration of 0.12 appm at 30 EPY for the outer shield is well below the 1-appm limit. For case 3, which has a boronated graphite row 1 inner shield in addition to a boronated outer shield inner surface, the helium concentrations are 0.7% of those for case 1. Helium concentrations for case 4, which has two rows of stainless steel inner shield assembly, are still substantial, but only about 20% of those for case 1.

TABLE 8-12
COMPARISON OF STAINLESS STEEL HELIUM CONCENTRATIONS
FOR RADIAL SHIELD CASES 1 THROUGH 7

Radial Shield Case	Relative Helium Concentration ^(a) (%)		
	Row 1 ^(b)	Row 2 ^(b)	Outer Radial Shield ^(c)
1	100	100	100
2	85	94	0.3
3	0.7	0.2	0.7
4	16	16	21
5	0.3	0.1	0.6
6	0.2	0.1	0.2
7	0.9	0.5	0.8

(a) Relative to case 1 assuming nominal exposure values.

(b) Inner radial shield at 3 EPY.

(c) At 30 EPY.

TABLE 8-13
HELIUM CONCENTRATIONS IN STAINLESS STEEL AT VARIOUS TIMES FOR RADIAL SHIELD CASES 1 THROUGH 7

Case	Helium Concentration (appm)									
	Row 1 (a)				Row 2 (a)			Outer Radial Shield		
	3 EPY (b)	6 EPY (b)	9 EPY (b)	12 EPY (b)	3 EPY (b)	6 EPY (b)	9 EPY (b)	12 EPY (b)	30 EPY (b)	60 EPY (b)
1	7.08	14.6	25.4	39.4	4.45	8.03	12.4	17.9	3.54	5.81
2	6.04	12.0	20.1	30.5	4.17	7.69	12.0	17.6	0.12	0.32
3	0.52	1.02	1.50	1.96	0.11	0.22	0.33	0.44	0.24	0.50
4	1.14	2.41	3.80	5.32	0.70	1.42	2.16	2.93	0.75	1.54
5	0.26	0.53	0.81	1.09	0.035	0.071	0.11	0.14	0.22	0.44
6	0.14	0.28	0.42	0.57	0.027	0.054	0.082	0.11	0.065	0.13
7	0.63	1.29	1.97	2.68	0.21	0.42	0.63	0.84	0.30	0.61

(a) Inner radial shield.

(b) At 0.8 capacity factor.

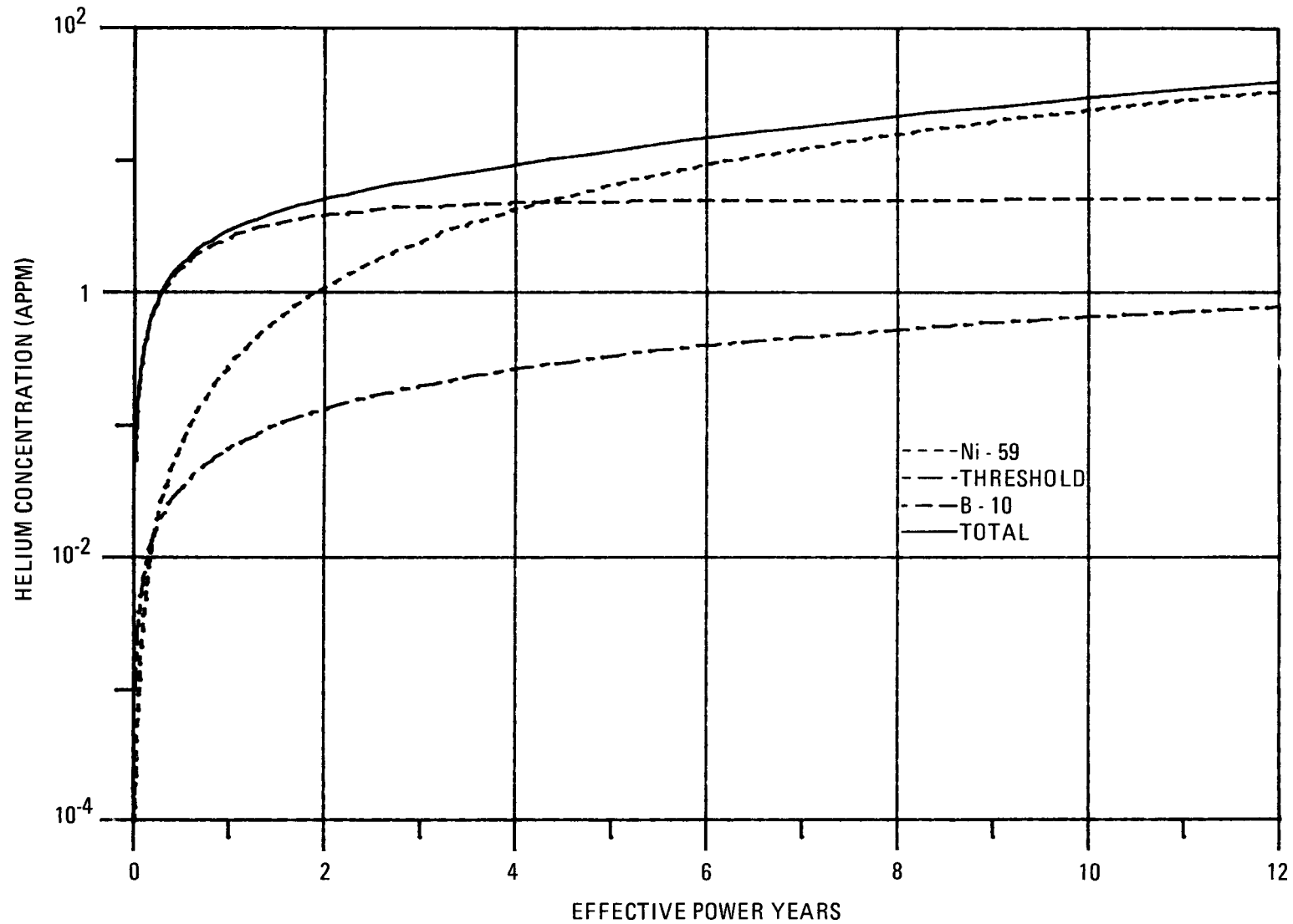


Fig. 8-5. Stainless steel helium concentration vs EPY at inner surface of first row of inner radial shield assemblies for case 1

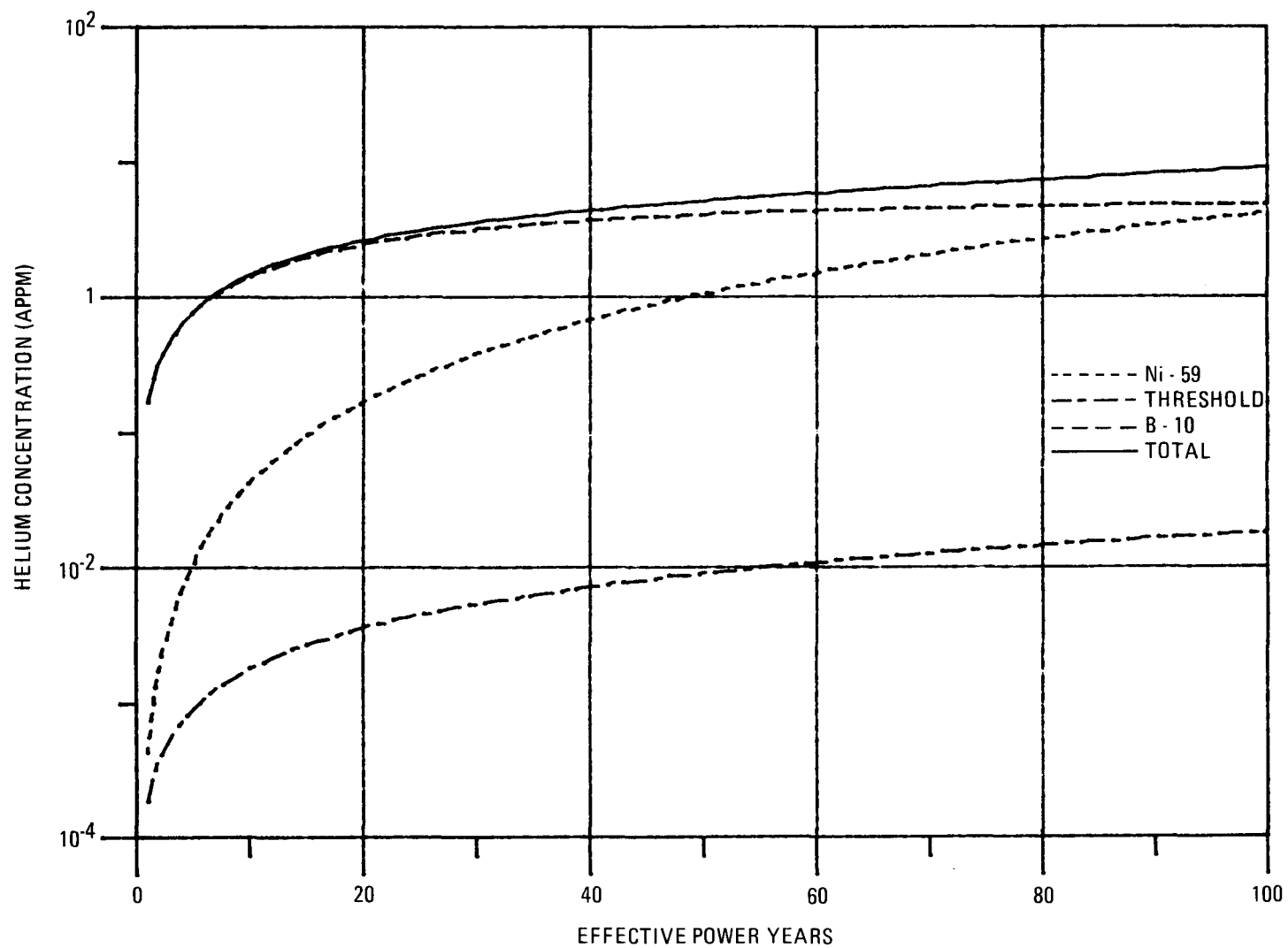


Fig. 8-6. Stainless steel helium concentration vs EPY at inner surface of outer radial shield for case 1

With a third row of reflector or blanket assemblies (cases 5 through 7), helium concentrations are further reduced in the inner shield assemblies and the outer radial shield. The lowest helium concentrations were calculated for case 6, which has a third row of radial blanket assemblies, a boronated row 1 inner radial shield, and a boronated outer radial shield inner surface. Figure 8-7 shows helium concentration at the inner surface of the row 1 inner radial shield assemblies as a function of exposure time for case 6. Note that B-10 burnout dominates helium production, contributing about 80% of the total helium concentration. Figure 8-8 shows helium concentration at the inner surface of the outer radial shield as a function of exposure time for case 6. Again, B-10 dominates helium production, contributing about 95% of the total helium concentration. For case 6, nominal helium concentrations at 3 EPY are 0.14 and 0.027 appm for row 1 and row 2 of the inner shield, respectively, and 0.065 appm for the outer shield at 30 EPY; all concentrations are well within the 1-appm helium concentration limit.

8.1.5. Summary and Recommendations

One-dimensional transport analyses were performed for the seven shield configurations shown in Fig. 8-1. The case 1 configuration is identical to the current reference shield design (i.e., canned graphite row 1 removable inner shield, solid stainless steel row 2 removable inner shield, and laminated graphite, boronated graphite, stainless steel permanent outer radial shield), except that the total thickness of the outer radial shield is increased to allow for lower PCSB radiation exposure and heating limits and to increase exposure margins at the PCSB. The total shield envelope (outer radius) is the same for all configurations to enable direct comparison of shielding effectiveness and performance. The alternative configurations (cases 2 through 7) generally differ from configuration 1 by the replacement of some graphite with either boronated graphite or stainless steel. Cases 5 through 7 have reduced-thickness outer shields and include either a reflector or third blanket row suspended on the grid plate and cooled with core inlet helium.

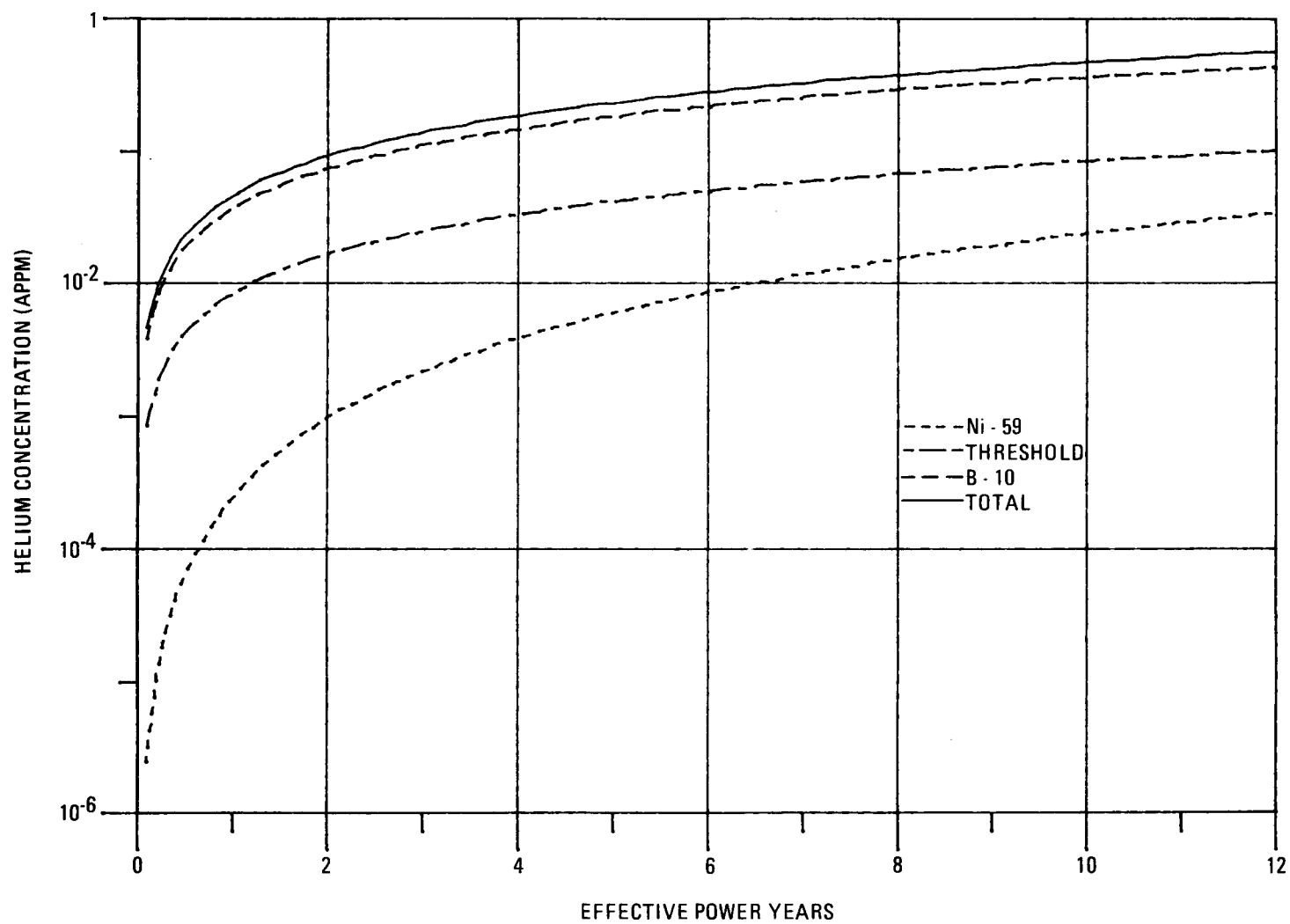


Fig. 8-7. Stainless steel helium concentration vs EPY at inner surface of first row of inner radial shield assemblies for case 6

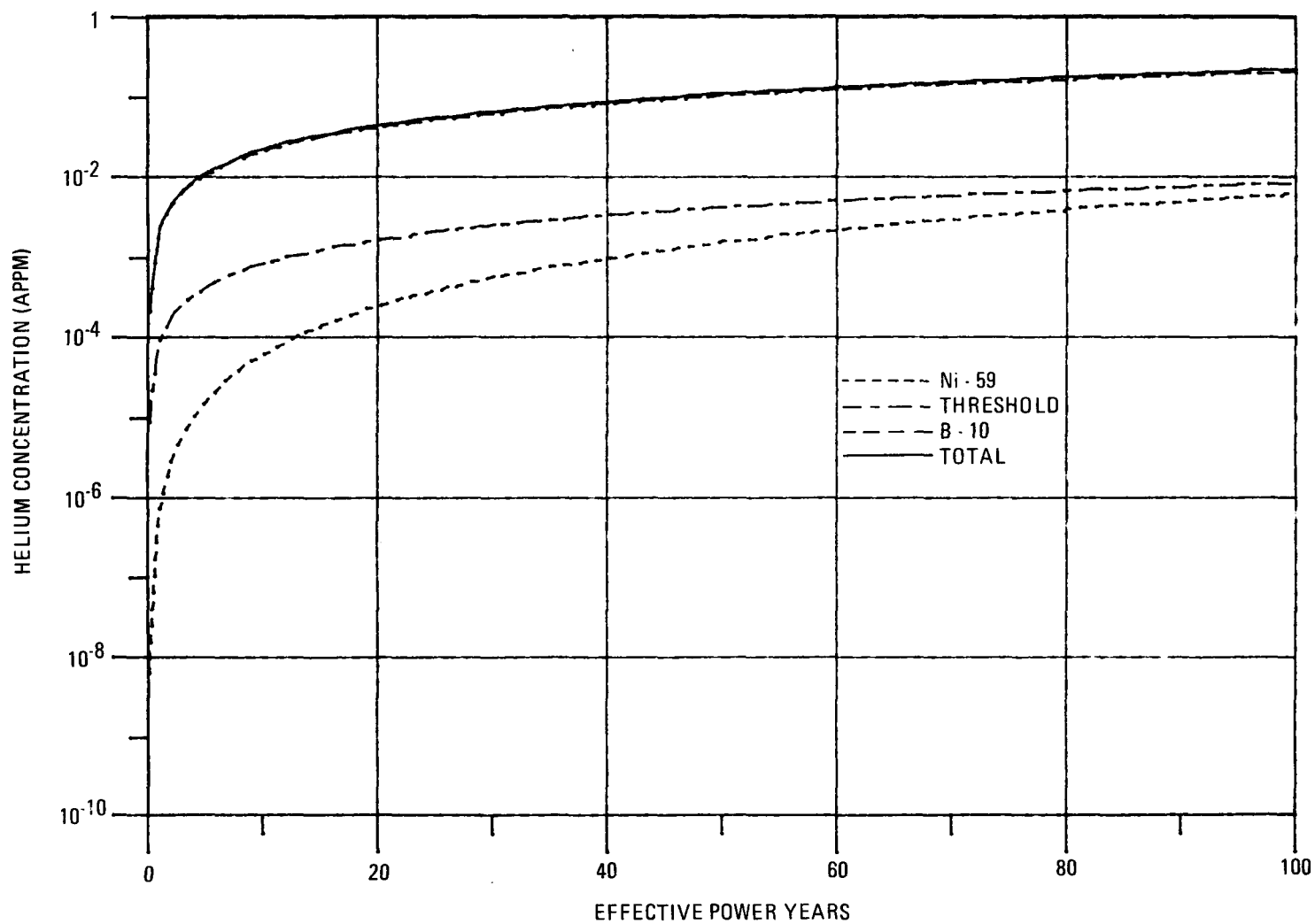


Fig. 8-8. Stainless steel helium concentration vs EPY at inner surface of outer radial shield for case 6

The radiation exposure criteria applied to the shielding structures were

1. An allowable 1-ppm helium gas concentration in the structural steel.
2. $\leq 10^{21}$ total fluence exposure to the outer radial shield.
3. Assurance of no graphite swelling.

The exposure criteria applied to the PCSB were

1. PCRV liner NDTs $\leq 47^\circ\text{C}$.
2. PCRV concrete heating $\leq 1 \text{ MW/cm}^3$.
3. PCRV tendon lubricant dose $\leq 10^9$ rad.

Margins of 0.5, 1.0, and 4.0 were considered necessary to account for uncertainties in radiation exposure to the inner shields, outer shield, and PCSB, respectively.

The key conclusions of this study are as follows:

1. The 10^{21} n/cm^2 total fluence limit to the outer radial shield structural steel can only be met with the required margin of 1.0 when shield configurations 5 or 6 are used with a radial blanket management scheme employing out-in blanket assembly shuffling.
2. Configurations 3, 5, and 6 are the most effective material arrangements for minimizing total shield thickness.
3. For configurations 5 and 6, nuclear heating rates in the row 1 inner radial shield assemblies are factors of 2 to 4 less than those for the other configurations. Nuclear heating rates in the row 2 inner radial shield assemblies for configurations 5 and 6 are factors of 3 to 15 less than those for the other configurations.

4. Only configurations 3, 5, and 6 enable the 1-appm helium concentration limit to be met for the inner shield assemblies with the required margin of 0.5 at 3 EPY.
5. Only configurations 1 and 4 do not enable the 1-appm helium concentrations limit to be met at the outer radial shield with a margin of 1.0 at 30 PEY.
6. Shield configuration 6 represents the overall best material arrangement in terms of (a) lowest nuclear heating rates in the radial shielding; (b) lowest helium generation rates in the radial shielding; (c) lowest fluence exposure to the outer radial shield; and (d) minimization of the total shield thickness required to protect the PCSB.

Based on the above conclusions, it is recommended that the material arrangement of configuration 6 be adopted for the revised radial shielding with the following contingencies:

1. The exposure criteria must remain the same.
2. The performance of the boronated graphite shielding in high-flux locations must be demonstrated.

This recommendation is limited to inner radial shield assemblies extending from about the top of the upper axial blanket to the bottom of the lower axial blanket. A decision has not yet been made as to whether the radial shield assemblies will be extended to the bottom of the lower plenum and/or whether shielding will be incorporated in the lower portion of the fuel assemblies (Ref. 8-2). It is further recommended that the revised configuration incorporate the flexibility of allowing the outer row of grid-plate-suspended assemblies to be either blanket or reflector assemblies. This flexibility could be retained even during plant operation. Note in

Fig. 8-1 that 70% stainless steel/30% helium reflector assemblies were assumed. Additional physics and thermal-mechanical analysis is required to determine the volume fractions and materials for the reflector assemblies.

8.2. CONCEPTUAL SHIELDING CONFIGURATION I

Figure 8-9 shows a full RZ model of a down-flow demonstration plant shielding system and the reactor internal components. This model, designated conceptual shielding configuration I, has been sent to ORNL for two-dimensional discrete ordinates analysis. The model extends axially from a distance into the concrete above the top closure plug shield to a distance into the concrete below the core catcher and extends radially past the PCRV liner, incorporating part of the helium cross ducts leading to the circulators. The model integrates the following design characteristics:

1. The core and blankets are shielding core model A (Ref. 8-2), except that a third row of radial blanket assemblies (assumed to be BOL) has been added.
2. Two rows of axially segmented removable inner radial shield assemblies are suspended on the outer radial shield and extend to the bottom of the outer radial shield. The first row consists of stainless steel canned boronated graphite assemblies, and the second row comprises solid stainless steel assemblies (see Section 8.1).
3. The lower axial wraparound shield is raised above a uranium metal bath core catcher and includes a lip extending radially inward which is intended to reduce streaming under the outer radial shield of neutrons and gamma rays from the core catcher region.
4. Boronated graphite is added to some regions of the upper axial shielding to reduce thermal flux transport to the PCRV in the upper plenum.

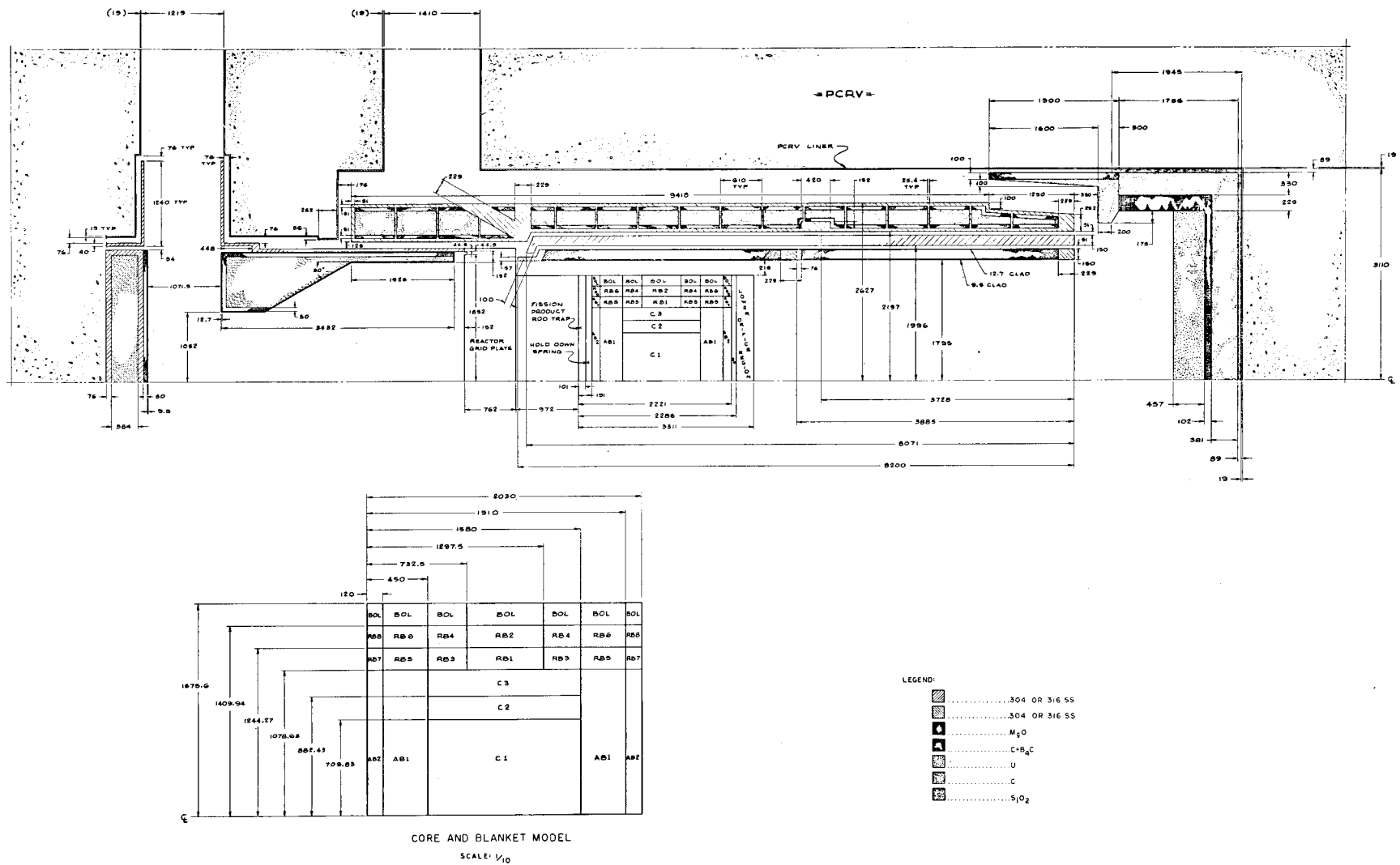


Fig. 8-9. Model of down-flow demonstration plant shielding system and reactor internal components

8.3. 300-MW(e) GCFR LOWER PLENUM STREAMING

During the last quarter an effort was initiated to study a method for reducing neutron streaming in the lower plenum of the 300-MW(e) GCFR. This method is placement of B_4C near the interior radius of the outer radial shield for its entire length below the lower axial blanket. The idea of using B_4C with a minimum stainless steel cover on the inside surface of the radial shield is to reduce the flux of neutrons backscattered into the plenum from the shield. If effective, the neutron flux will be significantly reduced at the top of the core catcher and at the PCRV liner above the wraparound shield.

During this quarter, calculations were performed with a two-dimensional (RZ) model of the lower plenum (Fig. 8-10). The calculations were performed with the GA version of the DOT 3.5 code (Ref. 8-11) with P_3 angular scattering and S_8 angular quadratures. A blanket spectrum surface source was used at the lower level of the axial blanket at $Z = 0$ in Fig. 8-10. The inner surface of the radial shield is 5.61 m below the axial blanket, with a 12.7-mm cover of stainless steel over either $B_4C + C$ (problem 1) or graphite (problem 2). Vacuum boundary conditions were assumed on the right and bottom boundaries and reflective conditions on the Z axis.

Isoflux graphs of the results are shown in Figs. 8-11 through 8-20. Figures 8-11 through 8-15 present the results from problem 1 with $B_4C + C$; Figs. 8-16 through 8-20 present the results from problem 2 with graphite. For each problem, isoflux plots are given for thermal flux, $E < 2.38$ eV (Figs. 8-11 and 8-16); epithermal flux, $0.12 \text{ MeV} > E > 2.38 \text{ eV}$ (Figs. 8-12 and 8-17); second fast flux, $0.91 \text{ MeV} > E > 0.12 \text{ MeV}$ (Figs. 8-13 and 8-18); first fast flux, $E > 0.91 \text{ MeV}$ (Figs. 8-14 and 8-19); and total flux (Figs. 8-15 and 8-20).

As expected, the effect of B_4C is greatest for thermal flux, as seen by comparing the magnitudes of the fluxes in the lower right streaming path in Figs. 8-11 and 8-16. This order of magnitude decrease in the thermal flux in problem 1 compared with that in problem 2 is clearly

Fig. 8-10. Model of lower plenum for streaming calculations

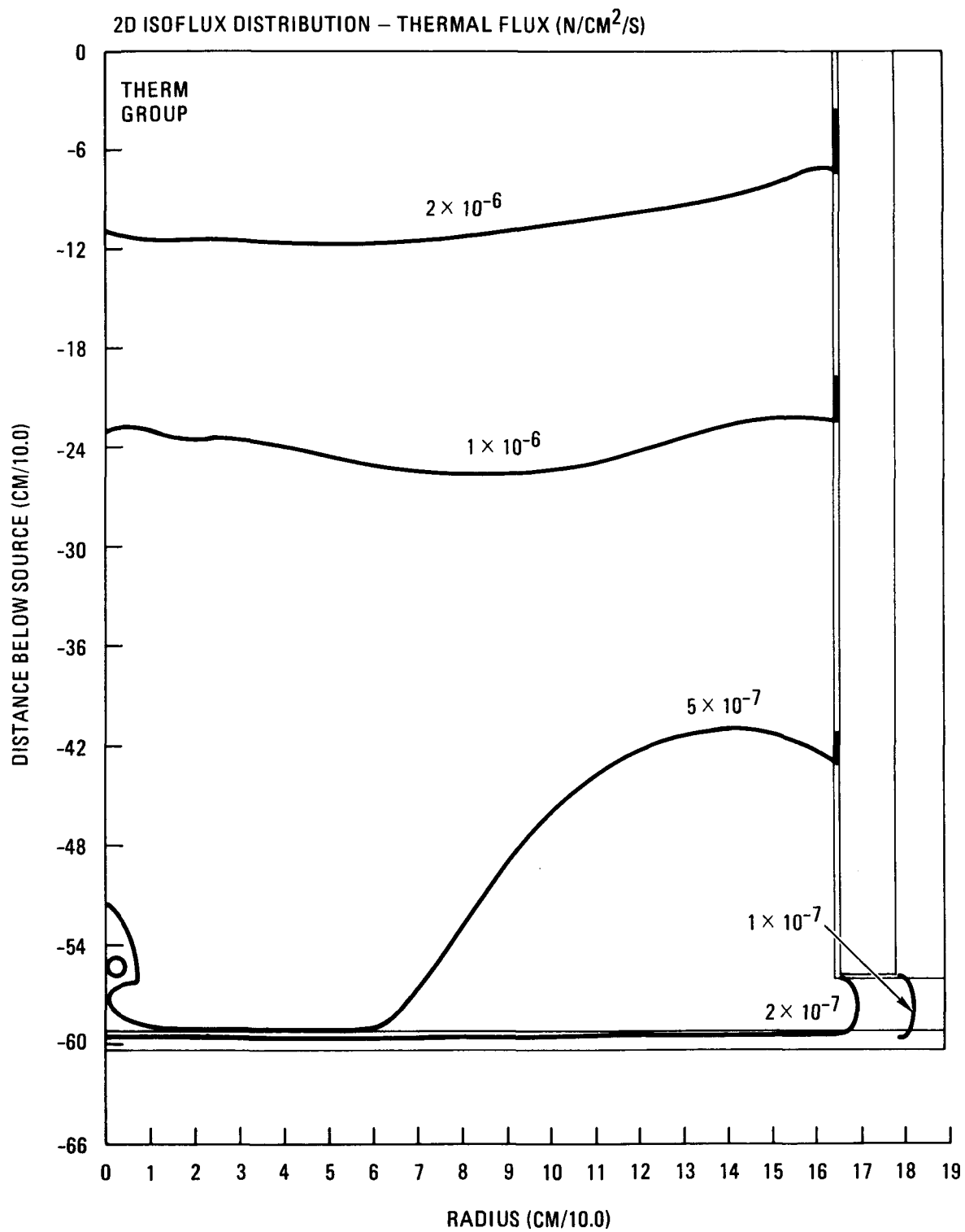


Fig. 8-11. Lower plenum streaming, problem No. 1 ($B_4C + C$); isoflux graph for normalized thermal flux, $E < 2.38$ eV (isoflux points with $B_4C + C$ are off scale)

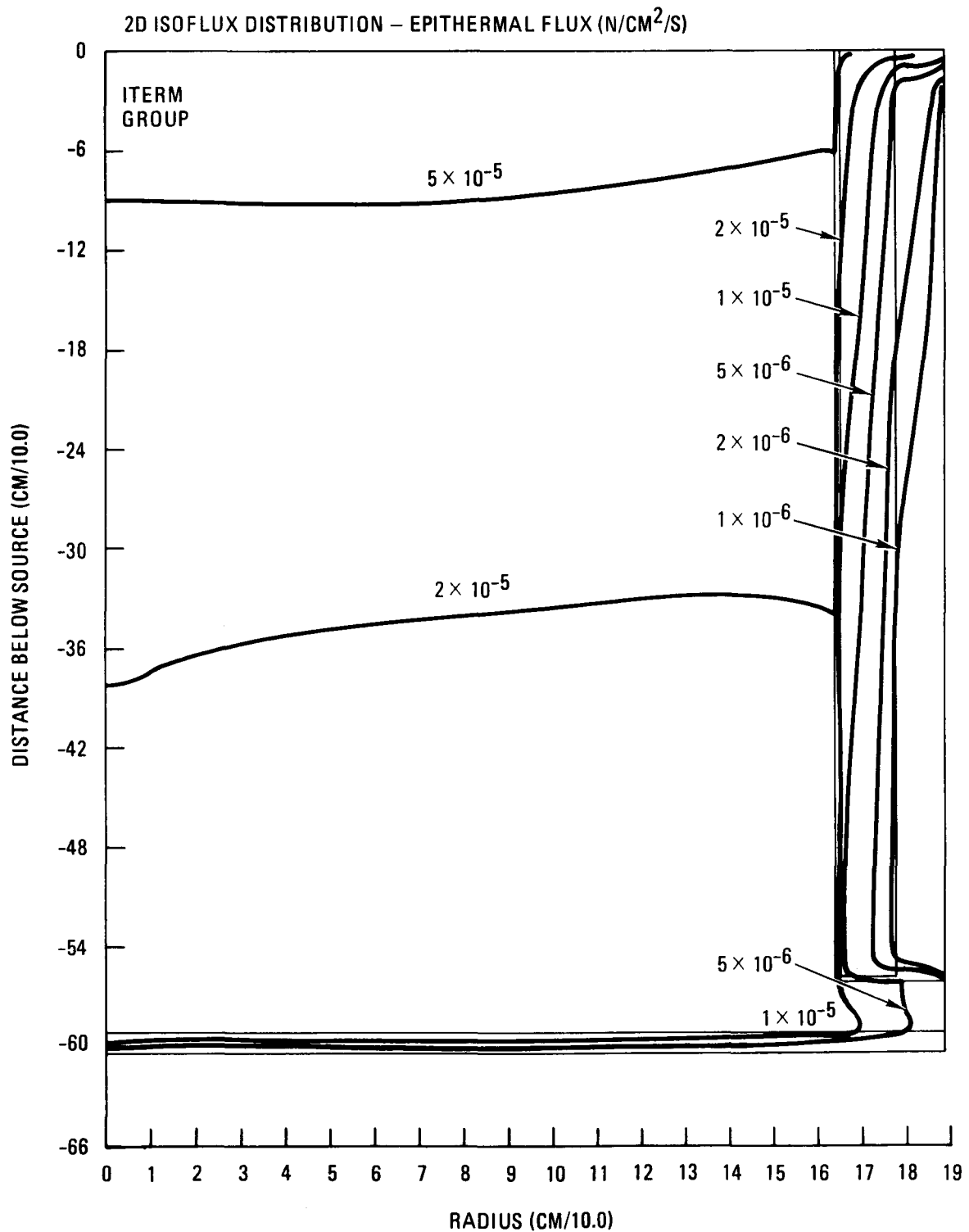


Fig. 8-12. Lower plenum streaming, problem No. 1 ($B_4C + C$); isoflux graph for normalized epithermal neutron flux, $0.12 \text{ MeV} > E > 2.38 \text{ eV}$

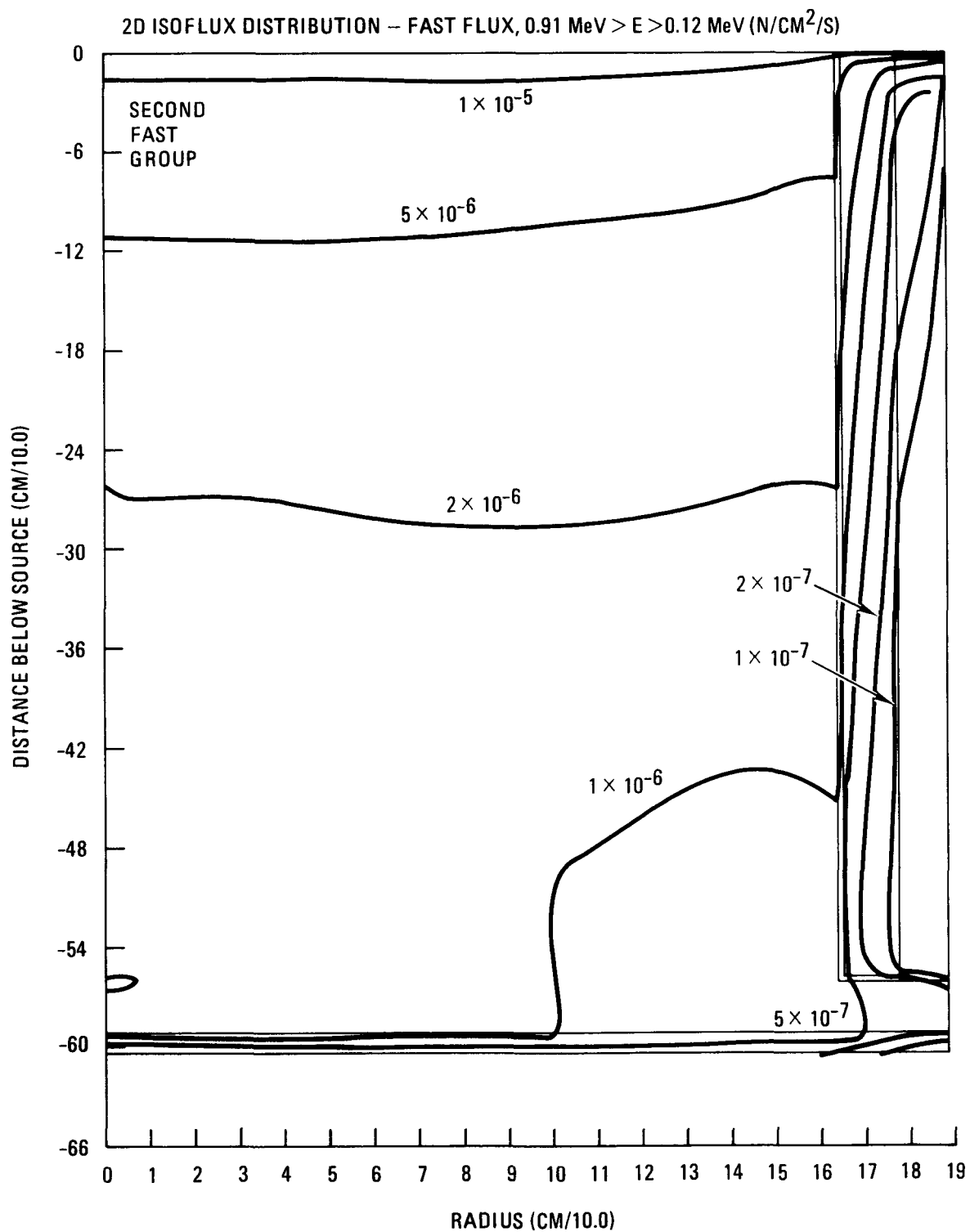


Fig. 8-13. Lower plenum streaming, problem No. 1 ($\text{B}_4\text{C} + \text{C}$); isoflux graph for normalized fast flux, $0.91 \text{ MeV} > E > 0.12 \text{ MeV}$

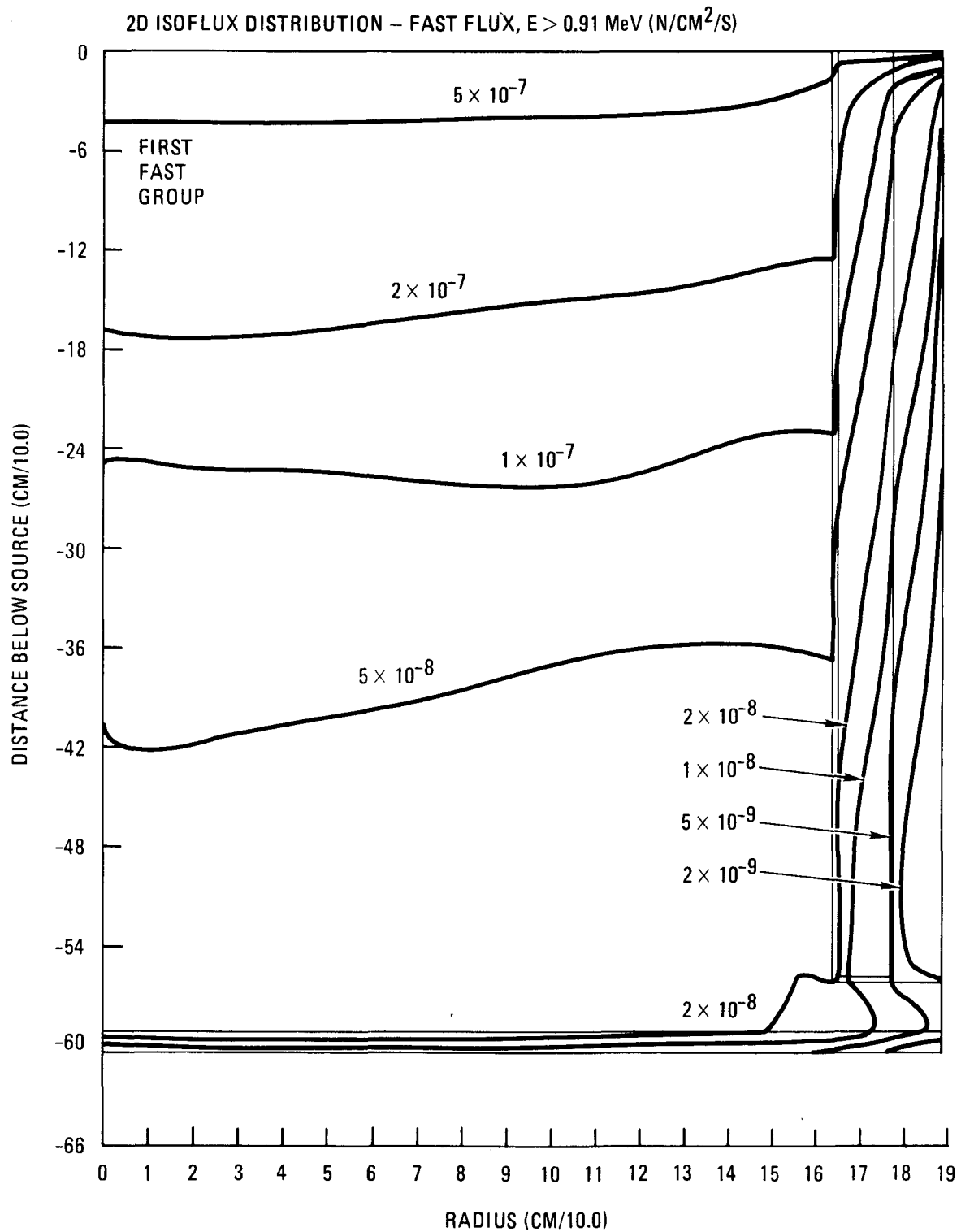


Fig. 8-14. Lower plenum streaming, problem No. 1 ($B_4C + C$); isoflux graph for normalized fast flux, $E > 0.91$ MeV

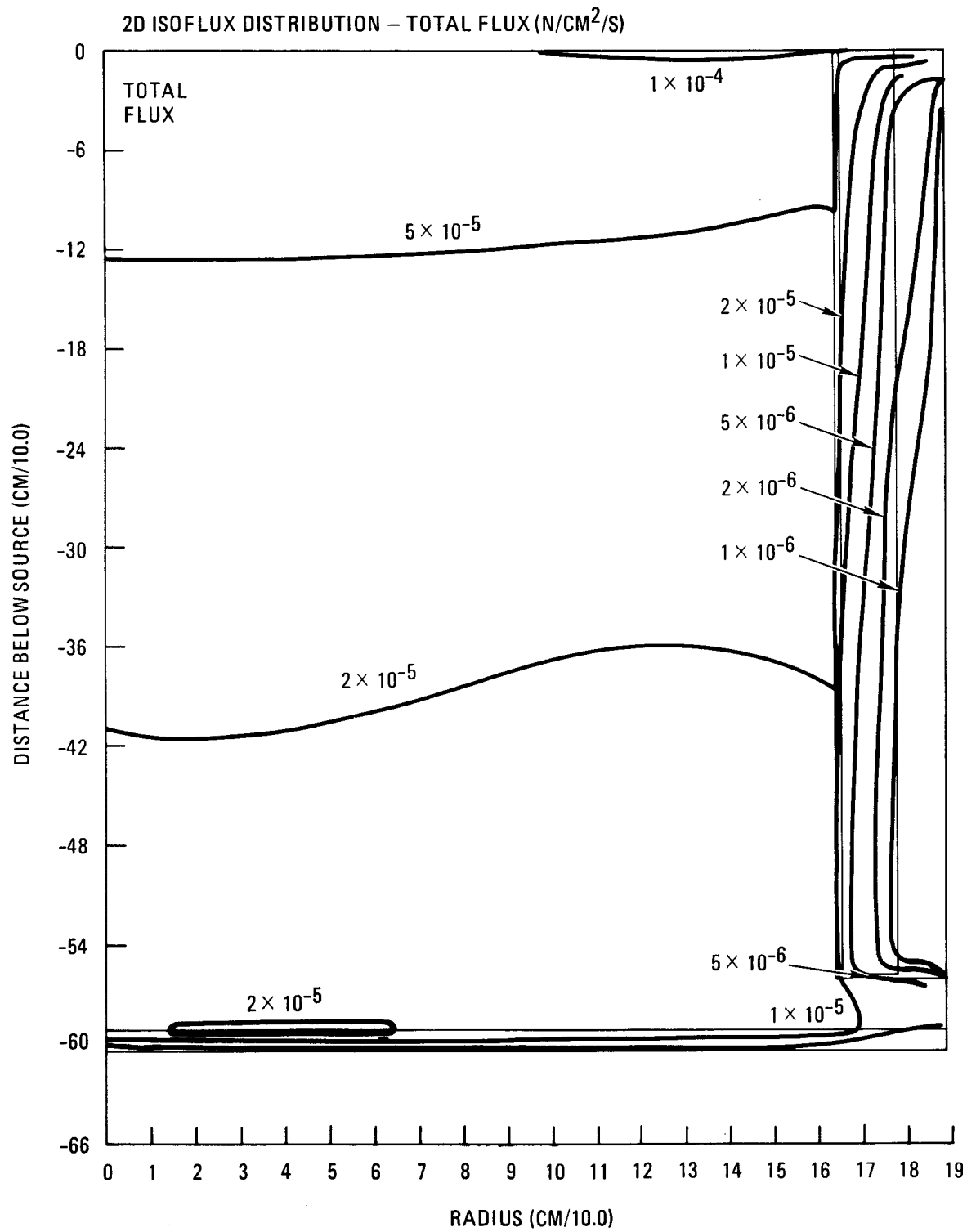


Fig. 8-15. Lower plenum streaming, problem No. 1 ($B_4C + C$); isoflux graph for normalized total neutron flux

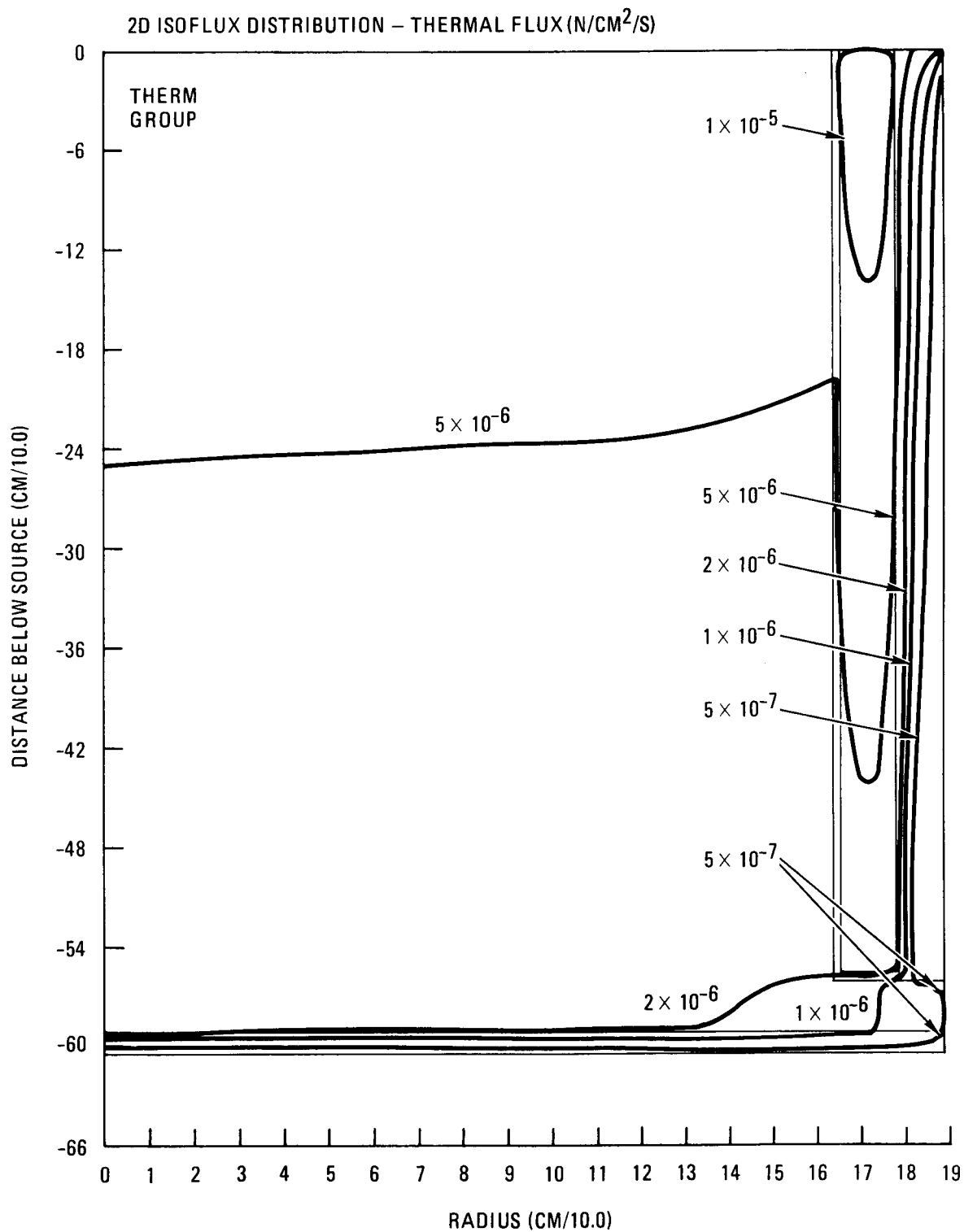


Fig. 8-16. Lower plenum streaming, problem No. 2 (graphite); isoflux graph for normalized thermal flux, $E < 2.38$ eV

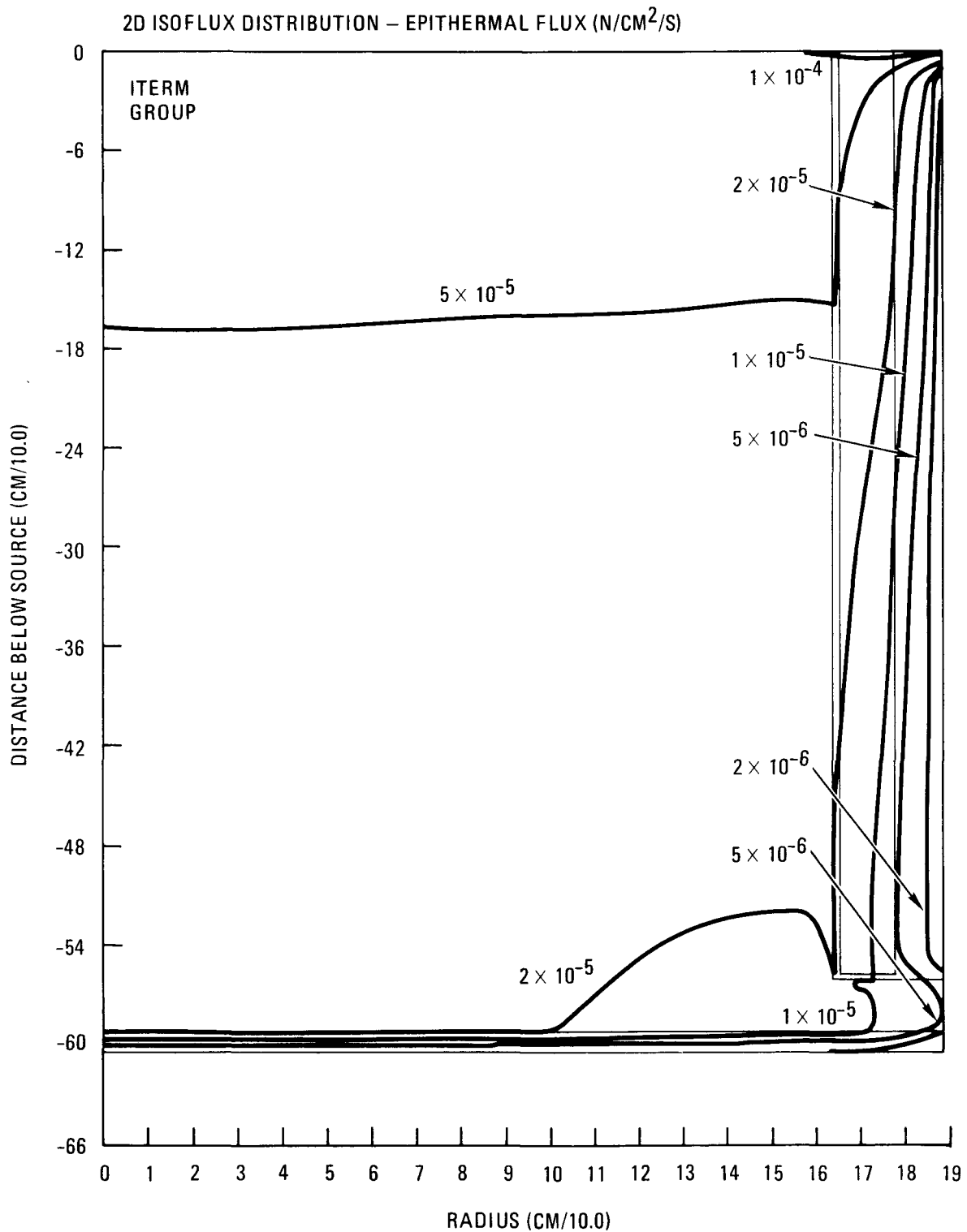


Fig. 8-17. Lower plenum streaming, problem No. 2 (graphite); isoflux graph for normalized epithermal neutron flux, $0.12 \text{ MeV} > E > 2.38 \text{ eV}$

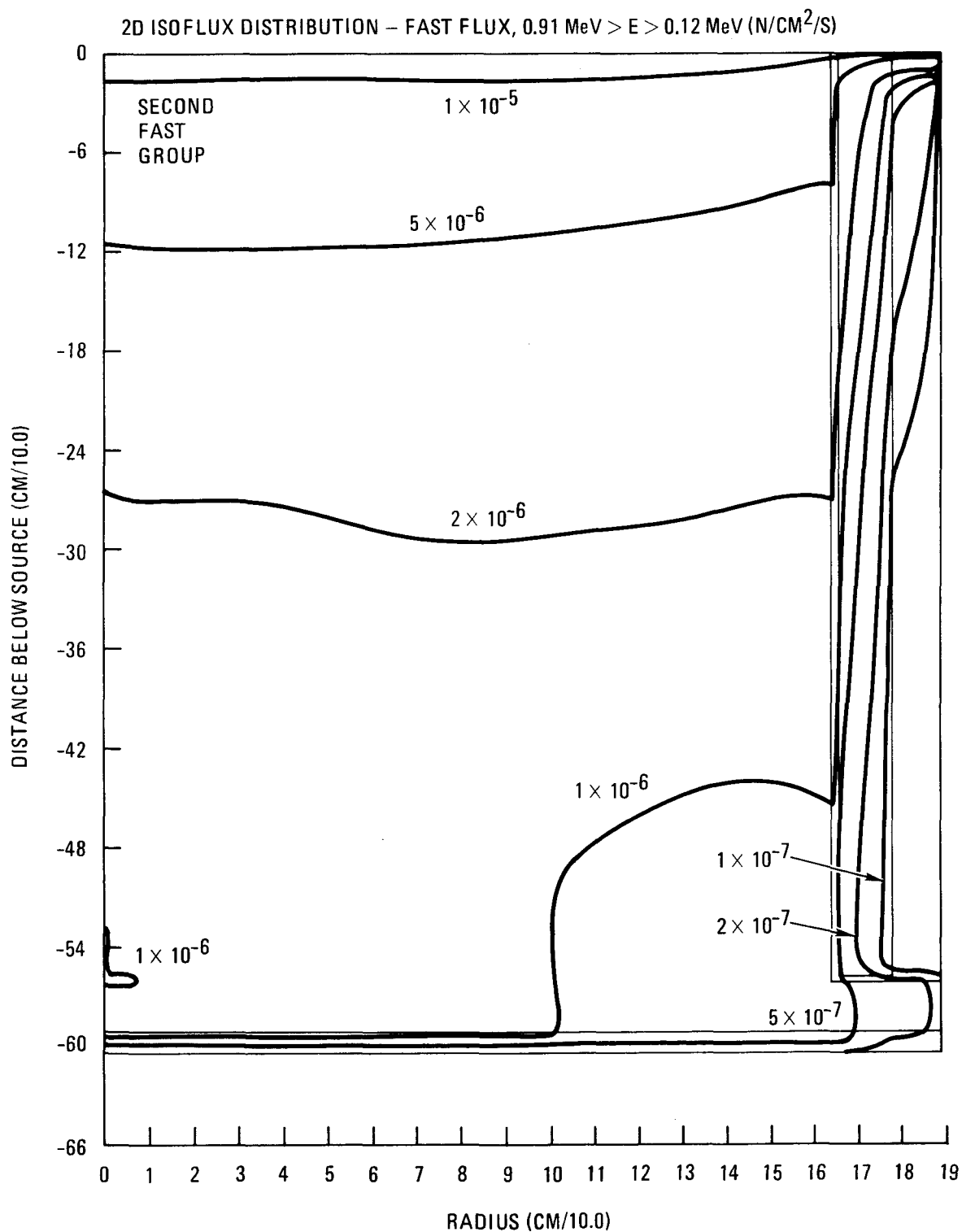


Fig. 8-18. Lower plenum streaming, problem No. 2 (graphite); isoflux graph for normalized fast flux, $0.91 \text{ MeV} > E > 0.12 \text{ MeV}$

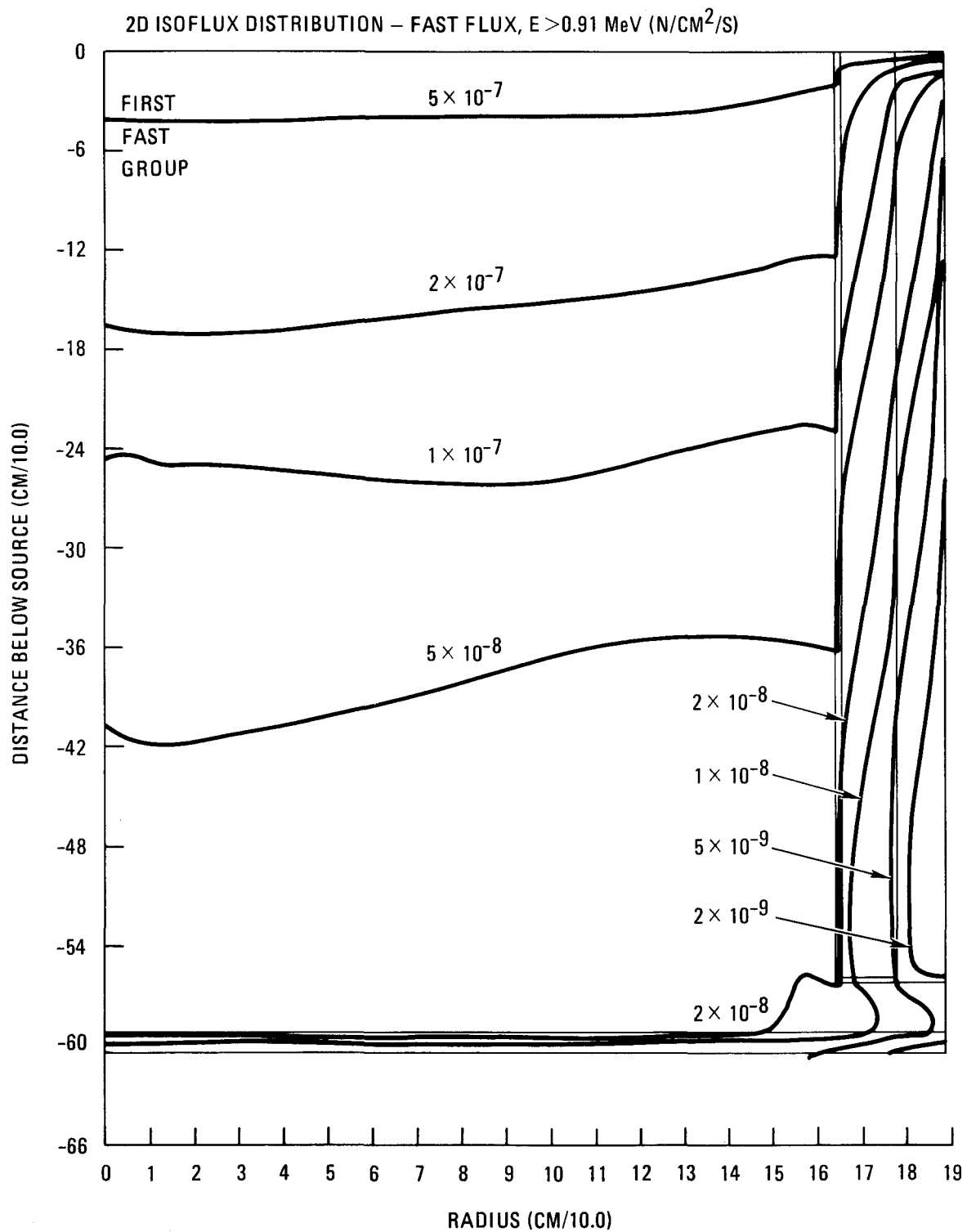


Fig. 8-19. Lower plenum streaming, problem No. 2 (graphite); isoflux graph for normalized fast flux $E > 0.91$ MeV

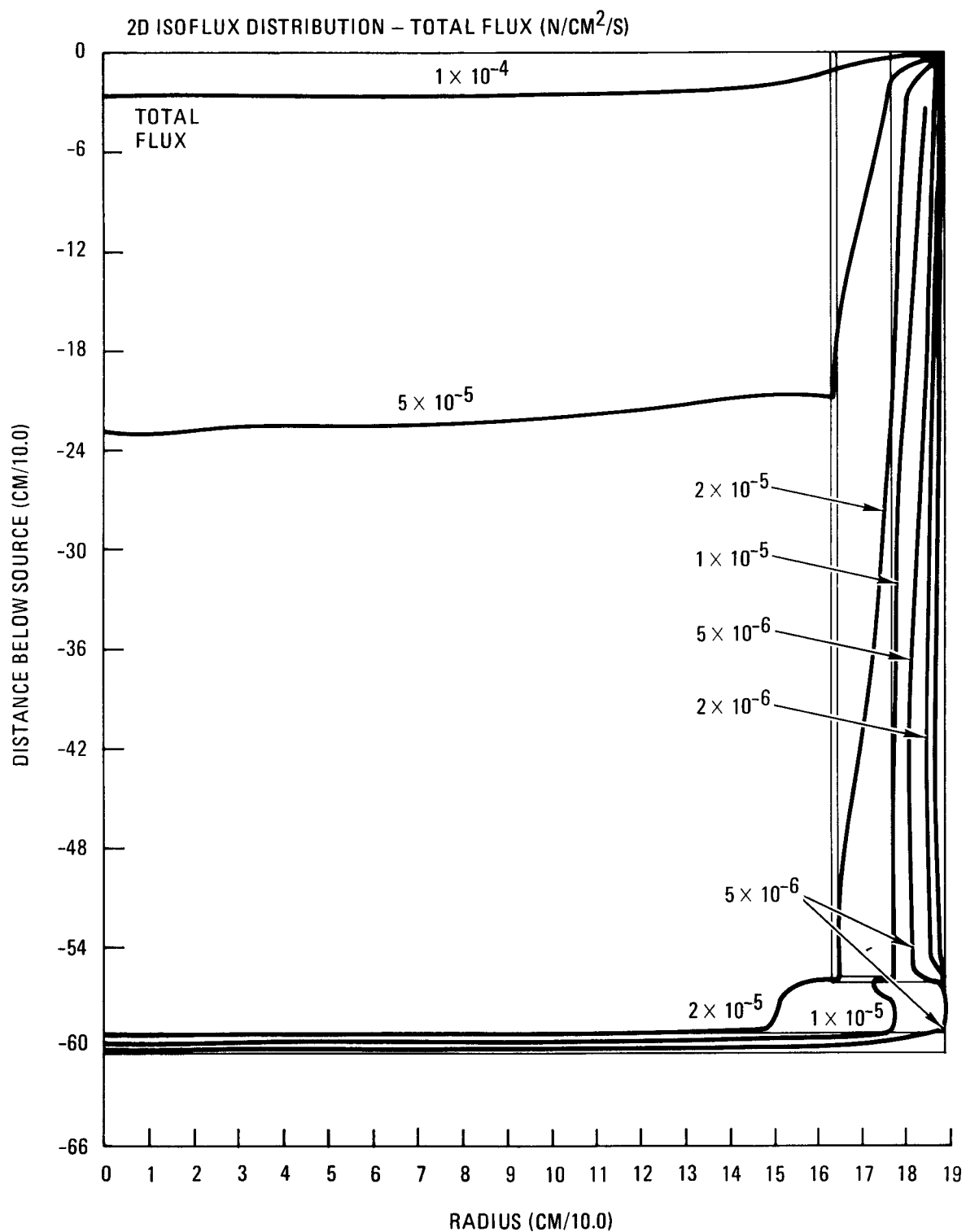


Fig. 8-20. Lower plenum streaming, problem No. 2 (graphite); isoflux graph for normalized total neutron flux

due to absorption of neutrons in the $B_4C + C$ in problem 1 which would otherwise be backscattered into the plenum. The other neutron group significantly affected is the epithermal group of neutrons ($0.12 \text{ MeV} > E > 2.38 \text{ eV}$). A decrease of about a factor of two is noted in Figs. 8-12 and 8-17 at the lower end of the $B_4C + C$ region. (In the GCFR blanket spectrum, the epithermal neutron group also has the most flux.) As expected, both fast groups (Figs. 8-13 and 8-14 for problem 1 and Figs. 8-18 and 8-19 for problem 2) show no significant changes. The total fluxes (Fig. 8-15 for problem 1 and Fig. 8-20 for problem 2), reflect a factor of two decrease in the dominant epithermal flux at the streaming path below the outer radial shield (lower right part of the figures).

This study shows that a significant decrease in a neutron streaming flux can be obtained using a neutron absorber on the streaming boundaries with a minimum of cover. Therefore, to reduce the streaming flux impinging on the PCRV liner above the wraparound shield, B_4C with a minimum stainless steel cover should be placed (1) on the inner surface of the radial shield below the blanket in the down-flow GCFR; (2) on the top surface of the lower plenum preshield or core catcher; (3) on the lower part of the outer surface of the radial shield; and (4) on the inner surface of the wraparound shield. If, however, shields are added to the fuel assemblies in the down-flow GCFR, less extensive boronation of the lower plenum surfaces may be adequate to protect the liner.

Other effects of streaming boronation are (1) an increase in the heating rate in boronated regions; (2) a decrease in gamma production in stainless steel; (3) a decrease in helium production in stainless steel; (4) a decrease in gamma heating rate in adjacent PCRV concrete; and (5) a decrease in gamma dose at tendons. Where these effects are produced by thermal neutrons, the decreases could amount to about an order of magnitude.

8.4. HEATING RATES IN A PROPOSED DEPLETED URANIUM CORE CATCHER

Preliminary calculations of approximate heating rates in a proposed GCFR depleted uranium core catcher were performed. The approximate maximum

heating rate on the top surface in a BOC neutron flux was about 0.50 W/cm^3 based upon hand calculations using one-group cross sections. The results from 1DFX (Ref. 8-12) slab model calculations using multigroup cross sections suggest that this maximum heating rate could be as high as 0.67 W/cm^3 , based upon a GCFR blanket flux impinging on the uranium. In an EOC neutron flux the heating rates could be increased by a factor of 1.5 to 2.0, resulting in a maximum heating rate which could become as large as 1.5 W/cm^3 .

8.4.1. Method

The slab model of the proposed core catcher is shown in Fig. 8-21, with the source impinging from the right side at $x = 124.8 \text{ cm}$. From right to left, the configuration consists of 45.7 cm U-238; 10.2 cm MgO; 38.1 cm graphite; 8.9 cm SiO_2 ; liner; and concrete. Transport calculations were performed using the 1DFX code and the ORNL 36-group neutron - gamma ray coupled set with 26 neutron groups. The surface source was a previously computed 36-group radial blanket spectrum normalized to one-half the ORNL total neutron flux resulting from earlier two-dimensional calculations of the GCFR lower plenum calculated with a BOC core and blanket (Ref. 8-1). The resulting heating rates are shown in Fig. 8-21; the maximum heating rate is 0.238 W/cm^3 . As a check on the 1DFX calculations and as a means of understanding the physical sources of the heating rates, hand calculations were performed with the objective of estimating the maximum heating rate.

8.4.2. Hand Calculated Heating Rates in Depleted Uranium

The hand calculated heating rates were obtained using (1) one-energy-group cross sections obtained from GGC-5 calculations; (2) the ORNL lower plenum flux at the level of the reference preshield; and (3) the energy releases from fission and neutron capture presented in Ref. 8-13. The approximate heating rates Q are related, in general, by

$$Q \text{ (fission/capture)} \sim \Phi_T \cdot \sigma \text{ (fission/capture)} \cdot \text{nuclide density} \\ \cdot \text{energy release per (fission/capture)} ,$$

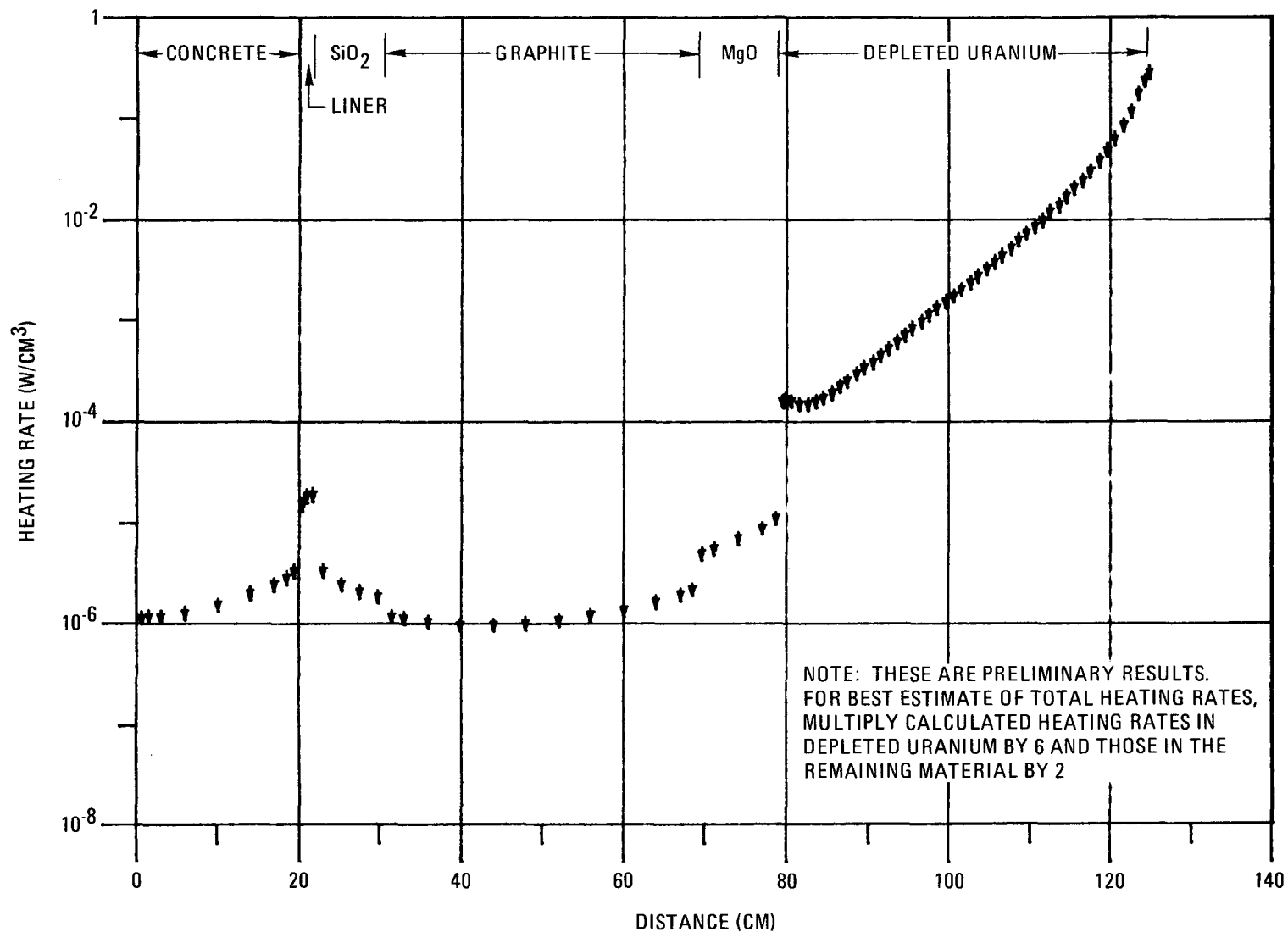


Fig. 8-21. Heating rates in proposed depleted uranium core catcher (capture gamma rays only)

where Φ_T is total neutron flux and σ one-group fission or capture cross section. If Φ_T is in $\text{n/cm}^2\text{-s}$, σ in barns, nuclide density in atoms/barn-cm, and energy release in MeV, multiplication by 1.602×10^{-13} J/MeV gives Q in W/cm^3 . The composition of the depleted uranium was assumed to be 99.7% U-238 and 0.3% U-235.

The one-group neutron capture and fission cross sections for U-235 and U-238 are given in Table 8-14, where the one neutron mass excess upon capture is given as 8.07 MeV (Ref. 8-1). Table 8-15 presents the distribution of energy per fission of U-235 and U-238, and Table 8-16 summarizes the approximate hand calculated heating rates due to neutron capture and fission of U-235 and U-238 resulting from a total neutron flux of 8.26×10^{12} $\text{n/cm}^2\text{-s}$ from a GCFR blanket spectrum. The physical insights given by these results are that (1) the heating rate due to fission of U-238 is comparable to that due to neutron capture, and (2) the energy deposition due to U-235 fission in depleted uranium accounts for about 20% of the total heating rate. The ORNL 1DFX multigroup U-238 heating rate of 0.238 W/cm^3 considers only capture gamma rays. Consequently, compared with the one-group capture rate of 0.18 W/cm^3 , the multigroup fission heating rates could be a factor of $0.238/0.18 = 1.32$ times greater than the one-group heating rates of Table 8-16, suggesting that the total of 0.50 W/cm^3 of Table 8-16 could be as large as 0.67 W/cm^3 at BOC. At EOC, this rate could be 1.5 to 2 higher, or about 1.5 W/cm^3 . In the proposed up-flow GCFR with the core catcher below the grid plate, the maximum heating rate could be reduced by a factor of 10. Furthermore, if fuel assembly shield extensions were added to the lower axial blanket of the reference down-flow GCFR, then the depleted uranium core catcher heating rates could be reduced by more than a factor of 10.

Multigroup U-238 fission cross sections will be obtained from ORNL for more accurate heating rate calculations. Until they become available, approximate EOC total heating rates in depleted uranium can be obtained by multiplying the U-238 results of Fig. 8-21 by about a factor of 6 and multiplying the heating rates in the other materials in Fig. 8-21 by a factor of 2.

TABLE 8-14
ONE-GROUP CAPTURE^(a) AND FISSION
CROSS SECTIONS FOR U-235 AND U-238

	σ_c (barns)	σ_f (barns)
U-235	0.853	2.45
U-238	0.362	0.0193

(a) Neutron mass excess = 8.07 MeV.

TABLE 8-15
DISTRIBUTION OF ENERGY PER FISSION
OF U-235 AND U-238 IN CORE CATCHER^(a)

	U-235 (MeV)	U-238 (MeV)
Kinetic energy		
Fission products	166.2	166.9
Neutrons (net kinetic energy)	4.8	2.4
Beta particles	<u>7.0</u>	<u>8.9</u>
Total kinetic energy	178.0	178.2
Gamma energy		
Prompt	8.0	7.5
Delayed	<u>7.2</u>	<u>8.4</u>
Total gamma energy	15.2	15.9
Total energy per fission deposited in depleted uranium core catcher	<u>193</u>	<u>194</u>

(a) Neutron capture in various reactor materials omitted.

TABLE 8-16
ENERGY DEPOSITION IN DEPLETED URANIUM
FRACTION OF U-235 = 0.003^(a)

Nuclide	Physical Process	Heating Rate (W/cm ³)
U-238	Neutron capture ^(b)	0.18 ^(c)
	Fission ^(b)	<u>0.23</u>
	Total U-238	0.41
U-235	Neutron capture ^(b)	0.0013
	Fission ^(b)	<u>0.089</u>
	Total U-235	<u>0.090</u>
Total U-238 + U-235		0.50

(a) Irradiated by total neutron flux = 8.26×10^{12} n/cm²-s in a GCFR blanket spectrum.

(b) Based upon one-energy-group cross sections.

(c) With ORNL multigroup cross sections, the U-238 capture heating rate is 0.238 W/cm³.

REFERENCES

- 8-1. "Gas-Cooled Fast Breeder Reactor Quarterly Progress Report for the Period August 1, 1976 Through October 31, 1976," ERDA Report GA-A14112, General Atomic Company, November 1976.
- 8-2. "Gas-Cooled Fast Breeder Reactor Quarterly Progress Report for the Period February 1, 1978 Through April 30, 1978," DOE Report GA-A14928, General Atomic Company, May 1978.
- 8-3. "Gas-Cooled Fast Breeder Reactor Quarterly Progress Report for the Period August 1, 1977 Through October 31, 1977," DOE Report GA-A14613, General Atomic Company, November 1977.
- 8-4. Rosenwasser, S. N., and R. G. Perkins, "Assessment of Materials Irradiation Effects Data Requirements for GCFR Out-of-Core Structural Components," General Atomic Company, unpublished data.
- 8-5. "Gas-Cooled Fast Breeder Reactor Quarterly Progress Report for the Period February 1, 1977 Through April 30, 1978," DOE Report GA-A14358, General Atomic Company, May 1977.
- 8-6. Moen, R. A., et al., "Neutron Fluence Limit Determinations for Some Fast Flux Facility Components," in Irradiation Effects on Structural Alloys for Nuclear Reactor Applications, STP 484, American Society for Testing and Materials, Philadelphia, 1970.
- 8-7. Goel, B., "Importance and Status of (η, α) Cross Sections for a Reliable Prediction of Radiation Damage in Stainless Steel," Kernforschungszentrum Karlsruhe Report EURFNR-1465, June 1977.
- 8-8. Nuclear Systems Materials Handbook, Hanford Engineering Development Laboratory (TID-26666).
- 8-9. Simons, R. L., "Helium Production in FBR Out-of-Core Structural Components," Paper Presented at the Ninth ASTM International Symposium on Effects of Radiation on Structural Materials, July 11-14, 1978, Richland, Washington.
- 8-10. Mathews, D. R., et al., "GGC-5, A Computer Program for Calculating Neutron Spectra and Group Constants," Gulf General Atomic Report GA-8871, September 27, 1971.

- 8-11. "DOT 3.5: A Two Dimensional Discrete Ordinates Radiation Transport Code," RSIC Computer Code Collection ORNL-CCC-276, Oak Ridge National Laboratory.
- 8-12. Archibald, R., K. D. Lathrop, and D. Mathews, "1DFX - A Revised Version of the 1DF (DTF-IV) SN Transport Theory Code," Gulf General Atomic Report Gulf-GA-B10810, September 27, 1971.
- 8-13. James, M. F., "Energy Released in Fission," J. Nucl. Energy 23, 517 (1969).

9. SYSTEMS ENGINEERING (189a No. 00585)

9.1. SYSTEMS DESIGN

The GCFR core auxiliary cooling system (CACS) design study report was completed (Ref. 9-1). This report presents the results of an alternate design study for the CACS of the 300-MW(e) GCFR demonstration plant. The study includes design, transient analysis, drive and control trade-off, and analytical methods development.

Preliminary process flow diagrams showing two parallel 50% feedwater trains for the turbine steam and feedwater and condensate systems were issued, and preliminary RECS input was revised to reflect possible critical path items and coordination with other organizations.

9.2. SYSTEMS INTEGRATION

The Baseline Data Book (Ref. 9-2), which contains data on and descriptions of the overall plant, NSSS, nuclear island, and turbine plant and auxiliary segment, was issued.

REFERENCES

- 9-1. Khoe, J. H., D. N. Leach, and H. S. Chung, "GCFR Core Auxiliary Cooling System Design Study," DOE Report GA-A14758, General Atomic Company, May 1978.
- 9-2. "Baseline Data Book," General Atomic Company, unpublished data.

10. COMPONENT DEVELOPMENT (189a No. 00586)

10.1. REACTOR VESSEL

The scope of this subtask is to ensure that the design of the PCRV and related components which contribute to the integrity of the pressure boundary is satisfactory and to test critical component configurations to make certain that they attain the design objectives. This subtask will demonstrate by analyses and tests that the PCRV and its penetrations and closures meet the design criteria, and it will also ensure that (1) the design of the thermal barrier satisfactorily protects the liner and PCRV from the effects of high temperatures, and (2) the flow restrictors for the large penetrations can be developed to limit the flow of helium from the primary coolant systems to acceptable levels in the event of structural failure of a penetration or closure component.

During the last quarter, the status of recent PCRV configurations was reviewed. The design objective of each configuration and the reason for proceeding to another arrangement are summarized in Table 10-1 of Ref. 10-1. A preliminary structural analysis report (Ref. 10-2) was issued describing the stress levels for the 1974 PCRV reference design.

During this quarter, as part of a safety evaluation for residual heat removal (RHR) schemes, a series of PCRV arrangements was made for up-flow core configurations. The basic requirement of these arrangements was that the core auxiliary heat exchanger be located at a certain distance [8230 mm (27 ft)] above the centerline of the reactor core to allow for natural convection cooling. The up-flow core has the core assemblies standing on the grid plate, with remote fuel handling within the cavity space above the core. The components of the primary cooling loop, i.e., steam generator

and helium circulator, are placed either in the same cavity or in separate cavities. In the arrangements shown in Figs. 10-1 through 10-3, the helium circulator is on the top surface of the PCRV. The arrangements shown in Figs. 10-4 through 10-7 have the helium circulator in the lower surface of the pressure vessel; the helium circulator is in a separate cavity for the arrangements shown in Figs. 10-4 and 10-5. These PCRV arrangements were evaluated, and the configurations shown in Fig. 10-3 and 10-7 were selected for further study. These two configurations were selected because of their relatively simple ducting, which facilitates design, structural analysis, and construction. Up-to-date layout drawings for these two PCRV configurations were prepared (Figs. 10-8 and 10-9). These arrangements incorporate a space envelope for a melted core catcher, and the arrangement shown in Fig. 10-8 includes a penetration between the steam generator cavity and the auxiliary circulator cavity to introduce the feedwater past the helium circulator for the once-through type steam generator. PCRV cold water for the core auxiliary heat exchanger will be brought in and hot water taken out in the top portion of this penetration. One of these configurations will be selected for a detailed PCRV arrangement.

Conceptual design studies for a molten core retention system continued, and preliminary layouts for a uranium metal bath core catcher were completed. This design consists of a crucible which utilizes a large mass of clad uranium alloy at its bottom with either tungsten or magnesia plates around its periphery. In this design the core debris decay heat melts the uranium alloy, forming a molten pool into which core debris falls and solidifies. The higher temperature and less dense core material remains on top, and decay heat is removed by internal convection, conduction, and radiation to the PCRV cooling water system. Depleted uranium was chosen for its high density, low melting point, high boiling point, and shielding effectiveness.

Development of a conceptual design for a stainless steel bath core catcher concept was initiated. This concept employs a large mass of stainless steel plates that melt during the postaccident fuel containment (PAFC) period, forming a "light metal bath" expected to be slightly higher than

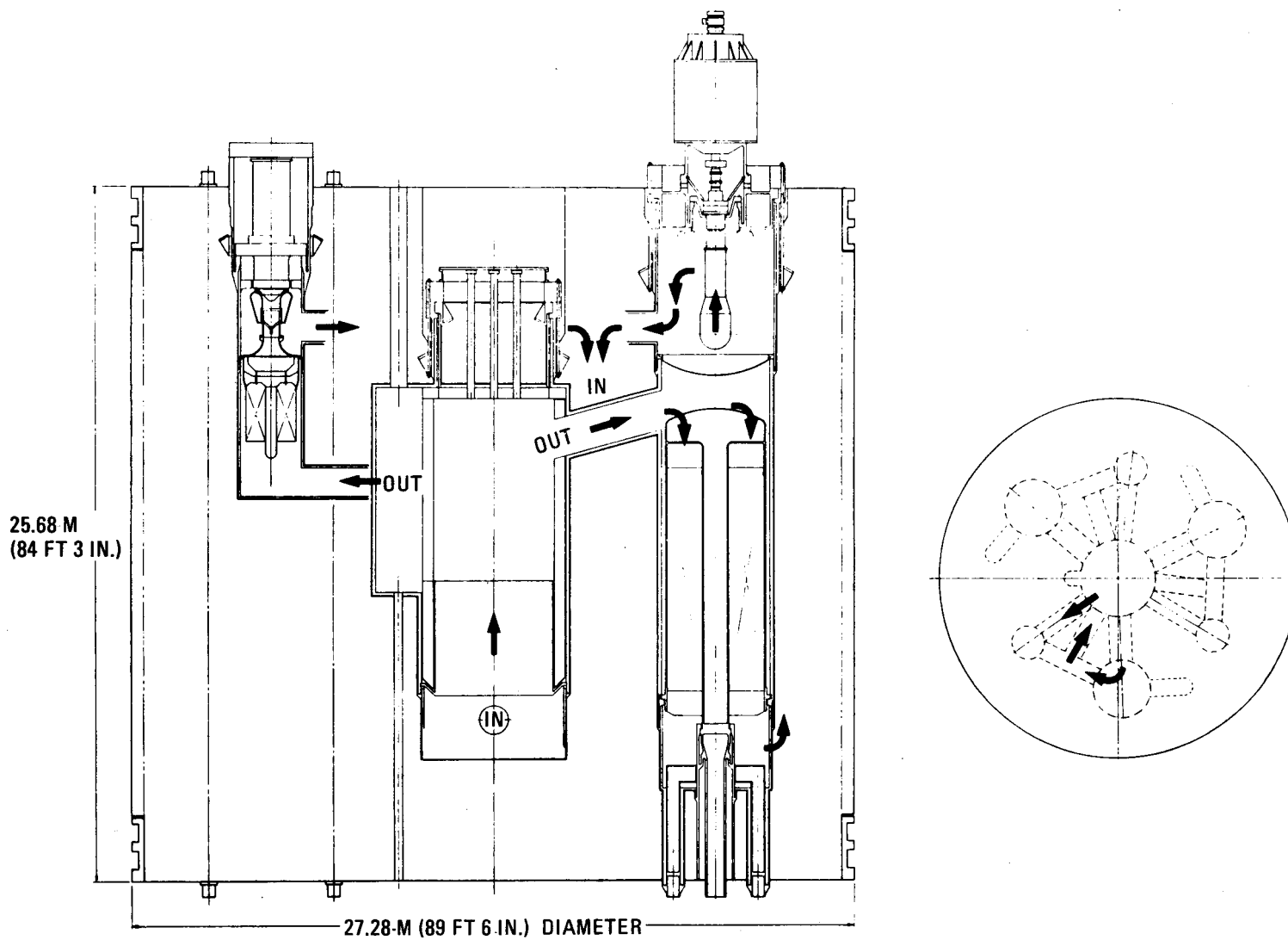


Fig. 10-1. 300-MW(e) GCFR up-flow core

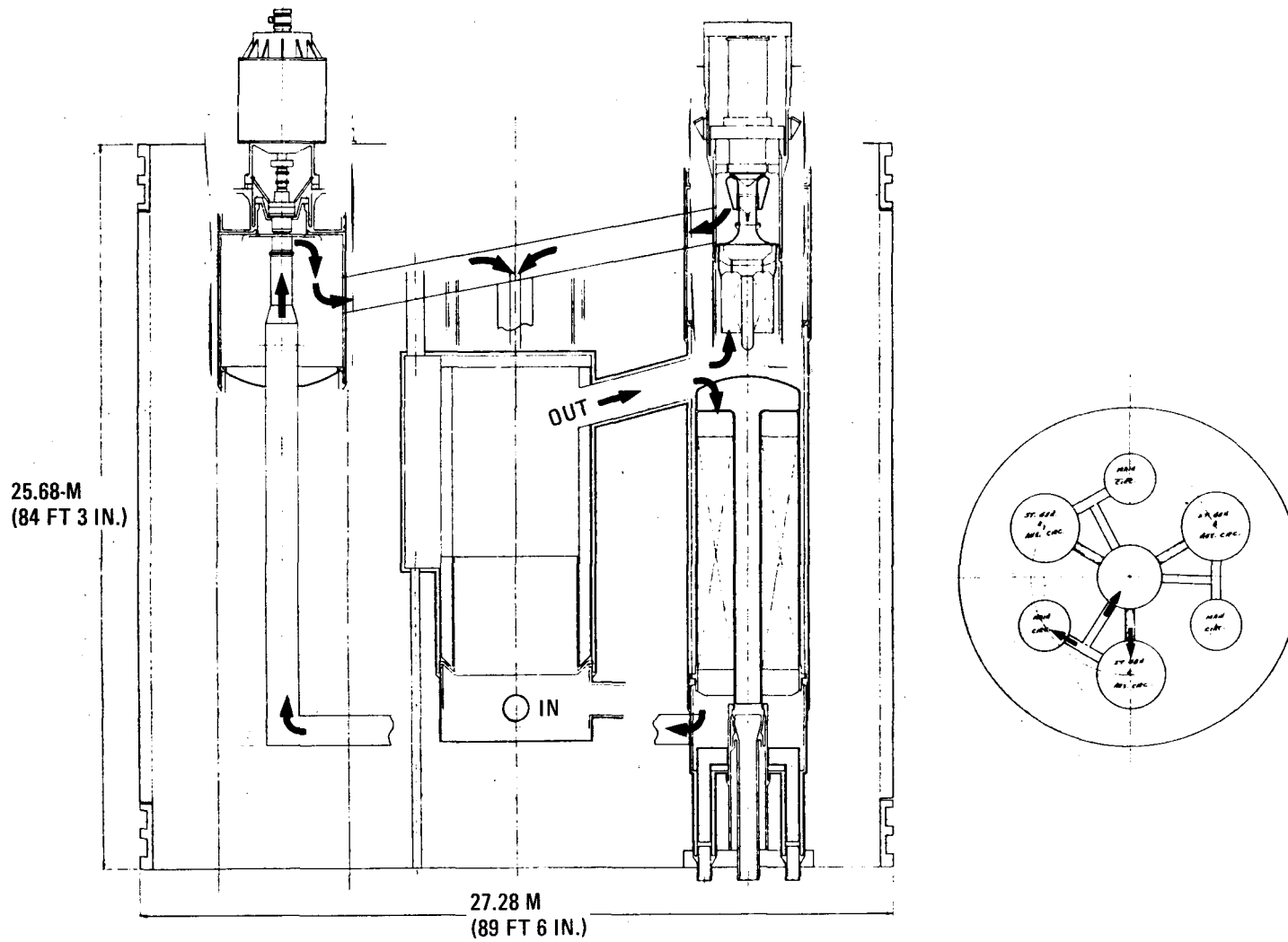


Fig. 10-2. 300-MW(e) GCFR up-flow core, concept No. 5

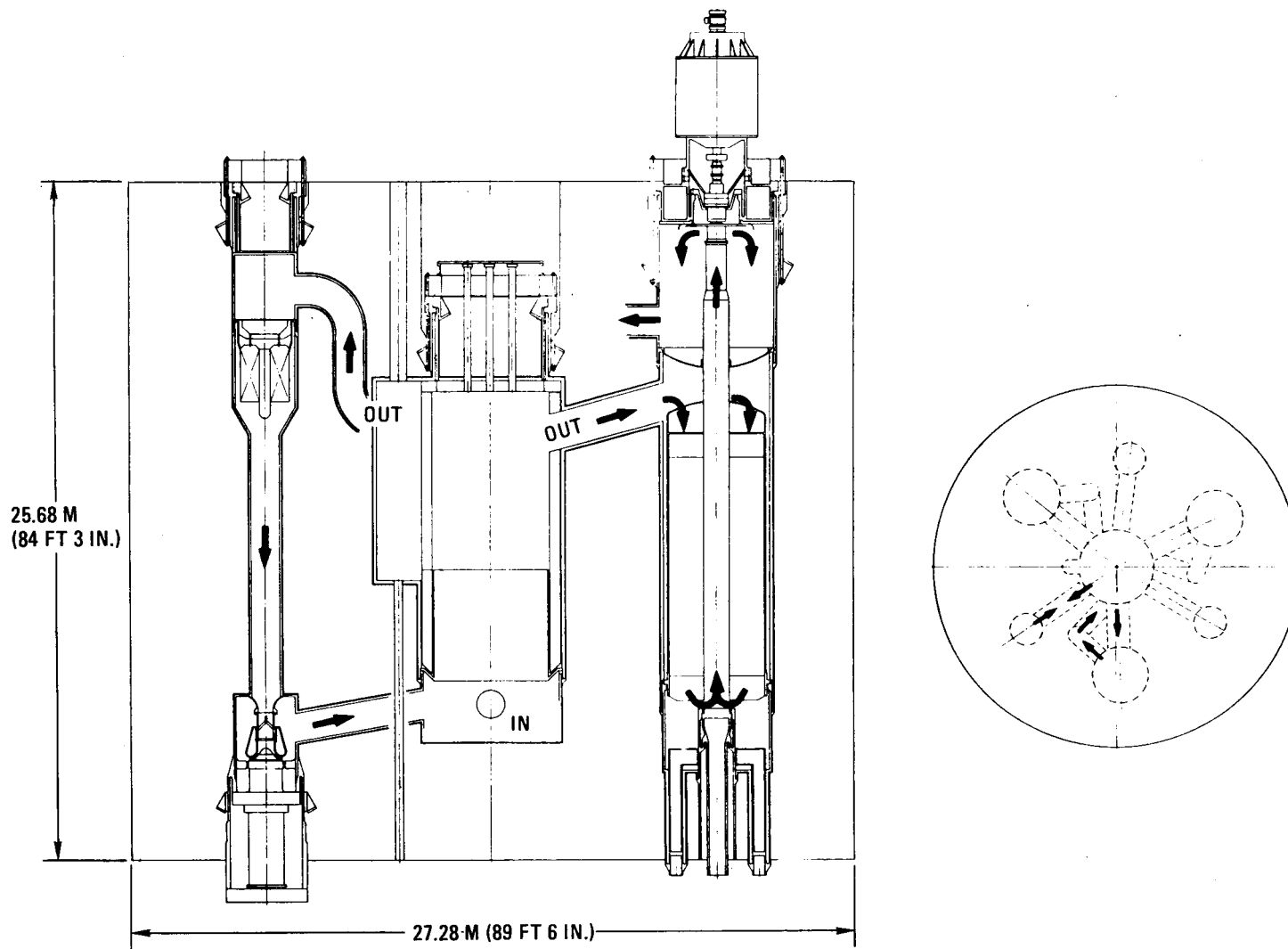


Fig. 10-3. 300-MW(e) GCFR up-flow core, concept No. 7

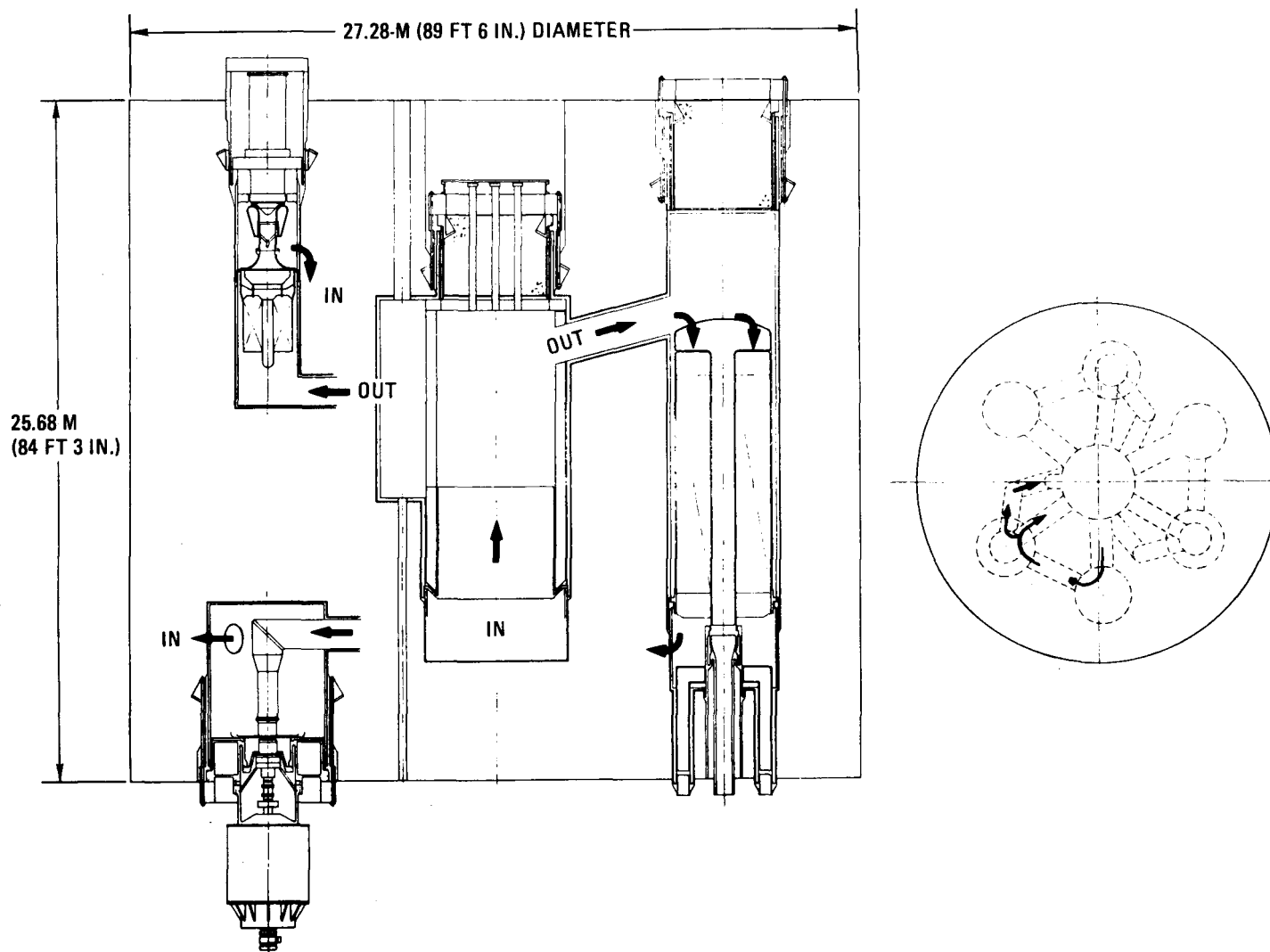


Fig. 10-4. 300-MW(e) GCFR up-flow core, concept No. 2

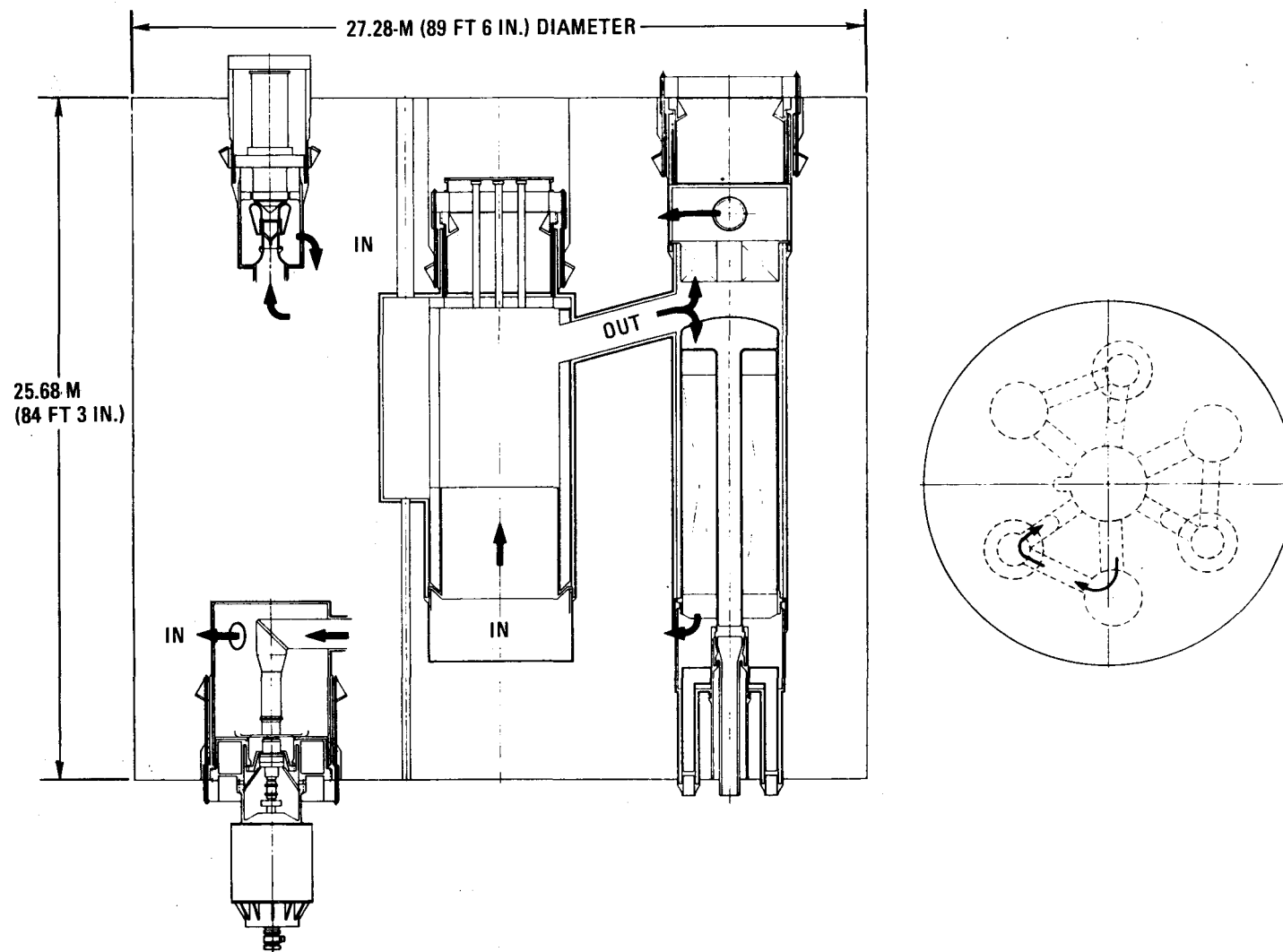


Fig. 10-5. 300-MW(e) GCFR up-flow core, concept No. 3

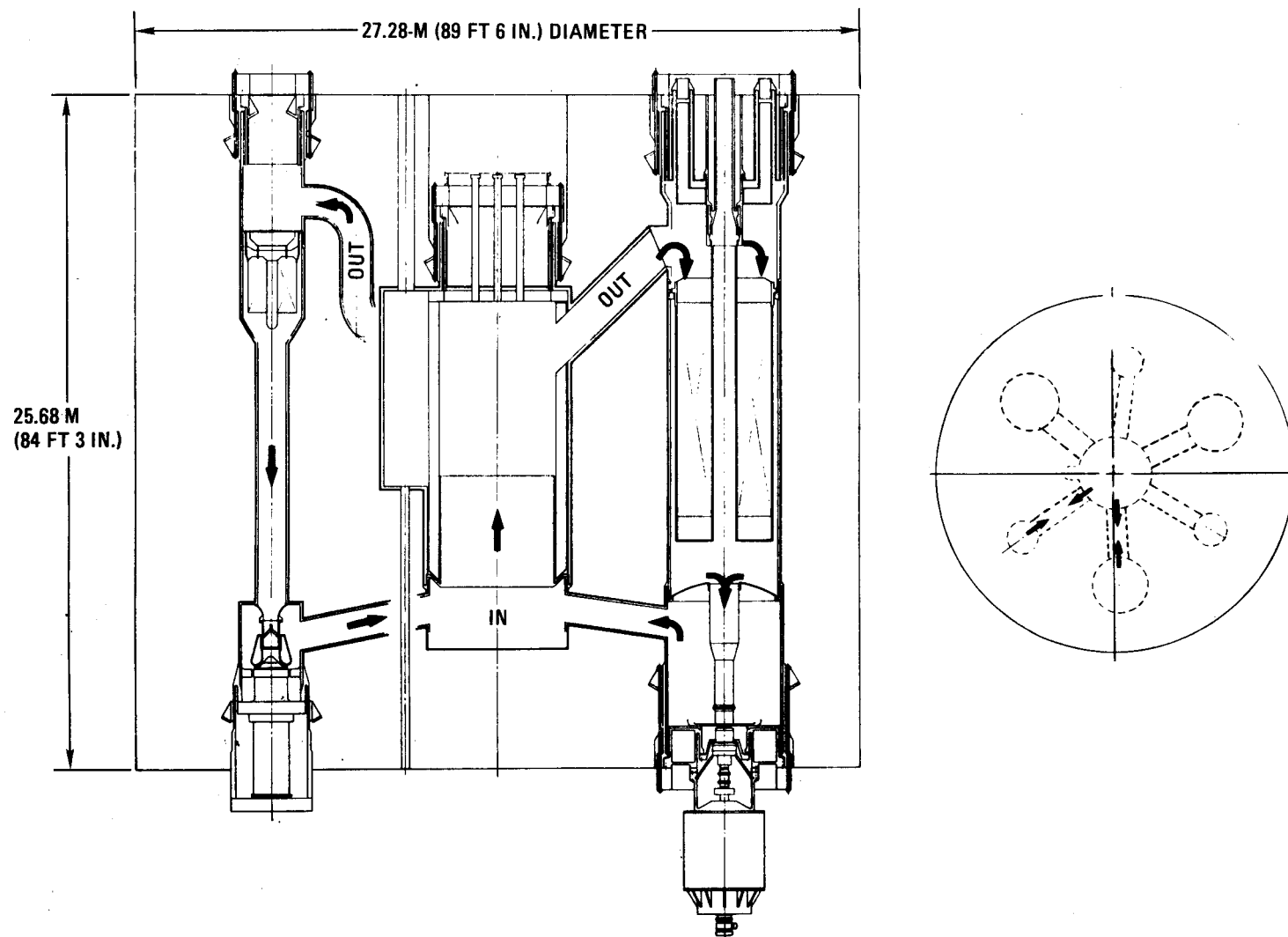


Fig. 10-6. 300-MW(e) GCFR up-flow core, concept No. 4

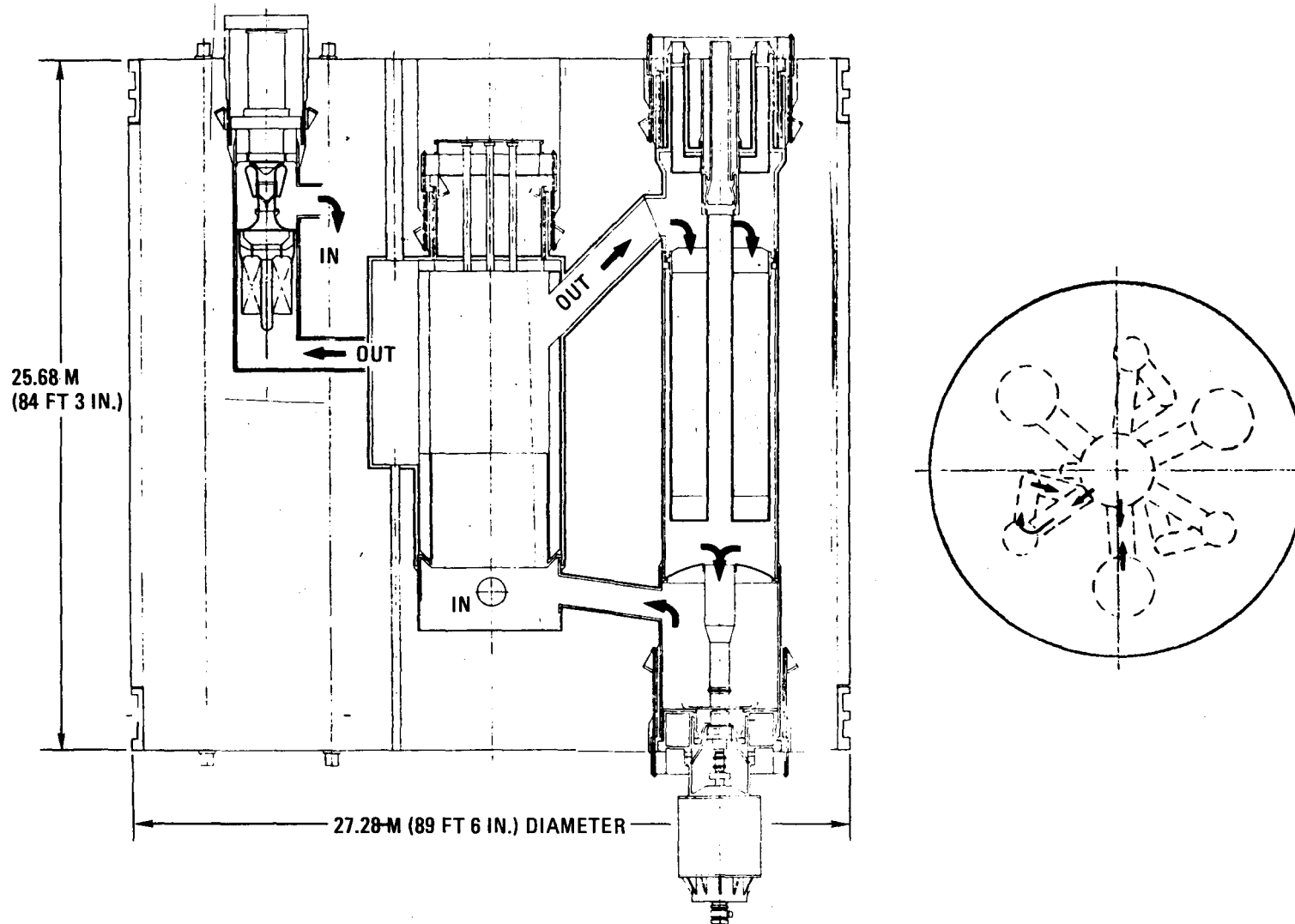


Fig. 10-7. 300-MW(e) GCFR up-flow core, concept No. 6

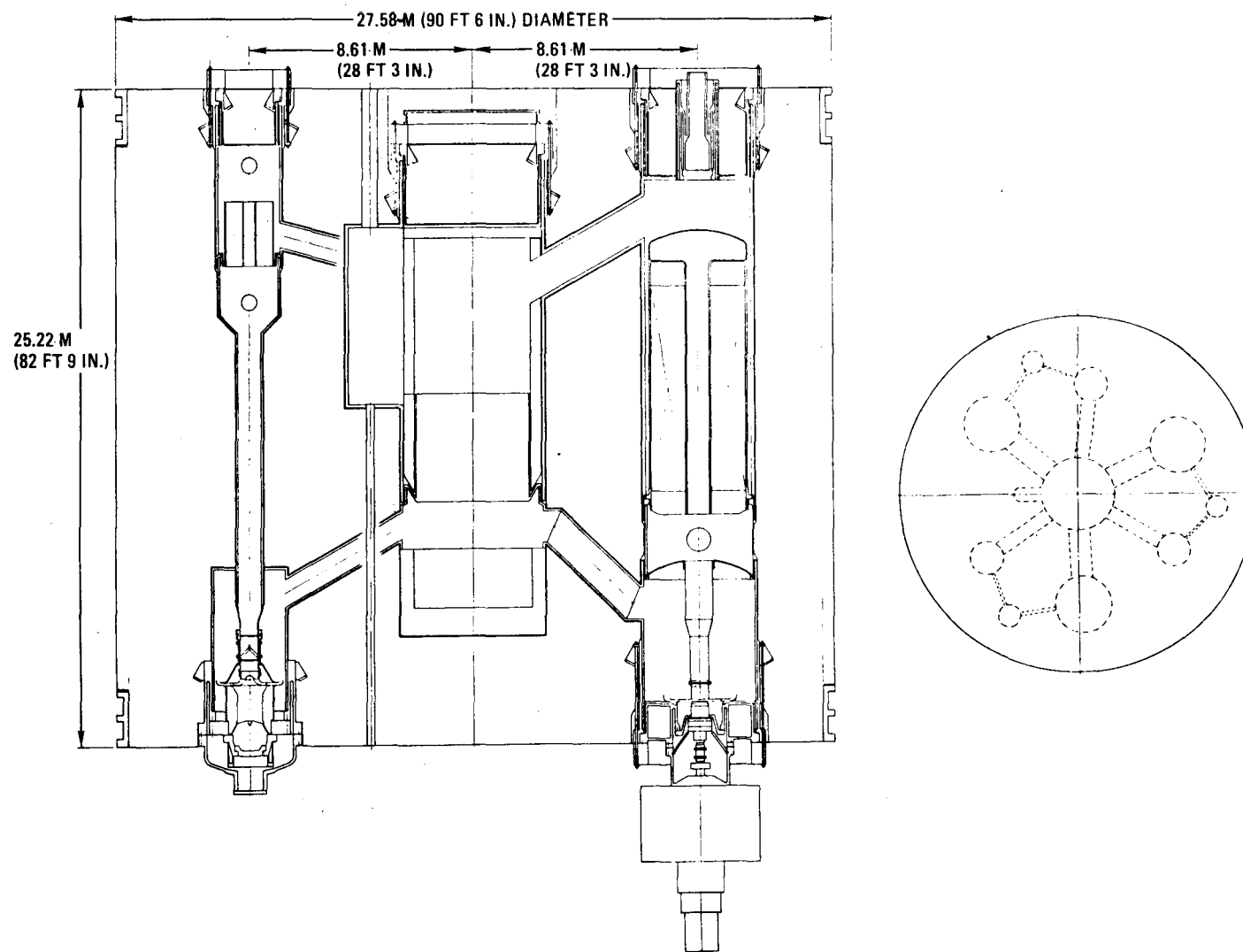


Fig. 10-8. 300-MW(e) GCFR up-flow core, concept No. 4A

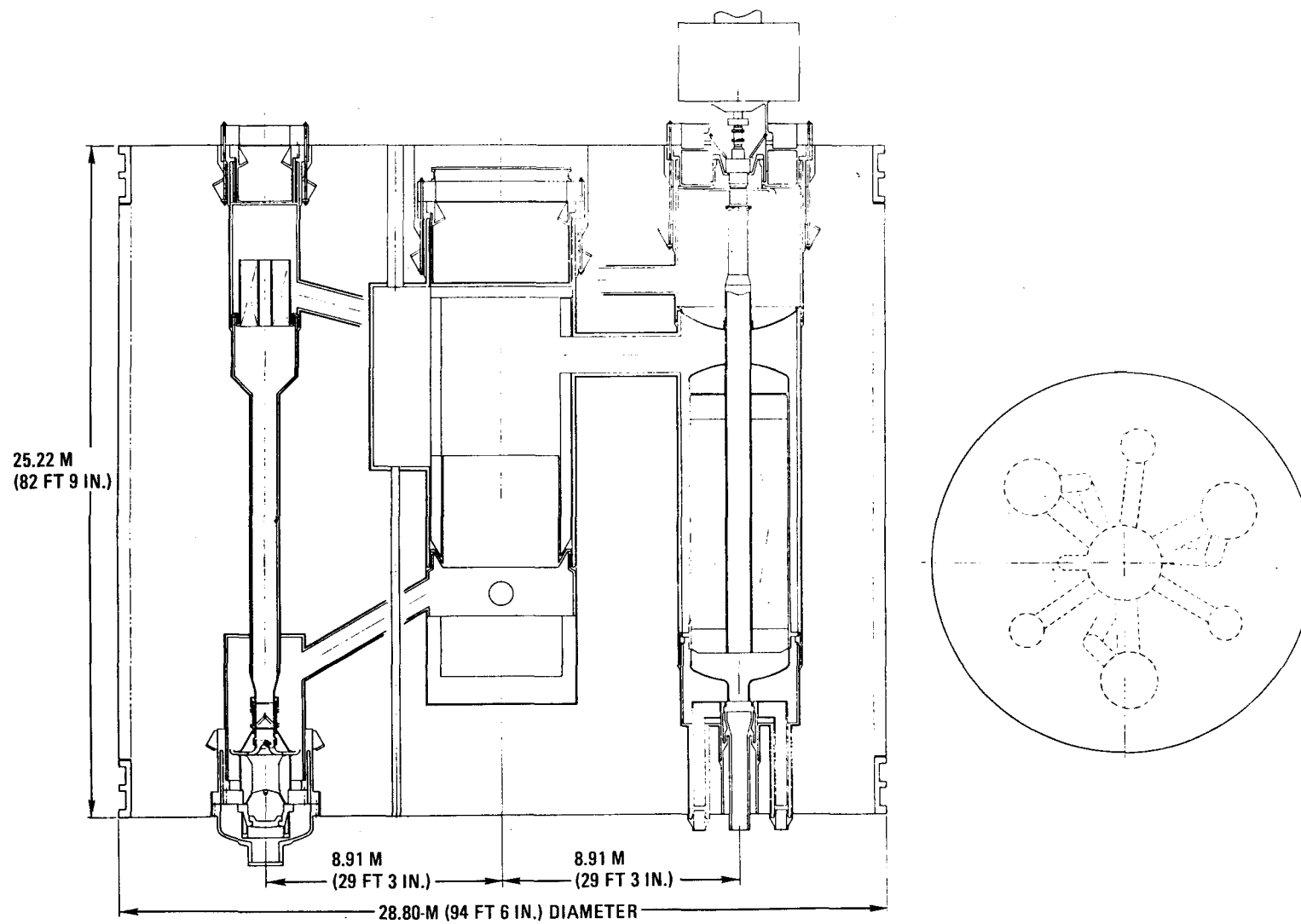


Fig. 10-9. 300-MW(e) GCFR up-flow core, concept No. 7A

the melting temperature of steel but far below the melting point of oxide fuel. Solid fuel debris immersed in a stainless steel pool is expected. This concept utilizes a thermal barrier (of nontypical design) which is located away from the liner to the top surface of the steel plates. This thermal barrier has to provide liner protection during normal operation, but is expected to be consumed during the postulated meltdown, thus enhancing greater PCRV cooling water system heat removal capability. A preshield consisting of a thick plate structure of stainless steel standing off the lower structures will probably be needed to provide a region where gamma heating can effectively be removed (by helium circulation) during normal operation. The preshield also functions as an impact barrier to prevent damage to the thermal barrier in the event that a fuel assembly is dropped during refueling. Stainless steel was chosen for its heat storage capability, melting characteristics, shielding characteristics, conductivity, relatively low cost, and capability of fabrication.

The lower main cross ducts of two alternate thermal barrier concepts (see Figs. 10-10 through 10-13) were compared. Alternate No. 1 features square-shaped insulation modules compressed by cover plates which are approximately 762-mm (30-in.) square. A large integral central adapter clamps the cover plate to the liner with a cluster of studs. There are no other peripheral or edge fixtures, and the cup-shaped central adapter design permits single failure of a stud attachment to the liner without impairment of thermal performance. Alternate No. 2 employs insulation rails which lie between rectangular insulation modules. The large rectangular cover plates are secured to the liner by edge retainers and a central adapter. The midedge retainers are contained in the insulation rails. There are 48 insulation modules per duct, twice as many as for alternate No. 1, and 36 fixtures per duct, compared with 24 fixtures in Alternate No. 1. Evaluation of these concepts indicates that alternate No. 1 is superior to alternate No. 2 in the following aspects:

1. Overall simplicity.
2. Manufacturing cost.

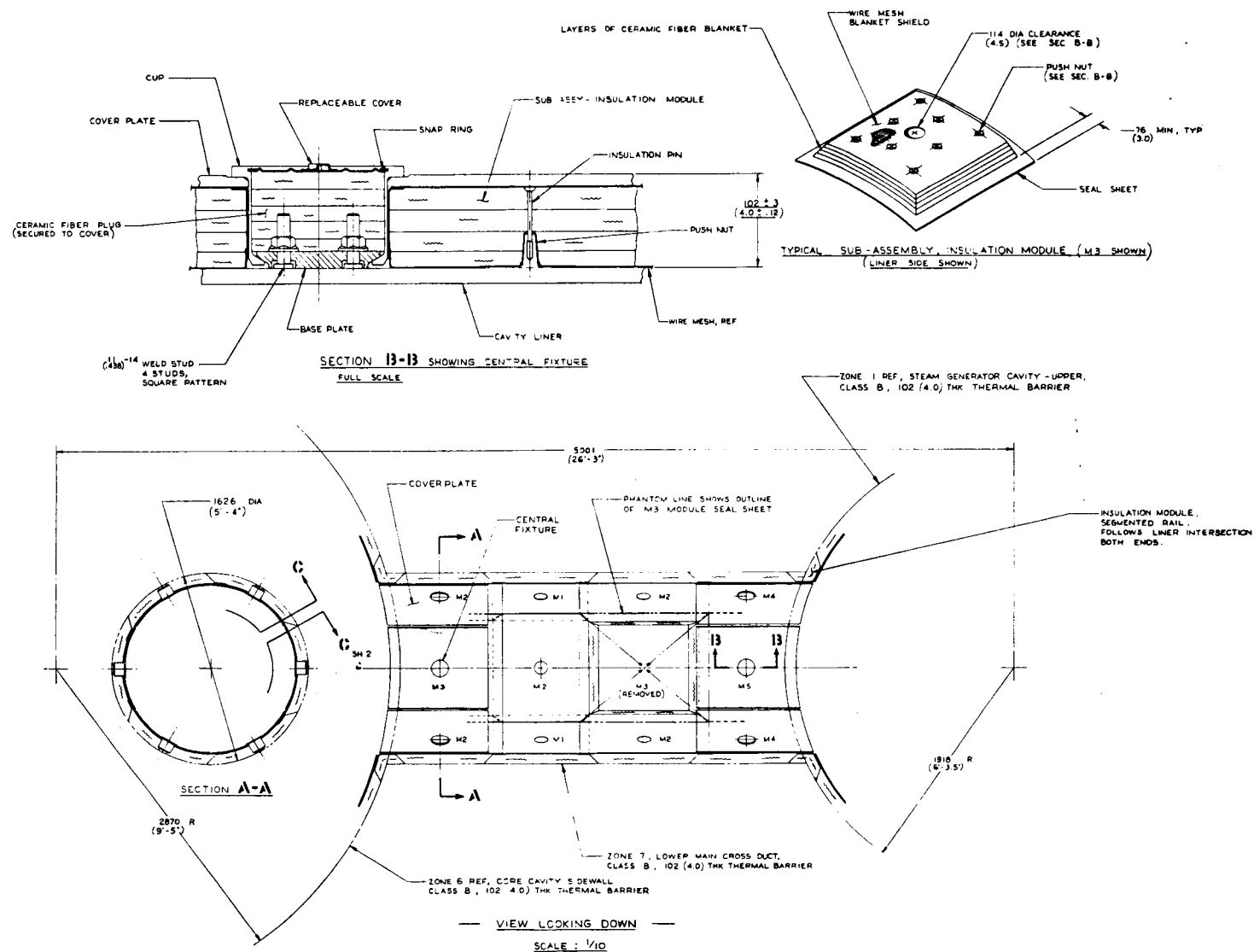


Fig. 10-10. Replaceable thermal barrier installation, zone 7, alternate No. 1 (view looking down)

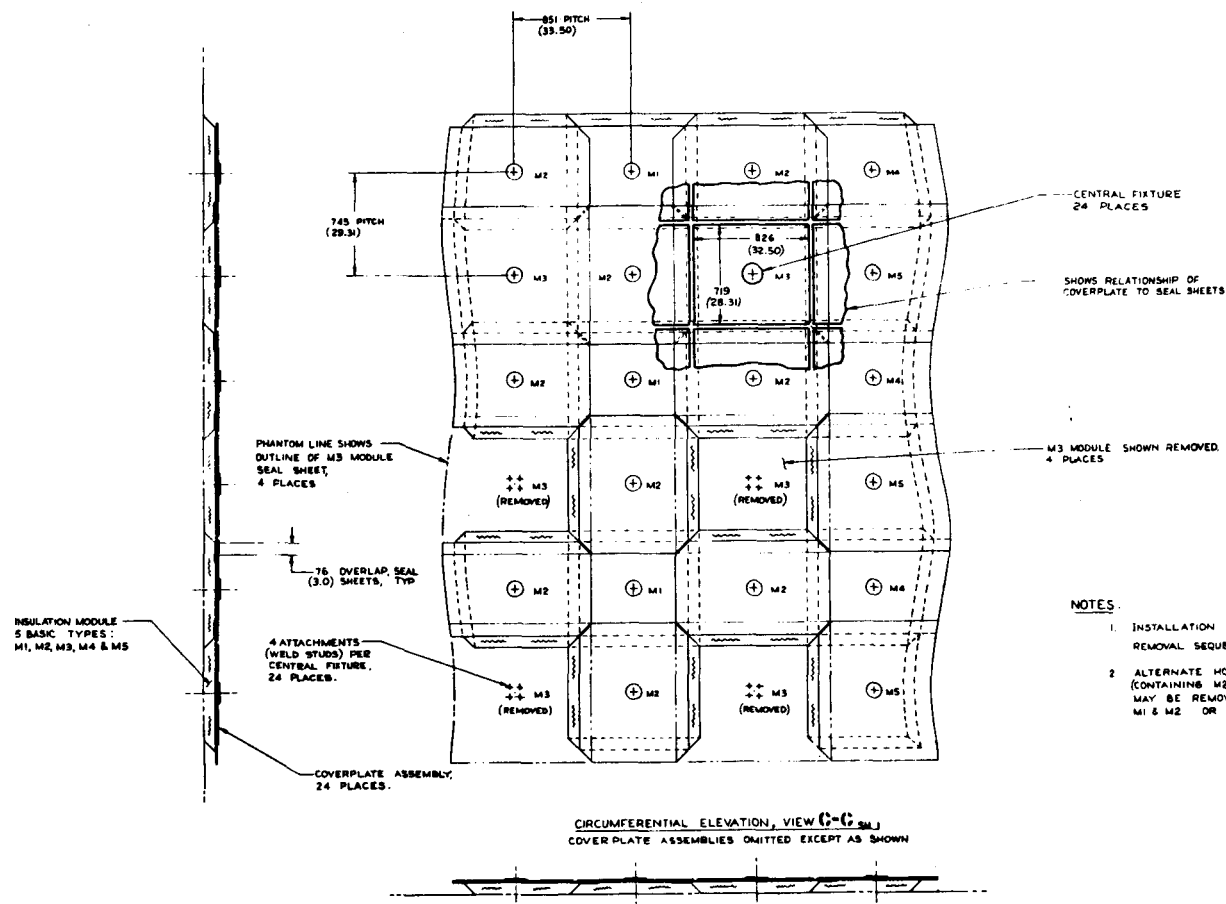


Fig. 10-11. Replaceable thermal barrier installation, zone 7, alternate No. 1 (circumferential elevation view)

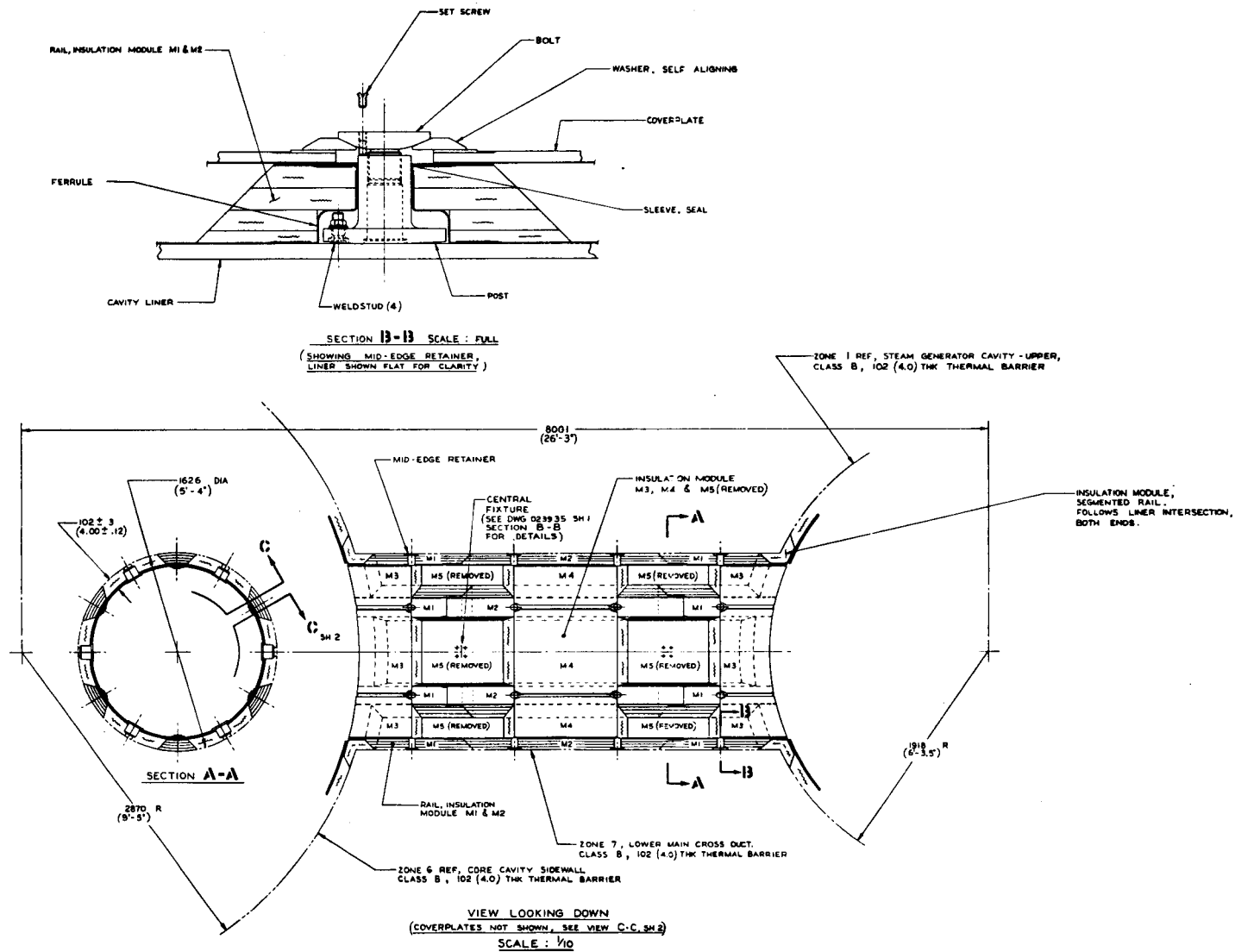


Fig. 10-12. Replaceable thermal barrier installation, zone 7, alternate No. 2 (view looking down)

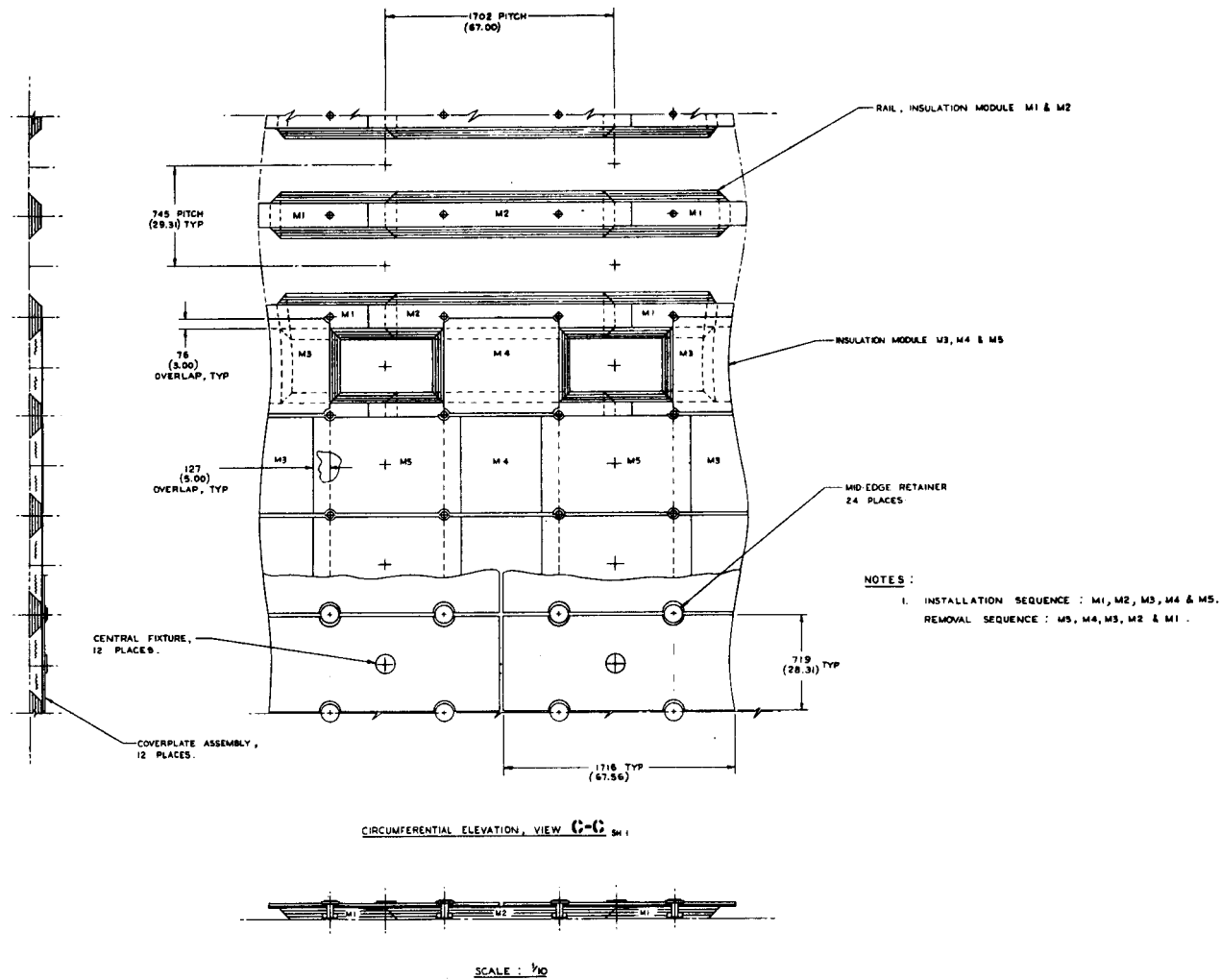


Fig. 10-13. Replaceable thermal barrier installation, zone 7, alternate No. 2 (circumferential elevation view)

3. Ease and cost of installation.
4. Ease and convenience of replacement.

The only significant possible advantage of alternate No. 2 is the structural redundancy provided by the midedge retainers. Thermal performance and practical fabrication aspects of installation and replacement should be proven by permeation tests and representative full-scale mock-ups.

Revision of the ORNL closure test program to replace the 1/15-scale confirmatory tests by development tests of comparable scale was reviewed. This revision was generally considered acceptable, but it was noted that design of the reactor core cavity closure may be governed by working stress limits. A two-dimensional analysis of the reactor core cavity closure considering the effect of steel penetrations and penetration shear anchors is being performed by ORNL to identify the critical working stresses in the closure. Based on the results of this analysis and on design considerations, GA will recommend the dimensions for the 1/18-scale development test models for the reactor core cavity closure. ORNL's proposed test schedule is being reviewed by GA for compatibility with the overall design programs.

A drawing for a stationary core cavity closure was completed for the up-flow core (Fig. 10-14). This closure contains 37 penetrations and has a forged holddown ring; studs and redundant tie rods are provided for holddown. Several core cavity closure design concepts using a rotating plug were generated for the up-flow core study. Such closures enable refueling equipment to be located external to the reactor, allow direct, in-line lifting of the fuel assemblies, and could permit a sizeable reduction of the core upper plenum height. One concept which is being evaluated is located in a 5601-mm (18 ft 4-1/2 in.) cavity and consists of a 5131-mm (16 ft 10 in.) concrete plug with 38 penetrations (one of which is used for replacing the outer blanket assemblies). The plug is supported by a ring structure which can rotate relative to the core for refueling, and the plug can linearly translate relative to the ring. Refueling may be accomplished through five penetrations, and an inflatable elastomer seal is provided for sealing during refueling.

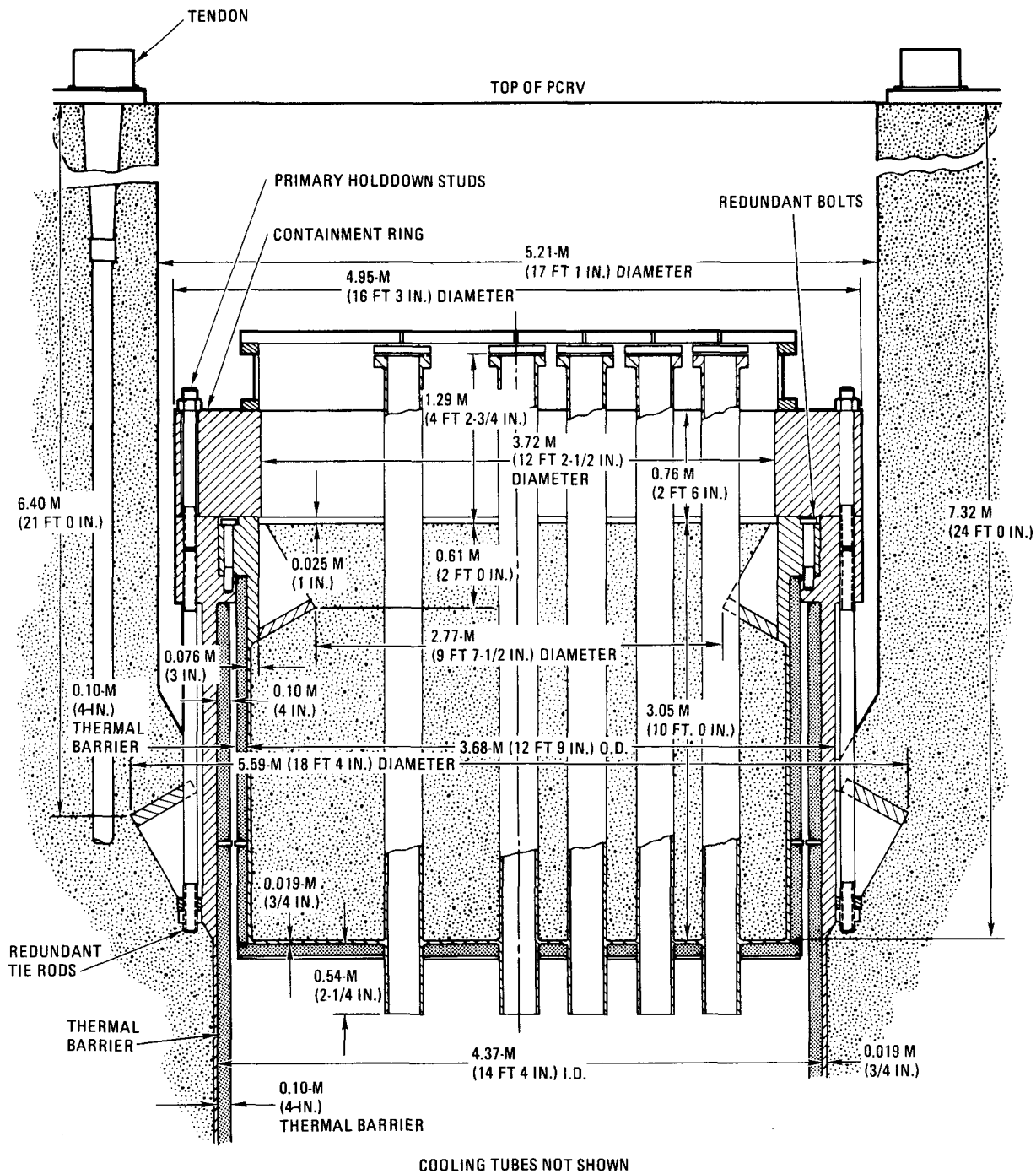


Fig. 10-14. GCFR up-flow core closure system

A conceptual design was developed for the auxiliary cooling loop flow restrictor. This concept utilizes an ellipsoidal head similar to the flow restrictor for the high-temperature gas-cooled reactor (HTGR) plant. All piping and electrical penetrations are located in a central nozzle, and the design allows access to the primary closure without removal of any piping.

10.2. CONTROL AND LOCKING MECHANISMS

During this quarter a conceptual design for the control rod latch mechanism using sequenced push-pull motions was prepared. The latch operation is performed by a special tool inserted into the top end of the control rod drive linear actuator. An electromechanical device for sensing proper latch engagement to the control rod is included. The latching mechanism provides the capability of extending the rod below the normal down-stop position of the control rod drive linear actuator. Since the control rod is an integral part of the core control assembly, it would be provided with its own internal down-stop. This stop has to be located below the down-stop position of the control rod drive linear actuator to assure that no external drive mechanism forces are transmitted to the core control assembly. To disengage the control rod, the latch mechanism has to be lowered the extra distance required to contact the core control assembly stop and then raised back up to its normal position relative to the linear actuator after reengagement.

Design concepts for the control rod drives and any necessary guide and support structures are being investigated for two types of refueling schemes for the up-flow core: (1) fixed top head cavity closure plug with in-vessel transfer of fuel exiting downward alongside the core and out through the bottom of the vessel and (2) rotating top head cavity closure plug with fuel axially transferred upward through control rod drive penetrations. Adaptation of the control rod drive mechanism principle and arrangement developed for the down-flow GCFR is being explored. For the down-flow GCFR, the control rod drives are housed inside core lock actuator assemblies

contained inside the top head penetrations and extending downward to the top of the grid plate. The control rod drive linear actuator mechanism, power components, control instrumentation, etc., are packaged within the cooled concrete depth of the PCRV top head. The core lock actuator portion extending below the top head provides the tubular guide housing for the driveline member connected to the control rod at the grid plate elevation. Thermocouple pathways to the core assemblies are also provided through the lock actuator assembly structure extending down to the grid plate.

Locking for the up-flow GCFR core assembly is not performed in the same manner as that for the down-flow concept. Therefore, this locking function as part of a control rod drive installation is not required. However, outlet temperature monitoring still remains a requirement, and the same principle of thermocouple routing used for the down-flow core would be retained.

10.3. FUEL HANDLING DEVELOPMENT

The equipment involved in refueling a down-flow core with assemblies handled through the bottom head has been described, and conceptual studies have been made of various methods of refueling an up-flow core. Other ongoing studies have resulted in concepts for refueling through a rotating plug arrangement in the top head and an alternate to the pantograph machine for the down-flow core which provides extended reach and greater stability.

10.4. CORE SUPPORT STRUCTURE

The purpose of this subtask is to assure the availability of the structural analysis methods and materials mechanical behavior required to assess the structural integrity of the GCFR core support structure under all anticipated operational and safety-related loading conditions. Work accomplished on the reference design during the last quarter included simplified comparative thermal and stress analyses of the grid plate for

the transient conditions following a reactor trip with electric and steam-driven circulators and a configuration study for a grid plate and core support applicable to an up-flow core. The configuration adopted (Fig. 10-15) consists of a two part grid plate with the upper part supported from the PCRV by means of a frustum of a cone. The proposed thickness of the upper plate is 305 mm (12 in.), that of the lower plate 152.5 mm (6 in.), and that of the interspace 363 mm (14.7 in.). A core barrel supported by the upper grid plate provides lateral restraint.

During this quarter descriptions of the efforts necessary for completing the core support task for an up-flow study were provided to the Program Planning Committee. The core support configuration study was broadened to include several new concepts, and Refs. 10-3 and 10-4 were reviewed.

10.4.1. New Concepts for Up-Flow Core Support Structure

Three new concepts for up-flow support were generated:

1. An arrangement utilizing a coolant gas distribution manifold above the grid plate for each group of seven fuel assemblies. This arrangement requires only one-seventh as many penetrations of the grid plate as the reference design.
2. A segmented concept using bolted sectors. The gas distribution manifold is used with this concept to minimize the number of penetrations.
3. A bottom-head-support grid plate.

10.5. REACTOR SHIELDING ASSEMBLIES

The purpose of this subtask is to design and develop analytical methods and experimental programs to evaluate the reference design of

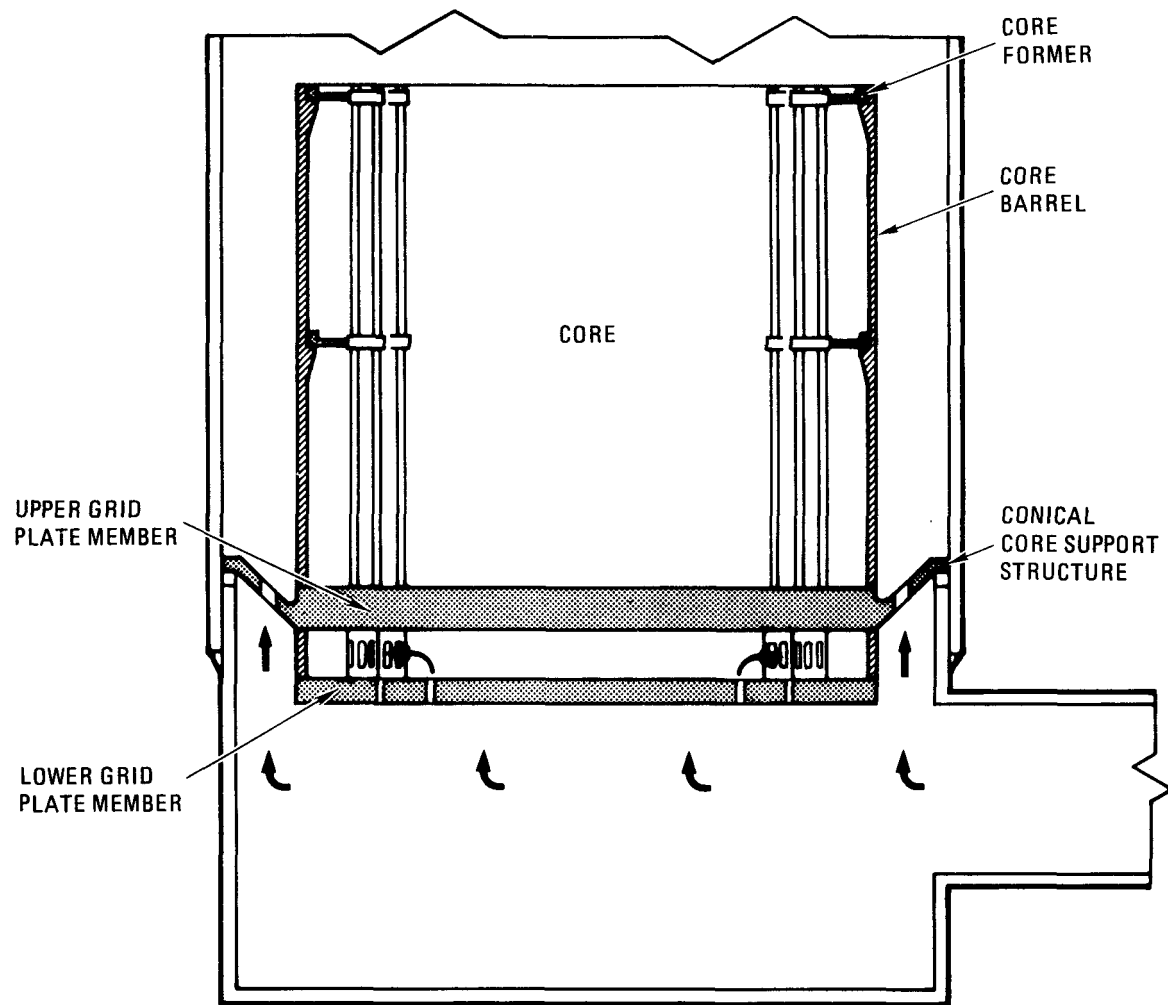


Fig. 10-15. Conceptual up-flow core configuration

the reactor shields. This evaluation is expected to cover nonuniform temperature distribution, material behavior, seismic effects, hydrodynamic tests, and structural analysis. Alternate shield configurations will also be studied so that a satisfactory and optimized shield design can be developed.

During the previous quarter preliminary layout drawings were developed for the reference design showing a method of supporting the outer radial shield from the liner and axially segmenting the replaceable inner radial shields (Fig. 10-16). A configuration study was also performed for outer radial shielding applicable to an up-flow core (Fig. 10-17). The configuration adopted is similar to that for the reference design except that the replaceable shielding assemblies are placed on the grid plate rather than on the outer radial shields, as in the reference design, and are cooled by core inlet gas.

During this quarter preliminary computer analysis of expected temperatures in the reference shielding was performed using the TAC2D code (Ref. 10-5), and a more extensive thermal analysis was performed on the up-flow shielding configuration using the same code. Stress and load analysis on the reference design lower plenum radial shielding was begun.

10.5.1. Reference Design Thermal Analysis

Bypass flows of 1% and 2% through each intrashield annulus were selected based on hand calculations of the fraction of total coolant flow which could be expected to flow upward in the inner shield annuli (Fig. 10-16). The four parameters listed below were varied to determine the sensitivity of the results to each variation:

1. Nuclear heat generation rates.
2. Graphite thermal conductivity.

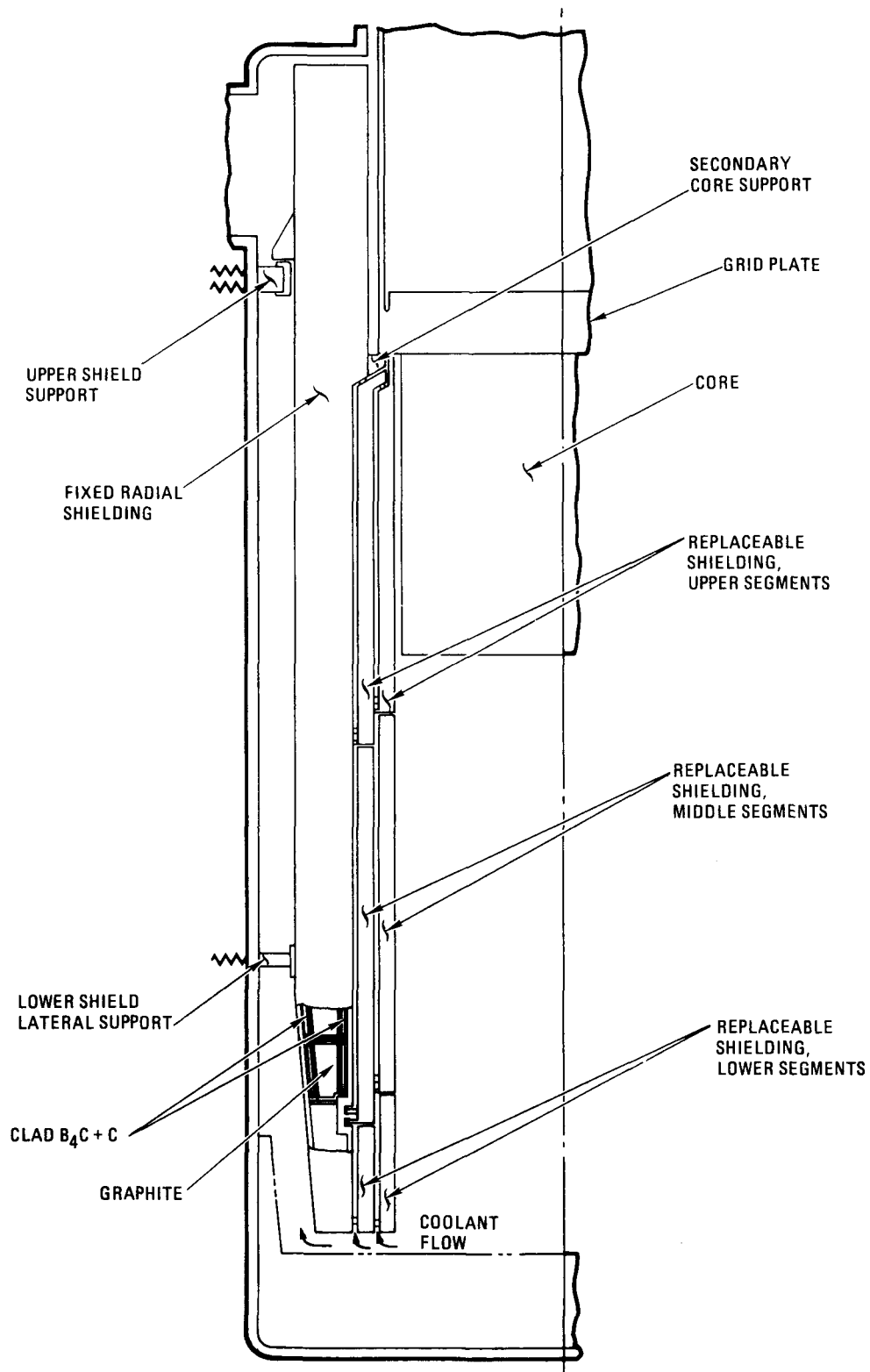


Fig. 10-16. Lower plenum shielding reference design

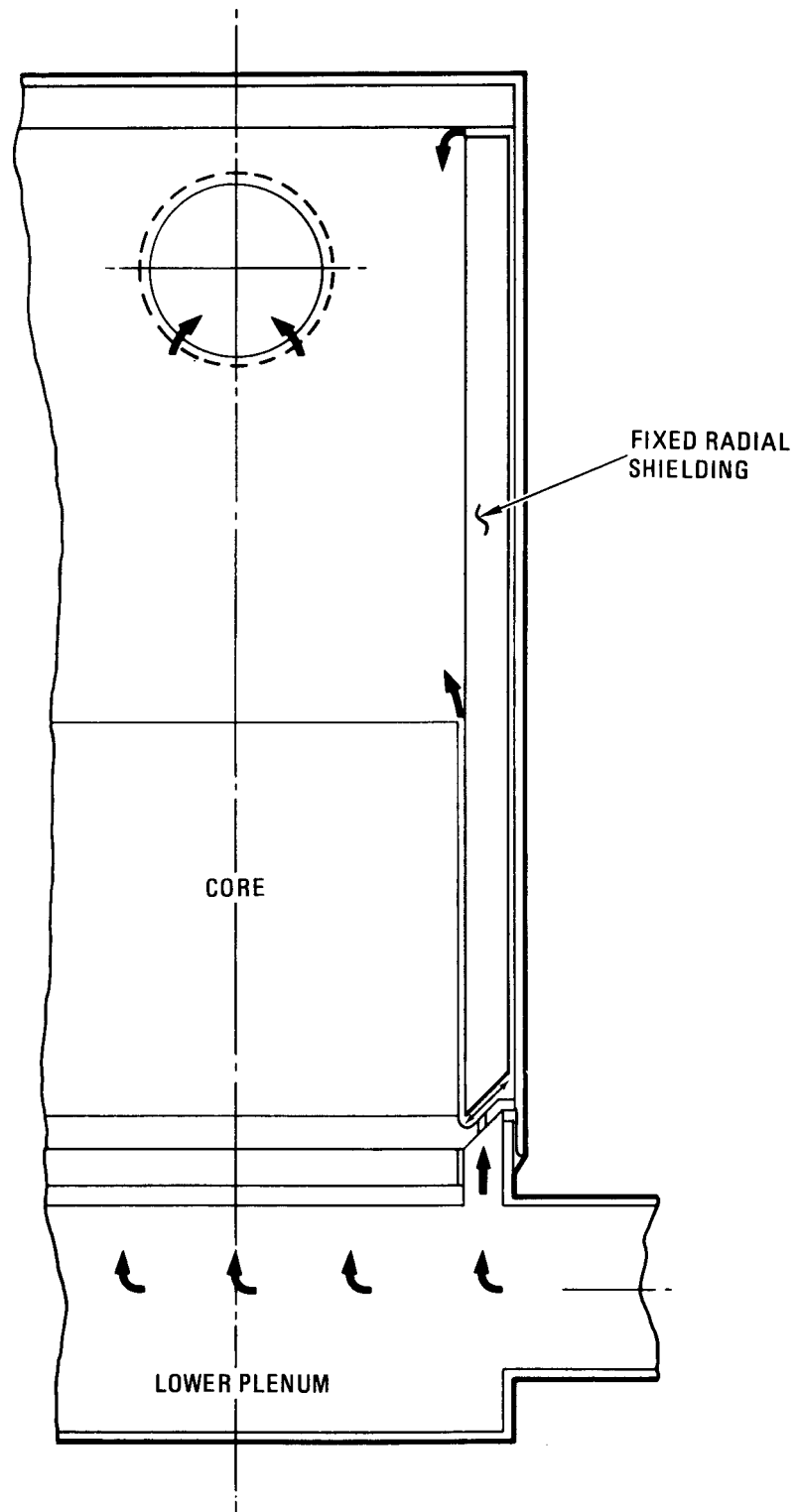


Fig. 10-17. Radial shielding concept for up-flow configuration

3. Gap between the boronated graphite cladding and the structural steel shell.
4. Bypass flow rates.

Preliminary results indicate excessively high temperatures in the stainless steel in some areas and in the boronated graphite.

10.5.2. Up-Flow Configuration Thermal Analysis

An analysis similar to that performed for the reference design was performed for the up-flow configuration shown in Fig. 10-17. Preliminary results indicate that both steel and boronated graphite temperatures are close to the acceptable levels for the bypass flows chosen.

10.6. MAIN HELIUM CIRCULATOR, VALVE AND SERVICE SYSTEM

The purpose of this subtask is to develop the helium circulator, its service system, and main loop isolation valve to demonstrate performance and reliability by testing under anticipated operating conditions. The overall objective for FY-78 is to continue first-of-a-kind conceptual design and performance analysis of the circulator reference design configuration (external steam drive), the service system, the loop isolation valve, and the alternate electric drive selected from studies made in FY-77. Preliminary layouts of the circulator components will be made and requirements established for bearings, shaft critical speeds, seal flow rates, helium buffer systems, drains and jet pumps, inlet and diffuser configuration, shaft coupling and rotor dynamics, aerodynamic performance, and model test requirements for compressor and diffuser. The work outlined will provide input to the circulator pretest analysis task and test facility requirements.

10.6.1. Main Helium Circulator

Four systems were considered in a final evaluation of the alternate GCFR main helium circulator and drive. A design study of an electric motor drive was completed by an electric motor manufacturer, an external steam turbine drive was evaluated by GA, and an architect-engineering firm completed a preliminary design of a full-power circulator test facility. A conceptual design was completed for a single-stage radial flow compressor supported by its own shaft and water-lubricated bearings. Its design speed is 3600 rpm, which matches the speed of the electric motor and the external steam turbine drive; a solid shaft coupling connects the compressor with the driver. The rotor dynamics of the overall system was evaluated as well as bearings, shaft seals, and auxiliary service systems. General operating conditions, including normal and accident conditions, were reviewed, and alternate pony drives investigated for operating the main circulators during certain accident and low-speed operating conditions.

Full-power testing of the circulators and preoperational testing in the reactor was investigated, and a preliminary design was made for a new test facility using an external electric motor drive (Ref. 10-6).

An overall evaluation of each circulator configuration was performed. This evaluation considered technical feasibility and difficulties; safety; licensing; reliability; development; commercialization; power plant operation and capital cost; maintenance; test facility power sources; preoperational testing; motor vendor reliability; development costs and schedules; etc. (Refs. 10-7 through 10-12). At this time it appears that a recommendation will be made to change the GCFR main circulator reference design to a radial flow compressor and an external electric motor drive and discontinue work on the steam turbine drives. The integral steam turbine drive will become the backup system; no further development work is required at present.

10.6.2. Evaluation and Recommendations

In the past 18 months, extensive studies have been made of alternate GCFR main helium circulators and drives. Four of the most promising candidate systems were evaluated in Ref. 10-7:

1. Two-stage axial flow compressor integrated with a two-stage axial flow steam turbine drive in series with the main plant turbine; design speed = 6000 rpm.
2. Single-stage radial flow compressor with a submerged electric induction motor drive; design speed = 3600 rpm.
3. Single-stage radial flow compressor with external eight-stage steam turbine drive in series with the main plant turbine; design speed = 3600 rpm.
4. Single-stage radial flow compressor with an external synchronous electric motor drive; design speed = 3600 rpm.

None of the four final candidate systems could be eliminated by considering the key feasibility issues. The main considerations affecting the final choice of drive system were (1) test facility power source, (2) full-flow preoperational testing, (3) overall plant availability and reliability, (4) development costs and schedules, and (5) commercial plant issues.

The single-stage radial flow compressor and the external electric motor drive were selected as the recommended GCFR main circulator configuration. An electric motor manufacturer has demonstrated sufficient capability to develop and deliver electric motor drives and control systems for the circulator test facility, the GCFR demonstration plant, and the GCFR commercial plants. The manufacturing costs of the motor and its control system are offset by the lower costs of the test facilities and preoperational testing. In addition, circulator developments tests and preoperational

tests are simplified. The most stringent preoperational test requirements for the commercial plant can be easily satisfied with the external electric motor drive. Sufficient confidence has been achieved so that the external synchronous motor and its control system can be designed, built, and delivered according to the schedules for GCFR main circulator development, testing, and installation in the GCFR demonstration plant and subsequent commercial plants (Ref. 10-12). The integral steam turbine drive will remain a viable backup system, but no further work will be required on it as long as the design and manufacture of the electric motor drive proceeds without difficulty.

10.6.3. Selected Design Concept

The new GCFR main helium circulator reference design with an external electric motor drive is shown in Fig. 10-18. This design incorporates the following changes from Ref. 10-1:

1. A 3600-rpm design point speed has been selected because it is the highest speed preferred by the manufacturer of the electric drive motor.
2. The compressor bearing assembly has been redesigned and the compressor assembly thrust bearing deleted along with the other changes.
3. The electric drive motor dimensions are as received from the motor vendor for the main drive motor, the exciter, and the pony motor assembly.
4. The high-pressure rotating shaft seal design is as received from the seal vendor.

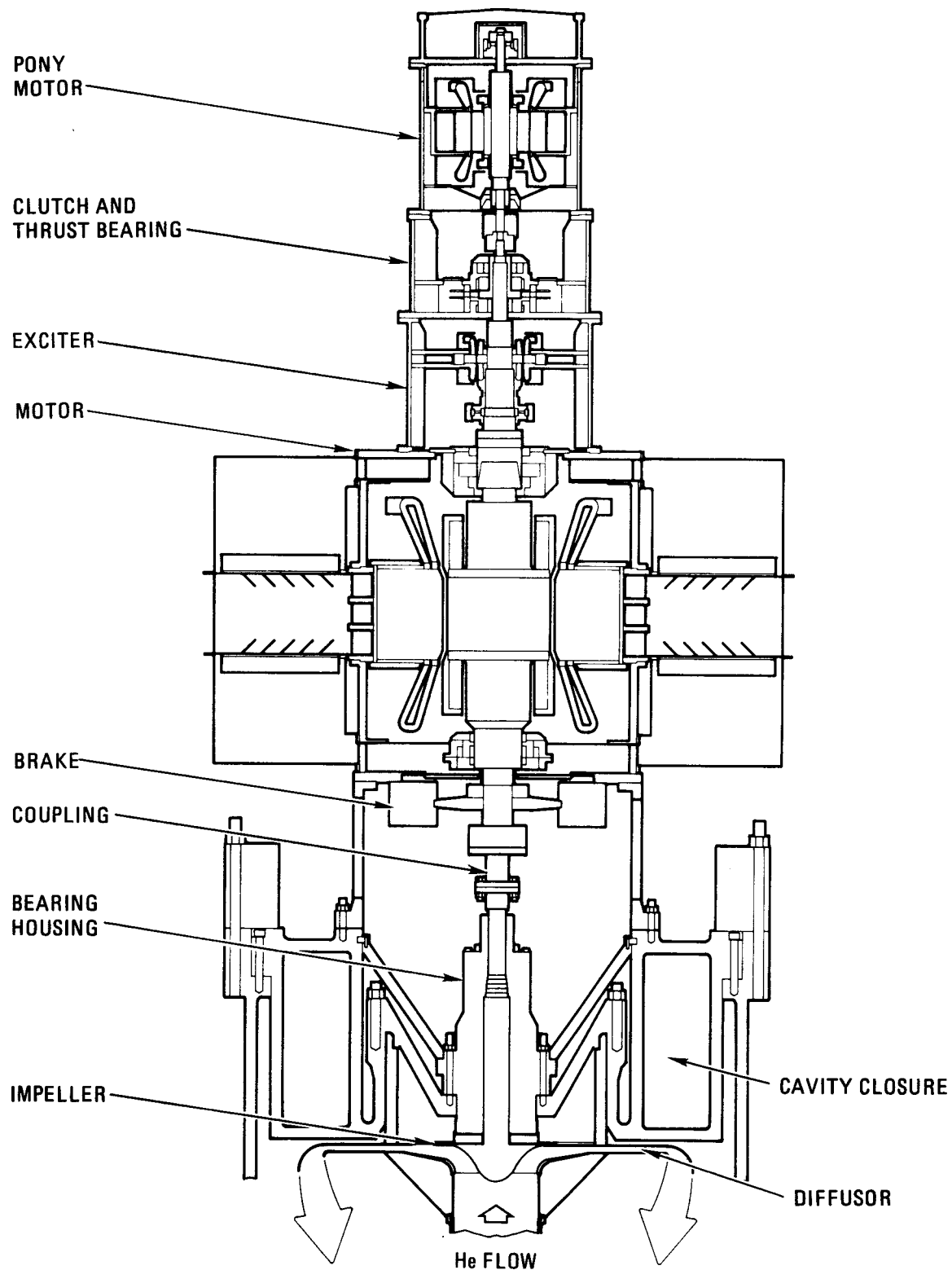


Fig. 10-18. Main helium circulator installation

The compressor assembly is installed in a central hole in the steel closure plug for the steam generator cavity of the PCRV. All rotating parts of the compressor assembly can be removed without removing the steel closure plug. The compressor bearing housing has been structurally redesigned owing mainly to elimination of the compressor assembly thrust bearing. All circulator thrust loads are now carried by the thrust bearing in the electric motor. The compressor journal bearings are water-lubricated hybrid bearings similar to those used on previous circulators. The impeller end journal bearing is 230 mm (9 in.) in diameter, and the drive end bearing 165 mm (6.5 in.). The bearings have been sized so that the first lateral critical speed will be at least 1.15 times the maximum operating speed.

Two bellows-actuated face seals are installed in series at the impeller end of the compressor bearing assembly. These seals isolate the bearing assembly from the PCRV when the main circulator is shut down. Water-driven jet pumps and a buffer helium system are used for impeller end isolation when the circulator is operating. At the drive end, a multistage mechanical face seal is used to seal the high-pressure bearing water when the circulator is operating. The seal is similar to those used for high-pressure shaft seals on the primary coolant pump in LWRs. Because there is some rubbing in these face seals, they must be periodically replaced. Seal life has averaged 2 yr in LWR applications. The seal can be replaced in less than 8 hr without removing either the motor or the compressor by using a removable section in the shaft coupling. The shaft coupling rigidity connects the compressor and motor shafts, and precise alignment of these components is required during installation. A removable section in the coupling is utilized for replacing the compressor shaft seal. A pneumatically actuated brake stops the circulator at 10% speed. The brake also keeps the shaft from rotating when the circulator is shut down and the shutdown face seals are set.

Westinghouse Electric Corporation has completed a study of the electric drive motor. The motor has the following characteristics:

1. 24,000-hp synchronous, round-rotor type, totally enclosed motor with vertical shaft, air-to-water discharge heat exchanger, brushless exciter, guide bearings, and holding brake.
2. Rated speed = 3600 rpm.
3. Two-pole.
4. 75 Hz, 4160 V.
5. 0.95 power factor.
6. Three-phase.
7. Temperature rise by resistance.
8. Maximum temperature on stator = 75°C.
9. Variable-speed operating capability from 0 to 3600 rpm, 0 to 60 Hz.

The motor bearings are lubricated by oil which is circulated by an integral shaft-driven viscosity pump and cooled by an oil-to-water heat exchanger. A separate, smaller, safety-related pony motor is mounted above the main drive motor. The pony motor drives the main motor through an overrunning clutch and is sized to drive the circulator for RHR operations when the reactor is shut down, including during refueling. The pony motor is rated at 300 hp at 1800 RPM.

10.6.4. Aerodynamic Design

10.6.4.1. Compressor Designs for Lower Speeds. Designs of centrifugal compressors for speeds of less than 3600 rpm were investigated for the design condition corresponding to the full-power pressurized reactor

condition and design speeds of 2800 to 3600 rpm. Impellers with vane exit angles of 50, 90 (radial), and 120 deg measured from the tangential direction opposite to rotation were included. Vane exit angles greater than 90 deg are reluctantly used by manufacturers of conventional machinery because of the uncertainty of their effect on flow within the impeller. The 120-deg exit angle was used to demonstrate it as a means of producing a reduced impeller tip diameter in a compressor design at lower speed.

10.6.4.2. Circulator Performance Prediction Methods. To facilitate the use of circulator performance predictions in overall system analysis programs, a new version of the compressor performance prediction program PREDM (Ref. 10-13) was developed. This version calculates additional normalized performance parameters, the most important of which are the pressure rise coefficients ψ and ψ^* and the flow coefficients ϕ and ϕ^* . These are defined as follows:

$$\phi \equiv \frac{240}{\pi^2} \cdot \frac{1}{D_{\text{tip}}^3} \cdot \frac{1}{N} \cdot \frac{\dot{m}}{\rho} \cdot C_{B1} \quad ,$$

$$\psi \equiv \frac{7200}{\pi^2} \cdot \frac{1}{D_{\text{Hp}}^2} \cdot \frac{1}{N^2} \cdot \frac{\Delta P}{\rho} \cdot C_{B2} \quad ,$$

where D_{tip} = compressor wheel tip diameter [m (ft)],

N = compressor speed (rev/min),

$\dot{m}$ = compressor mass flow rate [kg/s (lbm/s)],

ρ = coolant density [kg/m^3 (lbm/ft³)],

ΔP = pressure rise [Pa (psi)],

C_{B1} = conversion constant for British units = 1.0,*

C_{B2} = conversion constant for British units = 4636.8.*

*For metric units, C_{B1} and C_{B2} are unity.

$$\phi^* = \phi/\phi_0 \quad ,$$

$$\psi^* = \psi/\psi_0 \quad ,$$

where ϕ_0 = value of ϕ at the design point,

ψ_0 = value of ψ at the design point.

The use of these parameters permits representation of compressor performance over a range of speeds by means of a single line plot. The program prints these parameters together with 19 others for each operating point analyzed. It can also record these values, if required, on a magnetic drum file for easy access by the system performance analysis program.

The original version of the program has the capability of using metric units as well as British units. However, the metric units are the older M-K-S units (kg-force, kgf/cm², etc.) rather than the preferred S.I. units. Therefore, computations and printing formats were changed to conform to the S.I. standard.

10.6.5. Electric Motor Drive

Phase I of the GCFR demonstration plant circulator motor study was completed. This motor will be externally mounted. Details of this study are reported in Ref. 10-6. A 17,700-kW motor at 4500 rpm was considered to reduce the circulator compressor diameter and improve the dimensional constraints for circulator installation in the reactor. However, it was concluded that a 3600-rpm design speed with no overspeed capability would be the highest speed possible for the 300-MW(e) GCFR demonstration plant. A design with the main motor body critical speed in the operating range and the exciter critical speed above the operating range should be considered so that the self-lubricated motor bearings and realistic parameters discussed in Ref. 10-6 can be used. This approach is possible for the electric motor; however, the main circulator assembly would have to be designed to have its first critical speed above the operating range.

10.7. STEAM GENERATOR

The purpose of the steam generator is to transfer heat from the reactor primary coolant (helium) to the secondary coolant (water/steam) during normal plant operation. In addition, the steam generator provides a means of reactor core heat removal after normal shutdown and during abnormal operating conditions. The objective of this subtask is to design and develop a steam generator which meets the operational, performance, and safety requirements of the GCFR. The current design, a once-through steam generator, utilizes a helical tube bundle for active heat transfer. It consists of an economizer, an evaporator, and a superheater section. The feedwater enters from the bottom of the steam generator and evaporates into superheated steam at the top of the helical bundle. The superheated steam is routed down a straight tube section and out through a tube sheet. The helium flow (primary coolant) is arranged to form a counter-current pattern with respect to water flow.

At this point in the design evolution, the steam generator general arrangement features a bottom-supported helical bundle with straight superheater lead-out tubes in the center of the steam generator cavity (Fig. 10-19). The feedwater expansion loops are in the immediate vicinity of the lower penetrations, and the superheat expansion loops are located between the top of the main active helical bundle and the straight lead-out section. A flow device, tentatively identified as a perforated plate, is furnished to smooth out velocity maldistribution in the helium inlet flow. The necessity of having such a flow device has yet to be proven, and a test program is required to determine the helium inlet flow velocity distribution and, if necessary, to develop an optimum flow device design which will accomplish uniform flow distribution at the least expense of pressure drop.

The main concern in locating the main support flange at the bottom of the helical bundle is its capability to withstand seismic load. A preliminary seismic evaluation was made using the results obtained from a previous analysis of a similar HTGR lead plant unit (Ref. 10-14). Based on previous

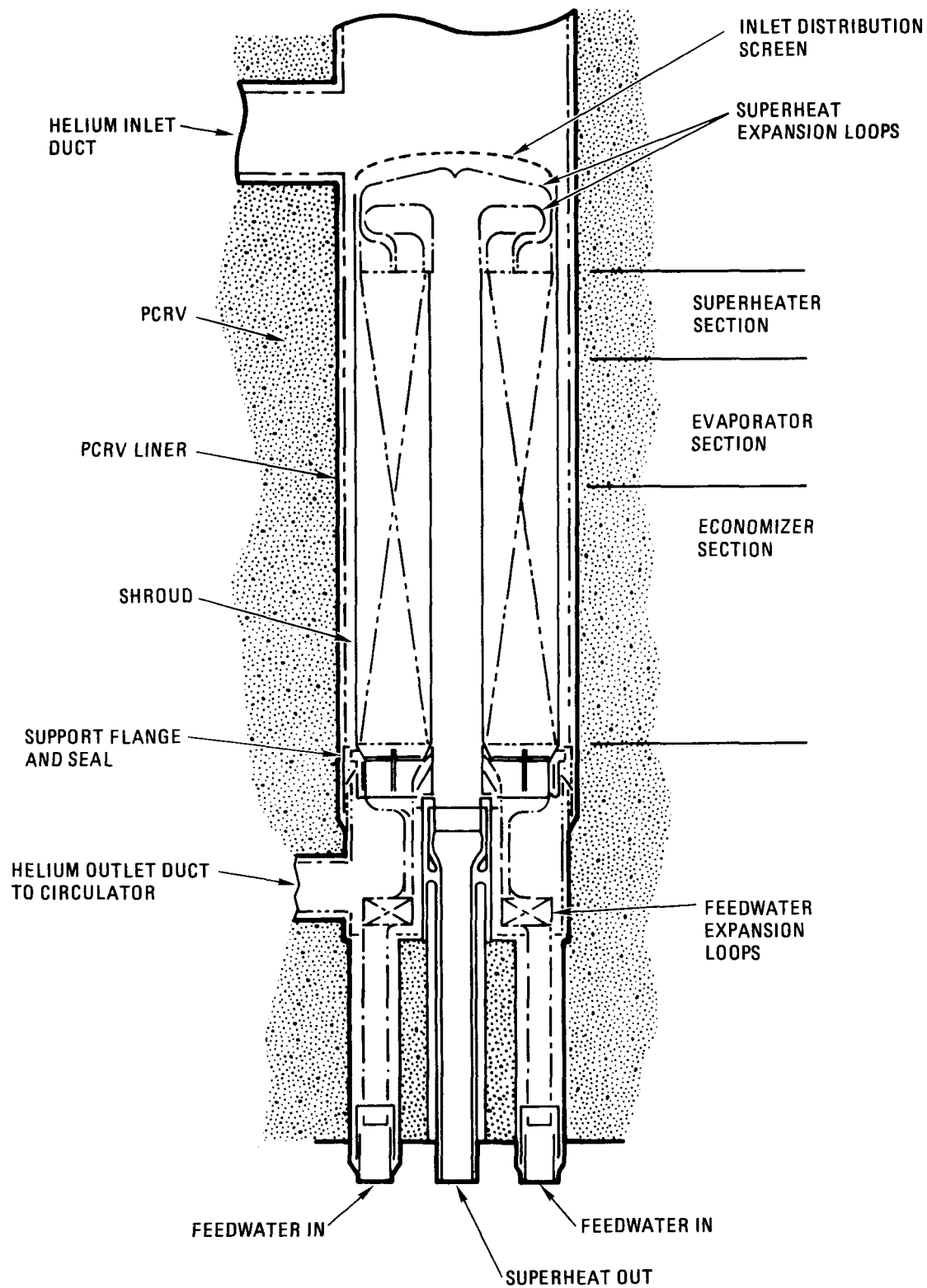


Fig. 10-19. Steam generator

experience, the critical location for seismic stress is in the tubes and the support plate ligament. The seismic stress at these locations were estimated based on an operating basis earthquake (OBE) 0.12-g loading with a 2% damping factor. The results indicate that it is satisfactory to have the main support flange located at the bottom of the bundle.

A detailed layout of the steam generator support design concept was made. The design employs heavy radial gusset plates which connect the inner and outer shrouds to support the entire helical bundle. Each two neighboring gusset plates are connected by a cross member to further strengthen the gusset plates by eliminating lateral buckling.

Owing to the large coil outer to inner diameter ratio, the bear-hug stress within the steam generator bundle is relatively high. However, the small temperature difference between the tube and support plates may allow the bundle to have a large outer to inner diameter ratio. Work was initiated to generate helical tube bundle thermal expansion stress design curves which will facilitate tube stress calculations during thermal performance studies. This task was performed using the CSTRES computer code (Ref. 10-15) and a typical temperature distribution. Preliminary results indicate that the analysis will need modifying factors to relate temperature differences between tubes and support plates at the top of the superheater and at the transition between the superheater and evaporator. The maximum tubing stress is inversely proportional to the vertical length of the superheater section, requiring further design equation modification.

The tubing material is 2-1/4 Cr - 1 Mo, and the tube sheet material is alloy 800H. The location of the bimetallic weld has not been determined. This location is primarily governed by metallurgical criteria. At the bimetallic weld, the tube wall maximum temperature must not exceed 482°C (900°F) and the steam should be at least 28°C (50°F) superheated. Steam generator temperature distributions corresponding to 100% and 25% loads were generated, and they showed that there is no place in the bundle where both conditions can be met for these loads. If the bimetallic weld is located

at the tube sheet, an intermediate section, probably made of 9 Cr, will be needed to cope with a temperature higher than 482°C (900°F). [The nominal steam temperature at the tube sheet is 510°C (950°F)].

Work has begun to support the up-flow core study. A top exhaust steam generator was identified as a possible candidate for an up-flow PCRV configuration. Prominent problem areas were identified:

1. The feasibility of an expansion loop design capable of accommodating the conflicting requirements of the rigidity required to withstand seismic excitation and the flexibility required to release the thermal growth.
2. The interaction of outer shroud bowing with the steam generator main support flange connection or lateral seismic support.

Analyses will be performed to determine the acceptable bundle height and expansion loop geometry and to explore the technical feasibility of a top exhaust steam generator.

RECS, which covers planning in design, analysis, design verification and support, fabrication, and documentation, was updated. Manpower requirements and work duration were estimated for the major tasks required to develop and fabricate the GCFR steam generator.

10.8. AUXILIARY CIRCULATOR, VALVE AND SERVICE SYSTEM

The rotor dynamics of the auxiliary circulator shaft system have been determined, and the life of the rolling element bearings has been estimated to study the feasibility of an idling CACS. The bearings are the main components whose fatigue life is affected by a continuously idling CACS.

10.8.1. Rotor Dynamics of the Auxiliary Circulator

Two computer codes, FLEXEV and ROTOR3 (Refs. 10-16, 10-17), were used in the analysis of the GCFR auxiliary circulator shaft system. FLEXEV is an eigenvalue code which can be used to determine the critical speeds and mode shapes of the rotor. It can also be used to narrow the range of bearing stiffnesses necessary to achieve a certain critical speed margin. ROTOR3 is an unbalance response code which must be run over a range of speeds to determine the maximum unbalance response. Comparison of the critical speeds determined by the two codes yielded a difference of less than 1%.

Table 10-1 shows the variation of critical speed with bearing stiffness as computed by ROTOR3. For a 3600-rpm rotor, it is necessary to have a lower bearing stiffness of 0.88×10^9 N/m (5×10^6 lb/in.) and an upper bearing stiffness of 0.18×10^9 N/m (1×10^6 lb/in.) to achieve a critical speed of 4350 rpm with a 203.2-mm (8-in.) shaft. For a 4500-rpm rotor, it is necessary to have much larger shaft diameters to achieve adequate critical speed margins. From the point of view of the shaft system, it seems to be much more difficult to design a 4500-rpm impeller.

10.8.2. Rolling Element Bearing Life

The auxiliary circulator motor is supported at each end with a rolling element antifriction bearing, with the impeller overhung from the lower end bearing. The rotor is vertically mounted, and the thrust load is taken by a pair of tandem-mounted ball bearings at the compressor end. The upper end alignment is provided by a roller bearing. One of the schemes presently being studied is to idle the auxiliary circulator at a low speed for the life of the plant to reduce the time necessary to accelerate the circulator to full speed after initiation of a depressurization accident. An important issue upon which the feasibility of this scheme rests is the life of the bearings, especially the tandem-mounted thrust ball bearings. The roller bearing is not expected to pose a problem, since it does not carry any

TABLE 10-1
GCFR AUXILIARY CIRCULATOR CRITICAL SPEED
VERSUS BEARING STIFFNESS (ROTOR3)

Bearing Stiffness [N/m (lb/in.)]	Critical Speed (rpm)
∞	4900
0.88×10^9 (5×10^6)	4550
0.88×10^9 , 0.18×10^9 (5×10^6 , 1×10^6)	4350
0.35×10^9 (2×10^6)	4000
0.18×10^9 (1×10^6)	3300

appreciable radial load. Calculations indicate that the bearing will have an adequate life when idled continuously at 5% or even 10% speed for the entire life of the plant.

10.8.3. Auxiliary Loop Isolation Valve

A study has been completed to determine flow-differential pressure characteristic as a function of the position of the split butterfly valve used for the GCFR auxiliary loop. Data obtained from Fort St. Vrain valves (which are similar to the GCFR valve) were used to generate the characteristic curve. The result will be used to model GCFR plant transients more accurately.

10.9. HELIUM PROCESSING COMPONENTS

The design criterion for the HPS (Ref. 10-18) has been completed, and conceptual system flow diagrams are being developed. The information obtained to date is presented in Fig. 10-20 and Tables 10-2 and 10-3. System components have not been sized, and the sizing data presented are preliminary. The HPS purifies helium drawn from the primary coolant system and the PES. These helium streams are processed to remove particulates, chemical impurities, and radioactive impurities so that the resulting purified helium can be safely used as a clean gas purge where it is needed throughout the plant. The conceptual design of the HPS includes features which

1. Provide sufficient sweep gas flow for the PES to accommodate the transfer of gaseous activity released from the vented fuel.
2. Maintain the concentrations of chemical impurities in circulating helium at acceptable levels.
3. Maintain the concentrations of radionuclides with long half-lives in the circulating helium at acceptable levels.

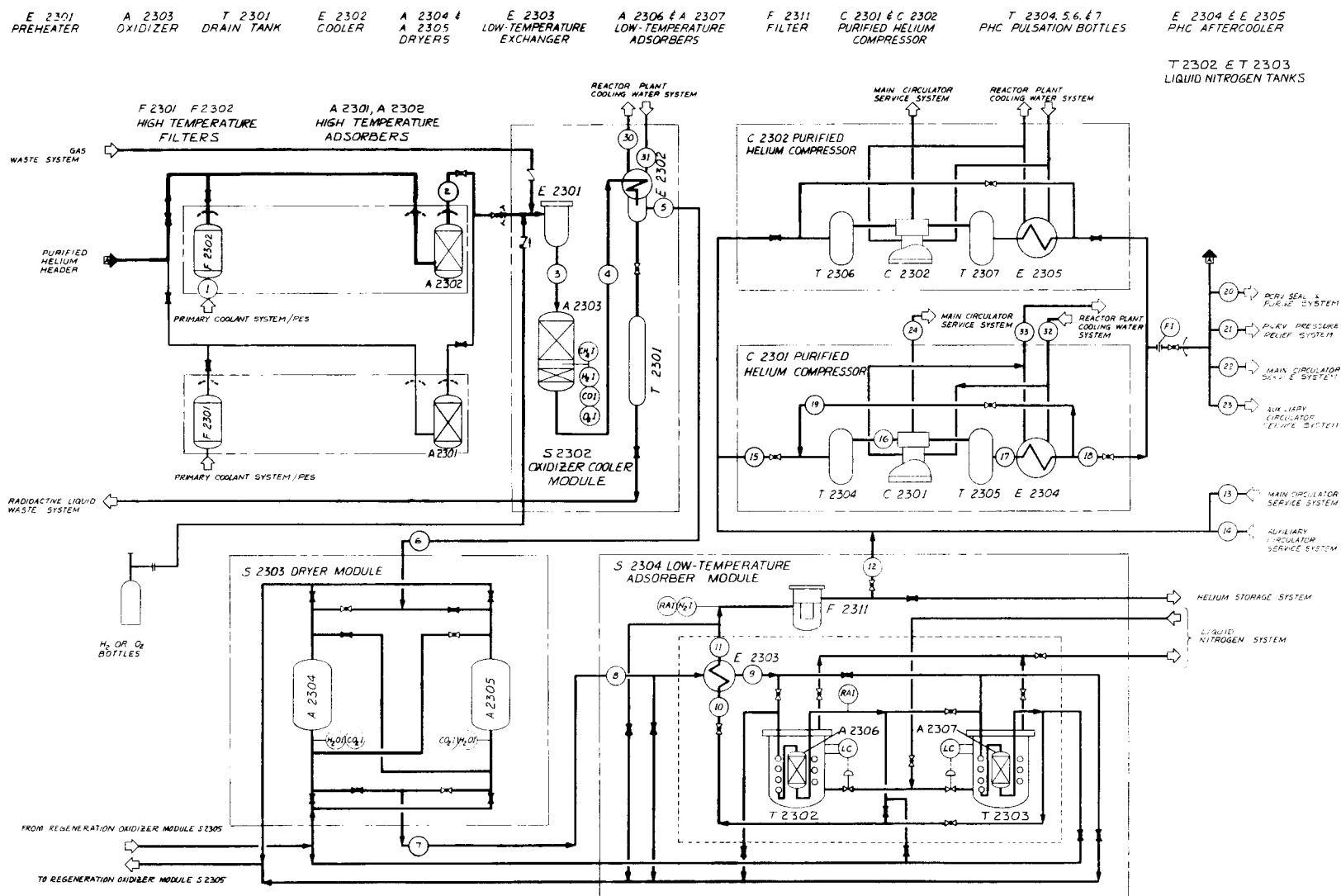


Fig. 10-20. Helium purification system (sheet 1)

E 2308 REGENERATION COOLER C 2303 REGENERATION COMPRESSOR A 2310 REGENERATION DRYER E 2309 REGENERATION HEATER A 2308 PRECIOUS METAL CATALYTIC CONVERTER E 2306 OXIDIZER ECONOMIZER A 2309 COPPER METAL REDUCER E 2307 OXIDIZER PREHEATER

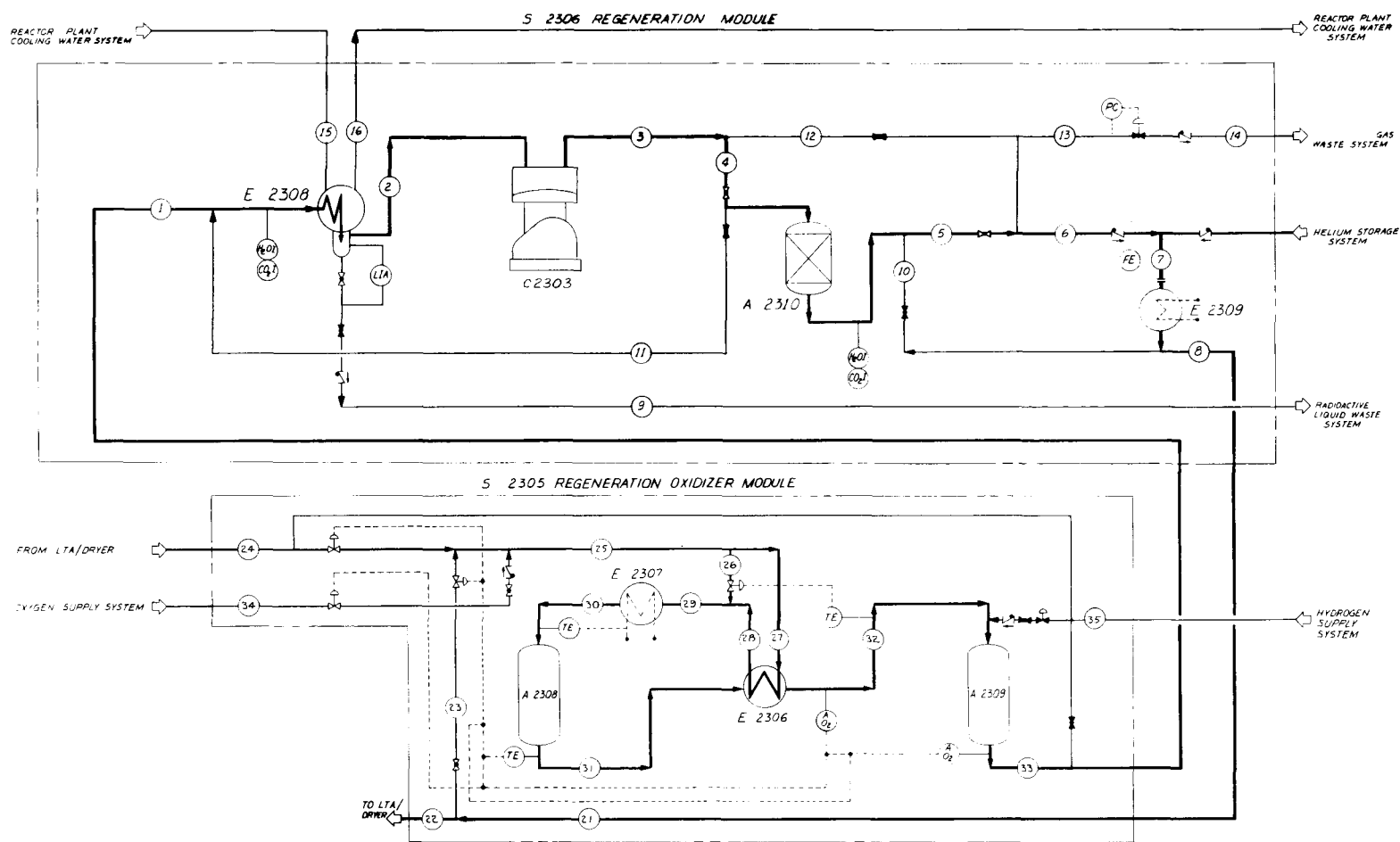


Fig. 10-20. Helium purification system (sheet 2)

TABLE 10-2
FLOW CONDITIONS FOR THE PROCESS SECTION OF THE HELIUM PURIFICATION SYSTEM^(a)

	Point No.															
	1	2	3	4	5	6	7	8	9	10	11	12	13	14	15	16
Flow rate [kg/s (lb/hr)]	0.328 (2600)	0.328 (2600)	0.328 (2600)	0.328 (2600)	0.328 (2600)	0.328 (2600)	0.328 (2600)	0.328 (2600)	0.328 (2600)	0.328 (2600)	0.328 (2600)	0.328 (2600)	0.096 (760)	0.064 (506)	0.487 (3866)	0.542 (4300)
Pressure [MPa (psia)]	8.95 (1298)	--	--	--	--	--	--	--	--	--	--	--	--	--	--	--
Temperature [°C (°F)]	316 (600)	316 (600)	316 (600)	316 (600)	49 (120)	49 (120)	49 (120)	49 (120)	-182 (-295)	-190 (-310)	41 (105)	41 (105)	49 (120)	49 (120)	43 (110)	44 (111)
Composition	He	He	He	He	He	He	He	He	He	He	He	He	He	He	He	He
Contaminants ^(b)	A	B	B	C	C	C	D	D	D	None	None	None	None	None	None	None
	Point No.															
	17	18	19	20	21	22	23	24 Through 29	30	31	32	33				
Flow rate [kg/s (lb/hr)]	0.542 (4300)	0.487 (3866)	0.055 (434)	0.074 (587)	0.094 (750)	0.191 (1519)	0.096 (760)	--	13.10 (104,000)	13.10 (104,000)	1.49 (11,800)	1.49 (11,800)				
Pressure [MPa (psia)]	--	--	--	--	--	--	--	--	0.48 (70)	0.38 (55)	0.79 (115)	0.72 (105)				
Temperature [°C (°F)]	61 (142)	49 (120)	49 (120)	49 (120)	49 (120)	49 (120)	49 (120)	--	46 (115)	54 (130)	46 (115)	52 (125)				
Composition	He	He	He	He	He	He	He	--	H ₂ O	H ₂ O	H ₂ O	H ₂ O				
Contaminants	None	None	None	None	None	None	None	None	None	None	None	None				

^(a) See Fig. 10-20, Sheet 1.

^(b) A = particulates I₂, H₂, CO, CH₄, CO₂, H₂O, N₂, Kr, Xe, O₂; B = H₂, CO, CH₄, CO₂, H₂O, N₂, Kr, Xe, O₂; C = CO₂, H₂O, N₂, Kr, Xe, CH₄; D = N₂, Kr, Xe, CH₄.

TABLE 10-3
FLOW CONDITIONS FOR THE REGENERATION SECTION OF THE HELIUM PURIFICATION SYSTEM^(a)

	Point No.															
	1	2	3	4	5	6	7	8	9	10	11	12	13	14	15	16
	Case A: regeneration of a purification dryer															
Flow rate [kg/s (lb/hr)]	0.021 (165)	0.021 (165)	0.021 (165)	0.021 (165)	0.021 (165)	0.021 (165)	0.021 (165)	0.021 (165)	0.006 (50)	--	--	--	(b)	(b)	0.912 (7240)	0.912 (7240)
Pressure [MPa (psia)]	4.82 (699.3)	4.82 (699.3)	4.83 (701.0)	4.83 (701.0)	4.83 (700.9)	4.83 (700.9)	4.83 (700.8)	4.83 (700.0)	4.81 (698.3)	4.83 (700.9)	4.82 (699.3)	4.83 (701.0)	4.83 (700.9)	(c)	0.48 (70)	0.46 (66)
Temperature [°C (°F)]	316 (600)	52 (125)	54 (130)	54 (130)	54 (130)	54 (130)	54 (130)	316 (600)	54 (130)	54 (130)	54 (130)	54 (130)	54 (130)	54 (130)	46 (115)	54 (130)
Composition	He	He	He	He	He	He	He	He	H ₂ O	He	He	He	He	He	H ₂ O	H ₂ O
	Case B: regeneration of regeneration dryer															
Flow rate [kg/s (lb/hr)]	--	0.021 (165)	0.021 (165)	--	--	0.021 (165)	0.021 (165)	--	0.006 (50)	0.021 (165)	0.021 (165)	0.021 (165)	(b)	(b)	0.912 (7240)	0.912 (7240)
Pressure [MPa (psia)]	--	4.82 (699.8)	4.83 (701.0)	--	--	4.83 (700.9)	4.83 (700.9)	--	4.82 (698.8)	4.83 (700.0)	4.82 (699.8)	4.83 (701.0)	4.83 (701.0)	(c)	0.48 (70)	0.46 (66)
Temperature [°C (°F)]	--	52 (125)	54 (130)	--	--	54 (130)	54 (130)	--	54 (130)	316 (600)	316 (600)	54 (130)	54 (130)	54 (130)	46 (115)	54 (130)
Composition	He	He	He	He	He	He	He	He	H ₂ O	He	He	He	He	He	H ₂ O	H ₂ O
	Point No.															
	21	22	23	24	25	26	27	28	29	30	31	32	33	34	35	
	Case C: regeneration of a low-temperature adsorber															
Flow rate (kg/s (lb/hr))	0.02 (165)	0.002 (15)	0.019 (150)	0.002 (150)	0.021 (165)	(c)	(c)	(c)	0.021 (165)	0.021 (165)	0.021 (165)	0.021 (165)	0.021 (165)	(c)	(d)	
Pressure [MPa (psia)]	4.83 (700.5)	4.83 (700.5)	4.83 (700.5)	4.83 (700.0)	4.80 (696.0)	4.80 (696.0)	4.76 (691.0)	4.76 (691.0)	4.76 (691.0)	4.76 (690.5)	4.76 (690.0)	4.72 (685.0)	4.75 (689.5)	4.85 (704.0)	--	
Temperature [°C (°F)]	177 (350)	177 (350)	177 (350)	-196 to 149 (-320 to 300)	143 to 177 (290 to 350)	43 to 177 (110 to 350)	116 to 66 (240 to 150)	143 to 177 (290 to 350)	366 (690)	482 (900)	538 (1000)	316 (600)	343 (650)	38 (1000)	38 (100)	
Composition(e)	A	A	A	B	C	C	C	C	C	C	D	D	E	O ₂	H ₂	

(a) See Fig. 10-21, sheet 2.

(b) Intermittent.

(c) Variable.

(d) Used only during regeneration of copper metal oxidizer.

(e) A = He, Xe, Kr, N₂; B = He, Xe, Kr, N₂, CH₄, H₂; C = He, Xe, Kr, N₂, CH₄, H₂; D = He, Xe, Kr, N₂, H₂O, CO₂, O₂; E = He, Xe, Kr, N₂, H₂O, CO₂, H₂.

4. Purify all helium before it is transferred to storage.
5. Provide sufficient purified helium for purging PCRV penetrations.
6. Provide for timely depressurization of the PCRV.
7. Maintain the PCRV slightly subatmospheric during refueling (to prevent out-leakage of gaseous fission products).
8. Process influent flow from the gas waste system for the purpose of separating and retaining gaseous fission products and recovering helium in purified form.

The HPS purifies helium during normal plant operation at a rate of 0.327 kg/s (2600 lb/hr). In addition, the system compresses 0.160 kg/s (1266 lb/hr) of pure helium recovered from the main and auxiliary circulator service systems, resulting in a system discharge flow of 0.487 kg/s (3866 lb/hr). These flow rates correspond to a main circulator discharge pressure of 9.053 MPa (1313 psia). Most of the influent flow from the PCRV is directly routed to the HPS, with the balance coming as fuel assembly sweep gas via the PES.

The HPS includes filters, adsorbers (high-temperature, ambient, and cryogenic), oxidizers, coolers, heaters, and compressors and the associated piping, valving, instrumentation, and controls necessary to process helium and return it to the PCRV. The system is an assembly of five modules plus two high-temperature filters, two high-temperature adsorbers, and two reciprocating compressors. These components are described below. Components designed for removing impurities from the process helium, and whose continued availability cannot be impaired due to impurity buildup, have been spared and can be periodically replaced or regenerated. A spare purified helium compressor has also been provided. Particulate filters which can be returned to acceptable operating conditions by a back-flow operation have not been spared in every case. Controls and instrumentation are provided to ensure safe and reliable operation of the system.

10.9.1. High-Temperature Filters

There are two high-temperature filters (F-2301 and F-2302) located in the penetrations on the top head of the PCRV. The sintered fiber-metal filter media have a particulate removal efficiency of >99.98% for particles with a diameter of 0.3 to 3.0 μ . Each filter has a dust accumulation chamber. The filters can be cleared by back-purging, without the need for interrupting system flow.

10.9.2. High-Temperature Adsorbers

There are two high-temperature adsorbers (A-2301 and A-2302) located in penetrations on the top head of the PCRV. Each adsorber contains impregnated charcoal for chemisorption of volatile electronegative fission products such as iodine.

10.9.3. Oxidizer-Cooler Module

The oxidizer-cooler module (S-2302) contains the following equipment: electrical preheater (E-2301); oxidizer (A-2303); aftercooler (E-2302); and liquid drain tank (T-2301). The oxidizer contains about 1590 kg (3500 lb) of catalyst which oxidizes H_2 to H_2O and CO to CO_2 so that these impurities can be removed by ambient temperature adsorption on a molecular sieve adsorbent. The preheater is used only when the plant is at low power or shut down and the inlet gas temperature drops below the oxidizer operating range. The catalyst can also remove O_2 from the gas stream, such as during refueling, by taking on oxygen to form a catalyst oxide. The catalyst can be reactivated while in service by bleeding low-concentration O_2 or H_2 into the process flow upstream of the oxidizer.

10.9.4 Dryer Module

The dryer module (S-2303) contains two molecular sieve dryers (A-2304 and A-2305). Each dryer contains 1816 kg (4000 lb) of type 5A molecular

sieve and adsorbs H_2O and CO_2 from the process flow. The two dryers operate in series, flip-flop. When the front dryer indicates breakthrough, it is removed from service, regenerated, and returned to service downstream of the other dryer.

10.9.5. Low-Temperature Adsorber Module

The low-temperature adsorber module (S-2304) contains a low-temperature exchanger (E-2303) and two low-temperature adsorbers (A-2306 and A-2307). Each adsorber contains 454 kg (1000 lb) of charcoal. In addition, there are two liquid nitrogen tanks (T-2302 and T-2303) and one filter (F-2311). Less volatile species (i.e., CH_4 , krypton, and xenon) accumulate on the first bed, and more volatile N_2 accumulates on the second bed. The first bed has a long operating life and is expected to be regenerated only once each year.

10.9.6. Compressors

There are two oil-free reciprocating compressors (C-2301 and C-2302). Each compressor is equipped with inlet and outlet pulsation bottles (T-2304, T-2305, T-2306, and T-2307) and water-cooled aftercoolers (E-2304 and E-2305).

10.9.7. Regeneration Oxidizer Module

The regeneration oxidizer module (S-2305) will be designed and fabricated by an outside vendor. This module and the regeneration module (Section 10.9.8) provide for regeneration of the dryers (A-2304 and A-2305), the low-temperature adsorbers (A-2306 and A-2307), and the regeneration dryer (A-2310). Regeneration is accomplished by circulating heated gas through the spent bed to drive off the adsorbed impurities, followed by purging the heated gas and impurities to the gas waste system and subsequent bed cool-down.

10.9.8. Regeneration Module

This regeneration module (S-2304) contains a water cooler (E-2308) and a regeneration heater (E-2309). The compressor (C-2303) is a diaphragm machine, and the molecular sieve dryer (A-2310) contains 1816 kg (4000 lb) of type 5A molecular sieve.

10.10. CONTROL AND ELECTRIC COMPONENTS

10.10.1. Main Circulator Electric Drive Study

Westinghouse Corporation continued its study of large, vertical high-speed motors and static converters suitable for application to the GCFR main helium circulators. The emphasis was on an externally mounted 24,000-hp motor for the demonstration plant. The conceptual design selected from this study is a wound-rotor synchronous motor for the main drive and a squirrel cage induction motor for the pony drive. Variable-speed operation will be obtained by means of dedicated variable-frequency power supplies and static control. This equipment will have capability of starting from a standstill as a synchronous motor without incorporation of additional windings.

Each drive will be self-contained, with a vertical frame arrangement supporting three guide bearings, a thrust bearing, a brushless exciter, and a holding brake on the main drive. The concept has tilting pad type hydrodynamic bearings with integral oil circulating pumps and heat exchangers. This is only possible with the first lateral critical speed within the operating range. Hydrostatic bearings with lubrication supplied at high pressure from a separate source will be required for operation with the first lateral critical speed 115% above the maximum operating speed. The pony motor has two guide bearings and a thrust bearing with integral cooling coils mounted above the main drive and coupled to it through an over-running clutch and flexible coupling.

A separate pony motor of conventional squirrel cage construction was recommended for the alternate drive instead of using the exciter as a motor or using the main stator windings of the main motor drive. This should lead to a simplified qualification procedure, since it is probable that only the pony motor and its controller will be class 1E and the mechanical parts of the main motor (rotor body, bearings, and supports) will be ASME Section III, Class 3.

The recommended cooling method for the external main motor is totally enclosed water/air cooling. Totally enclosing the motor has several advantages, including missile containment and reduction of acoustic and electromagnetic noise. The combination of high operating speed, variable-frequency operation, and static power supply imposes an unusual combination of design constraints and demands a unique selection of design dimensions and parameters, especially with respect to the rotor. The rotor must have service-proven damping provisions to accommodate the harmonic currents introduced by the solid-state power supply without overheating and must be of proven integrity and material quality to prevent missile generation.

The most challenging design issues are those for the bearing system and rotor. In order for the first lateral critical speed to be greater than 115% of the maximum operating speed, very high bearing stiffness is required. Stiffness can be achieved by the use of hydrostatic bearings under constant pressure and by external bracing of the upper bearings to a rigid foundation. Although motors with these features are feasible, Westinghouse has proposed an alternate design with the first lateral critical speed in about the middle of the operating speed range. This design offers the following advantages:

1. Elimination of need for constant pressure source for lubricating fluid for hydrostatic bearings.
2. Elimination of external bracing of upper bearings to rigid foundation.

3. Reduction of rotor body diameter, resulting in (a) reduced windage losses and higher efficiency; (b) reduced stresses on parts, thereby reducing cost of materials; (c) reduced stresses, which may allow elimination of end ring containment shields.
4. Enables a bearing to be designed with sufficient damping within the bearing support so that the effect of a given unbalance at the first critical speed of a flexible system (with the critical speed in the operating range) will be less than the effect of the same unbalance at the rated speed of a more rigid system (with the critical speed above the operating range).
5. Allows the demonstration plant design to be more easily extrapolated to larger sizes, because larger motors can only be built with the first lateral critical speed in the operating speed range.

10.10.2. Motor Missiles

The use of an electric motor drive for the primary coolant circulator has led to a concern for the safety implications of large motor-generated missiles. Westinghouse is aware of this concern and has stated that the electric motor drive can and will be designed so that destructive accidents resulting in penetration of the motor case by the rotor or rotor missiles will not occur. In view of this, and until information to the contrary is available, the design of the electric motor installation will not include motor missile protection external to the motor enclosure.

10.10.3. Power Loading Sequence for Starting Core Auxiliary Cooling System Loops

The required start-up of CACS loops within 60 s imposes abnormal requirements on the IE power system and the CACS equipment. A study was undertaken to determine possible ways to cycle the CACS equipment on line at a fast rate using on-site/off-site power or diesel generators. The CACS equipment can be cycled onto the IE power system to start the CACS

system in different ways. The two methods proposed for sequencing CACS loads are (1) cycling CACS main loads every 5 s or (2) rapidly cycling all CACS loads in less than 5 s using on-site/off-site power or diesel-generated power. The times to energize CACS loads using both cycling techniques are illustrated in Figs. 10-21 through 10-26.

The effect of delays caused by the IE power system and the CACS system equipment was also considered. These delays can be reduced by redesigning control equipment and using oversized power generation and distribution equipment, as discussed in Sections 10.10.3.1 and 10.10.3.2.

10.10.3.1. Core Auxiliary Cooling System Circulator Motor Controller.

Examination of the HTGR CACS circulator motor controller indicates that with some modification, a similar controller could be used to start the GCFR CACS motor in 1 or 2 s.

10.10.3.2. IE Power Systems. The IE power systems which supply electrical power to CACS equipment have been studied to determine the starting time of the diesel generators, how fast the diesel generators can accept CACS loads, and the effect of different loading techniques on IE power system components. The two load-sequencing techniques discussed above can be accomplished with straightforward engineering principles. However, cycling the CACS loads at 5-s intervals (or faster) imposes increased size requirements for certain components in the IE power systems and the plant distribution system.

Application of CACS loads at 5-s intervals (Figs. 10-22, 10-23, and 10-26) will have a minimal effect on the IE power system. However, the diesel generator will probably have to be slightly oversized to handle the loading requirements and have high performance characteristics in starting, accelerating to operating speed, and fast recovery from starting large loads. Colt-Fairbanks & Morse and Cooper Energy Service supply diesels that should meet these requirements. Application of CACS loads at faster intervals (Figs. 10-23, 10-24, and 10-26) will require an

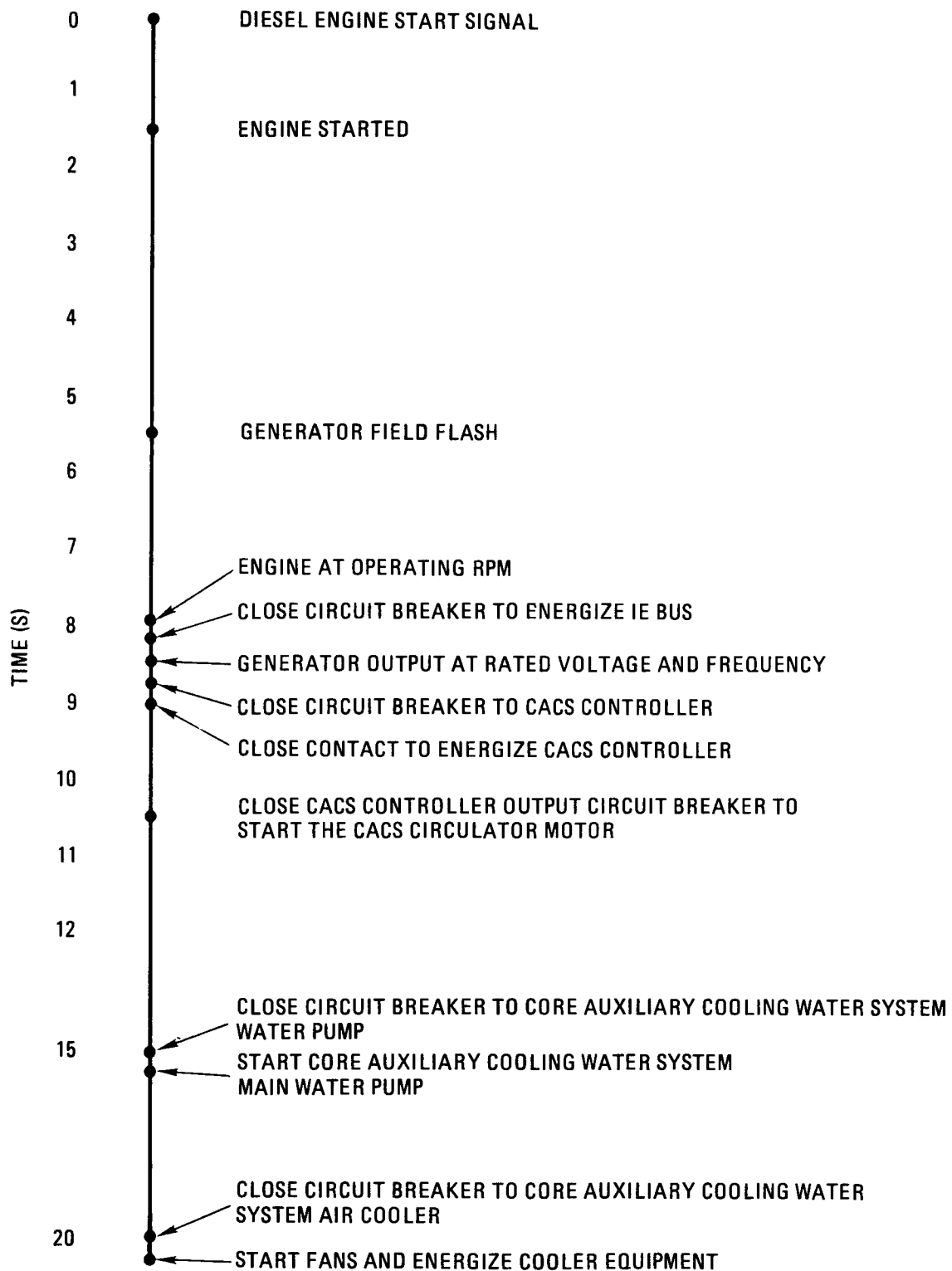


Fig. 10-21. Timing cycle to energize CACS equipment using a diesel generator; a main CACS load is energized every 5 s (diesel generator starting data are based on Ref. 10-19)

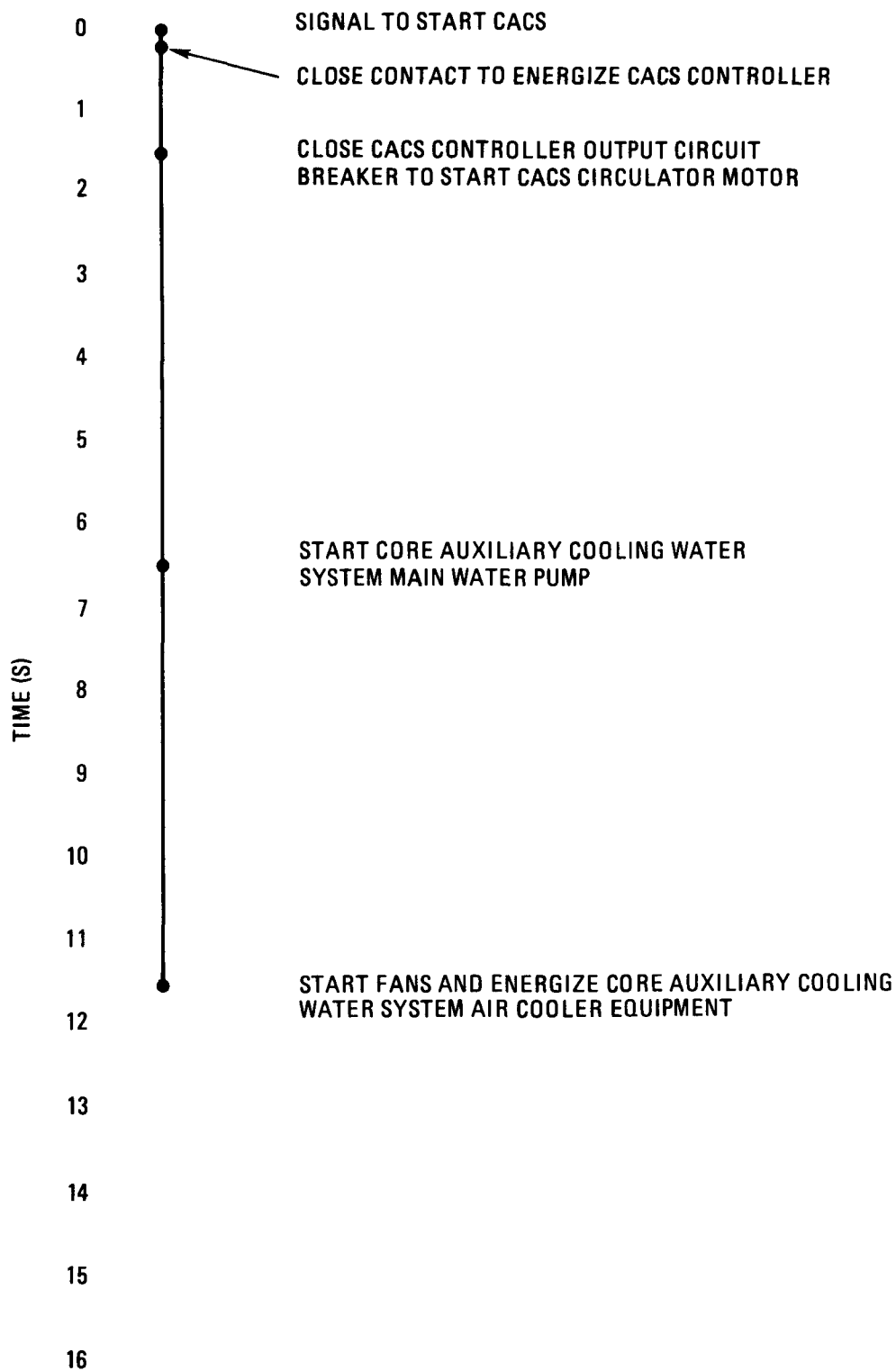


Fig. 10-22. Timing cycle to energize CACS equipment using on- or off-site power; a main CACS load is energized every 5 s

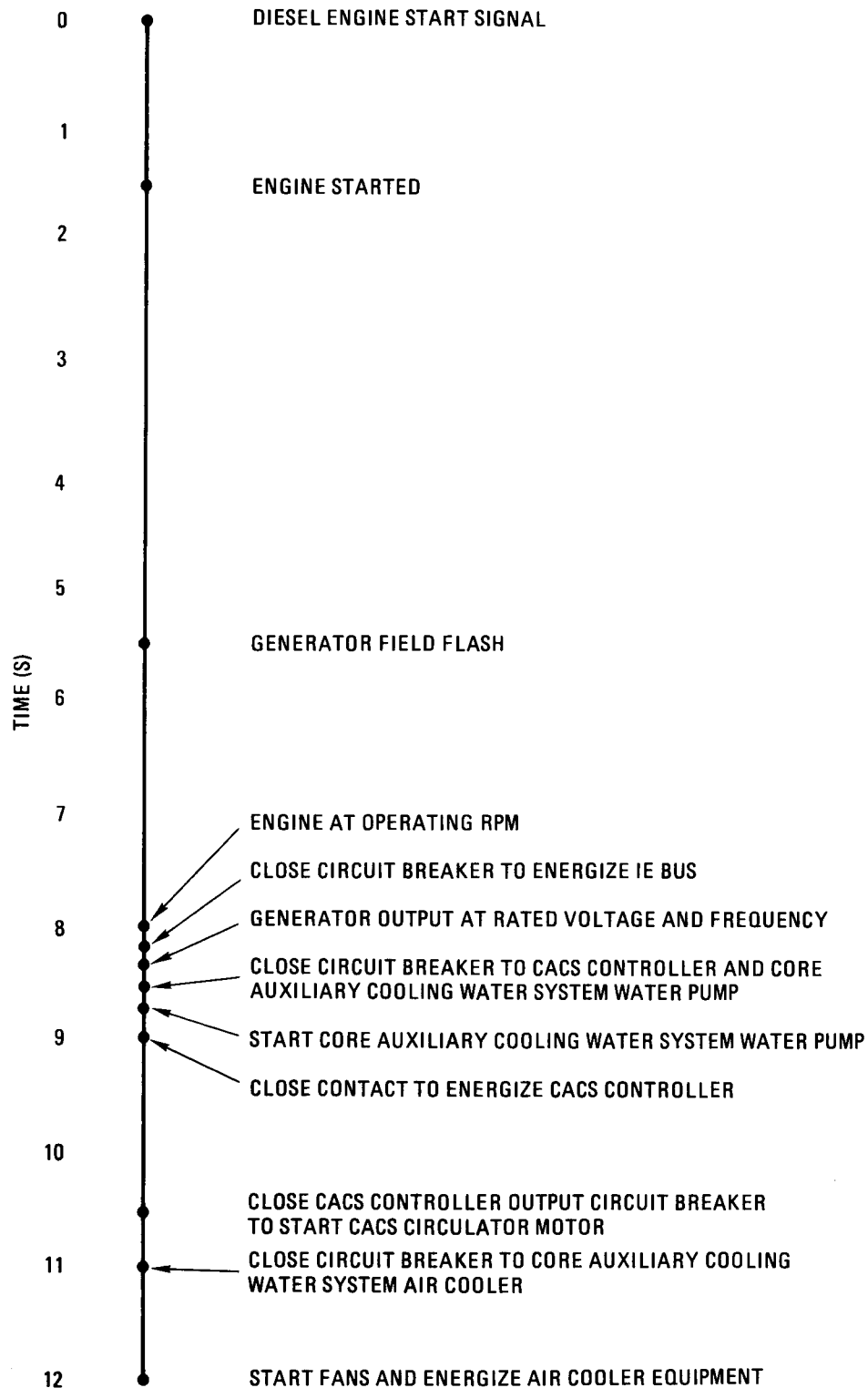


Fig. 10-23. Timing cycle to energize CACS equipment using oversize diesel generator for rapid cycling of CACS loads

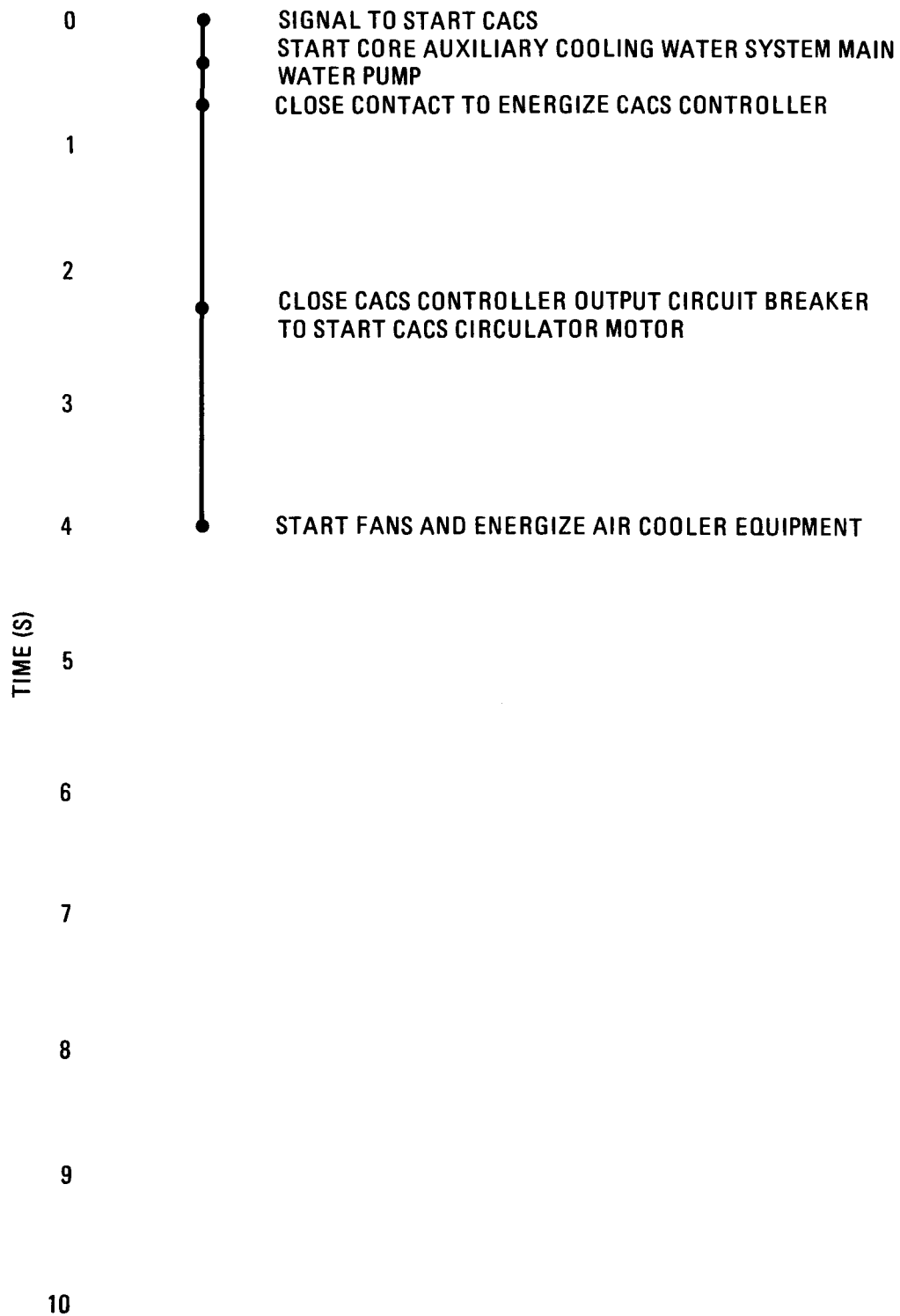


Fig. 10-24. Timing cycle to energize CACS equipment using on- or off-site power for rapid cycling of CACS loads

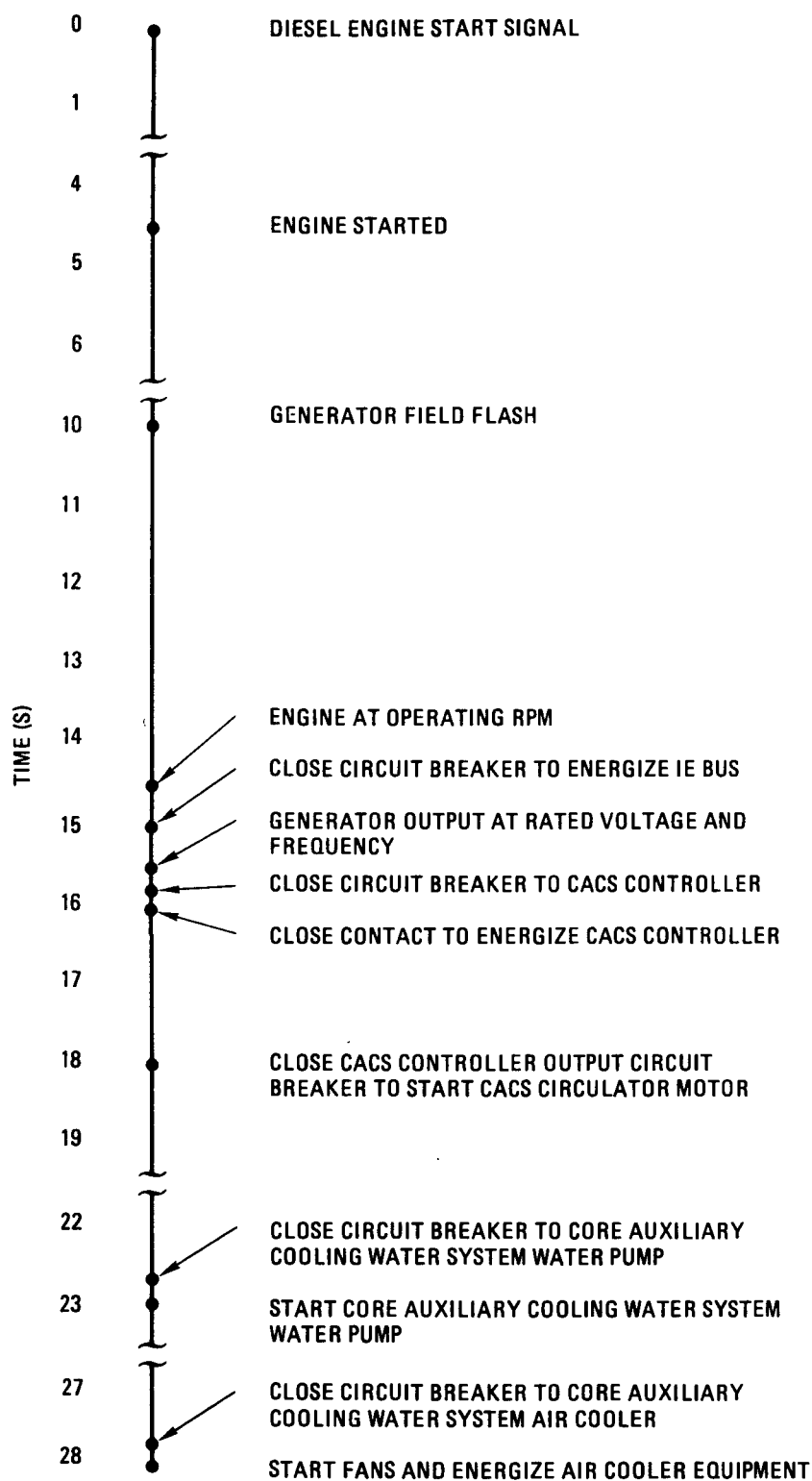


Fig. 10-25. Timing cycle to energize CACS equipment using standard size diesel generator; a main CACS load is energized every 5 s (diesel generator starting data are based on data of Ref. 10-19 except that engine starting time was increased 200% and acceleration time 50%)

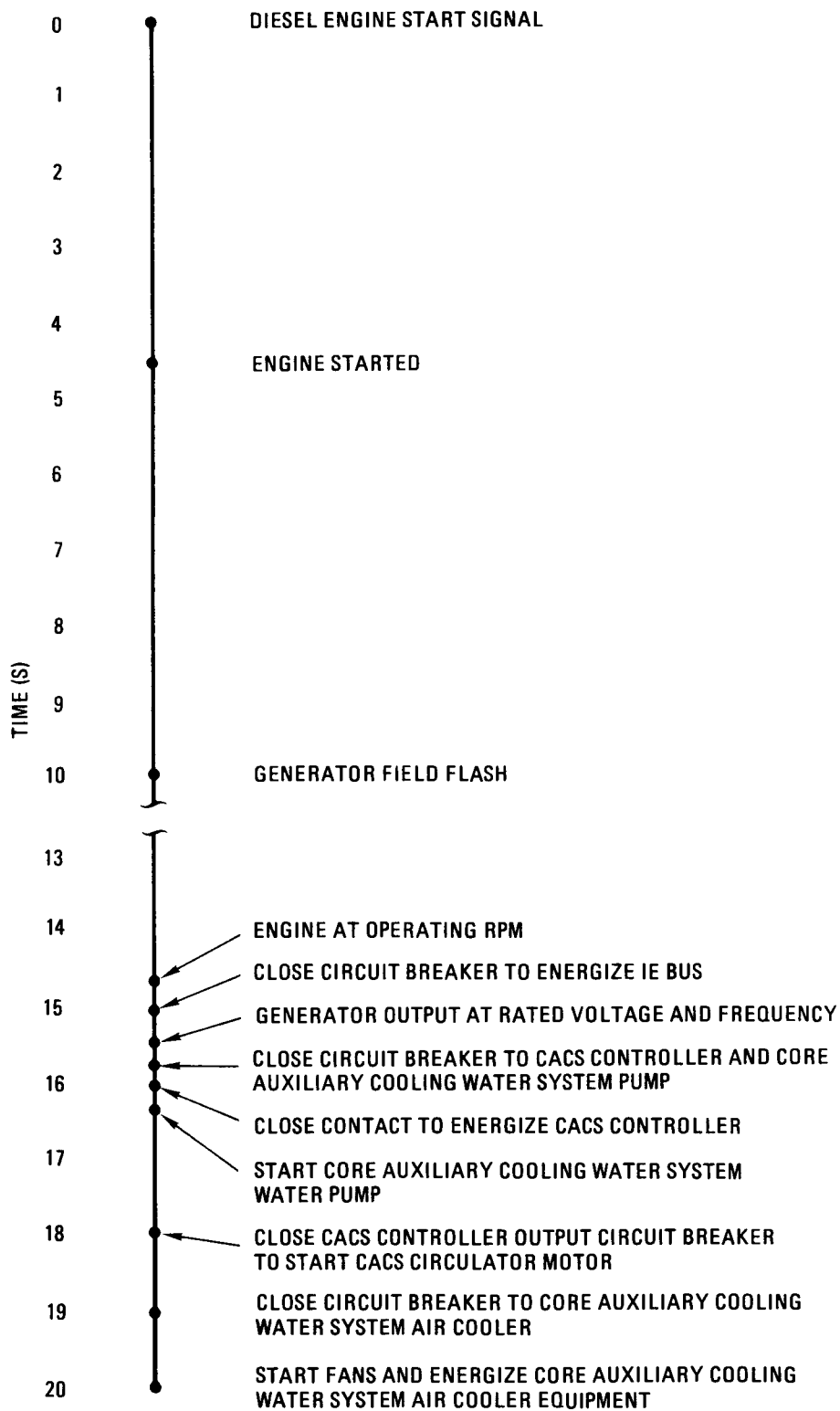


Fig. 10-26. Timing cycle to energize CACS equipment using oversize diesel generator for rapid cycling of CACS loads (diesel generator starting data are based on data of Ref. 10-19 except that engine starting time was increased 200% and acceleration time 50%)

oversized diesel generator and a larger distribution transformer to handle the inrush load from the CACS motors.

10.10.4. Main Loop Cooling System and Main Loop Residual Heat Removal System Using Electric Motor Drive(s)

From a functional standpoint, the main loop cooling system is classified as nonsafety-related and the RHR safety-related. The performance of both of these systems using a single electric motor drive leads to the safety classification of the main drive motor and controller and requires a motor generator set rated to supply the full load current of the motor drive at power. The latter provides independence between IE and non-IE power busses (Fig. 10-27). A simpler and more economical method of achieving the same results is to add a pony motor sized to supply the fractional cooling requirements during shutdown.

The main motor (except for mechanical features) and controller are classified as nonsafety and are powered from a non-IE bus (Fig. 10-28). The pony motor and associated components are safety-related and powered from a IE bus.

10.10.5. Control and Instrumentation Delays During CACS Initiation

A study was conducted to determine the best parameter for initiating core auxiliary cooling and to estimate the delays involved from the time cooling is lost until control rod release and CACS initiation. This information was used as input to a computer code for the analysis of transients following CACS transfer from standby to pressurized cooldown and DBDA. The best initiating parameter at this time is reactor core pressure drop; however, main loop circulator speed or mass flow through each loop is a possible alternative initiating parameter. Estimates of the delays are as follows:

Fig. 10-27. Schematic diagram of single circulator drive motor for main loop and shutdown cooling

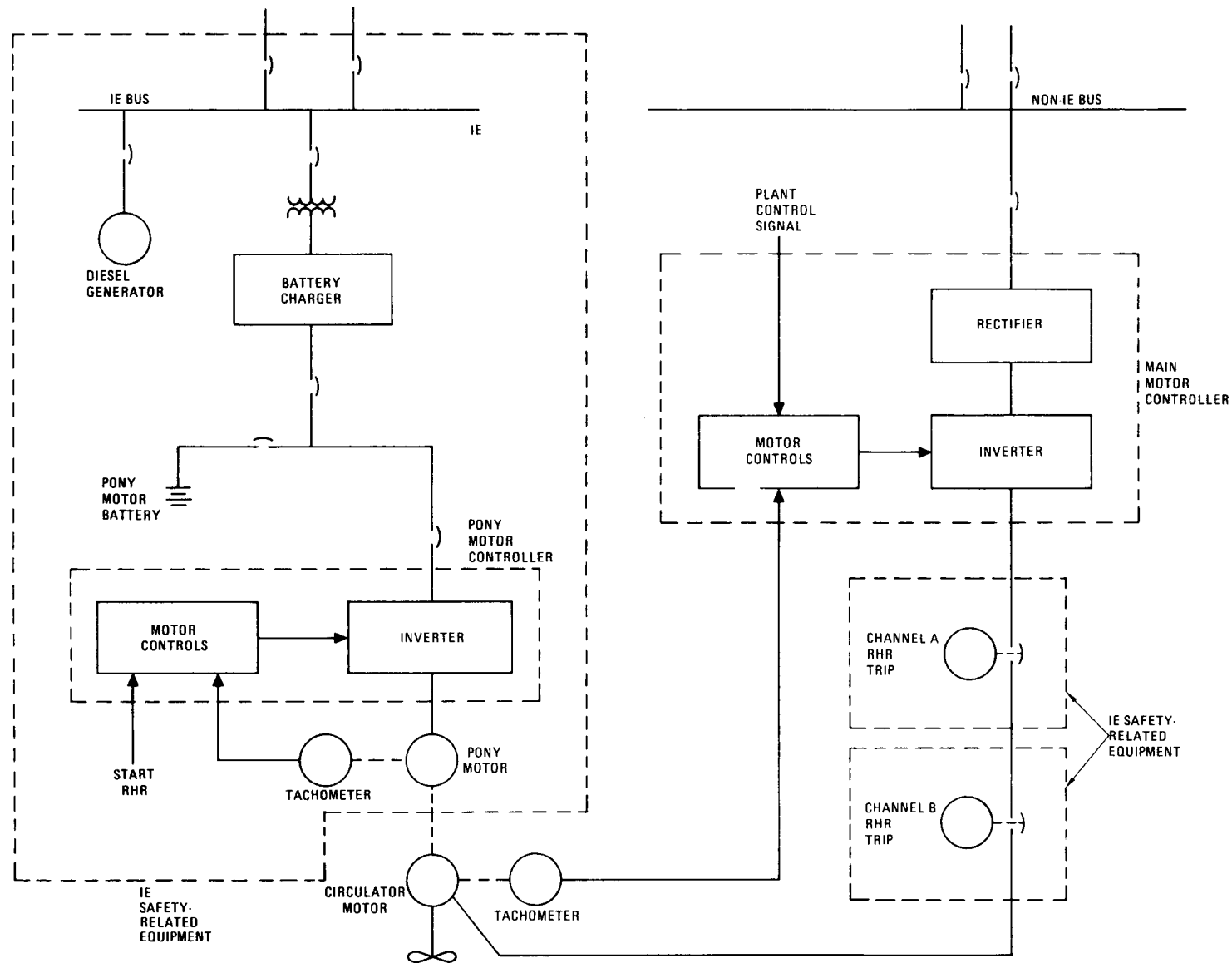


Fig. 10-28. Schematic diagram of main circulator motor with pony motor for shutdown cooling

1. Reactor trip delays to release control rods

Pressure instrument delay	200 ms
Delay in opening control rod motor contactors	50 ms
Magnet delay in releasing control rods	<u>100</u> ms
Total	350 ms

2. Delay in initiating fast start CACS

Pressure instrument delay	200 ms
Delay in CACS motor controller	<u>1000</u> ms
Total	1.2 s

3. Delay in idling CACS initiation

Pressure instrument delay	200 ms

10.10.6. Control Rod Drive Actuation and Position Indication Subsystem

The conceptual design of the control rod drive actuation and position indication subsystem has been initiated to allow for proper interface with the rod drives. During normal operation the control rods are automatically or manually inserted and withdrawn in a simulated bank of three rods. Each rod in a bank is sequentially actuated, and provisions are made to keep the relative positions of the rods in the bank constant. Sequencing of rod actuation is automatic, and owing to the high rod worths, an idle period following each incremental rod movement can be implemented, if required, to eliminate significant power transients. Provisions for individual rod selection and actuation are also included. One problem which must be resolved is that of meeting the safety-related single rod withdrawal requirement and keeping the actuation circuitry nonsafety-related. A tentative solution to this problem is being examined.

REFERENCES

- 10-1. "Gas-Cooled Fast Breeder Reactor Quarterly Progress Report for the Period February 1, 1978 through April 30, 1978," DOE Report GA-A14928, General Atomic Company, May 1978.
- 10-2. Young, G., and K. C. Cheung, "Preliminary Structural Analysis of PCRVR for 300-MW(e) Gas-Cooled Fast Breeder Reactor," DOE Report GA-A14857, General Atomic Company, May 1978.
- 10-3. "An Engineering Review of the Core Support Structure of the Gas-Cooled Fast Breeder Reactor," DOE Report C00-4265-1, Westinghouse Electric Corporation, Advanced Energy Systems Division, October 1977.
- 10-4. "Gas Cooled Fast Reactor (GCFR) Core Support Design Review," Draft Report, General Electric Company, April 1978.
- 10-5. Boonstra, R. H., "TAC2D: A General Purpose Two-Dimensional Heat Transfer Computer Code, User's Manual," General Atomic Report GA-A14032, July 15, 1976.
- 10-6. "Gas-Cooled Fast Breeder Reactor Conceptual Design for a Helium Circulator Test Facility," DOE Report GA-A14999, General Atomic Company, June 1978.
- 10-7. "Alternate Design Studies and Reference Design Selection for GCFR Main Circulator," DOE Report GA-A14754, General Atomic Company, January 1978.
- 10-8. Mouritzen, G., "Alternate Circulator Design Concept No. 1 for a 300 MW(e) GCFR Demonstration Plant - Two Stage Axial Flow Helium Circulator and Two Stage Steam Turbine Drive," DOE Report GA-A14530, General Atomic Company, December 1977.
- 10-9. Kelley, A. P., Jr., and T. Taniguchi, "A Reliability Analysis of the Residual Heat Removal Systems for a 300-MW(e) GCFR," DOE Report GA-14653, General Atomic Company, January 1978.
- 10-10. "300-MW(e) Gas-Cooled Fast Breeder Reactor Demonstration Plant," General Atomic Report GA-A13045, July 15, 1974.
- 10-11. Kroener, A., Kroeners Taschenbuch der Maschinentechnik, v. II, part II, Alfred Kroener Verlag, Stuttgart.

- 10-12. "GCFR Main Helium Circulator Electric Motor Drive Study," Westinghouse LRA Report 6878, June 8, 1978.
- 10-13. Adams, R. G., "A Computer Program PRDEM," General Atomic Company, unpublished data.
- 10-14. Cheng, S. C., "Lead Plant - SG EES Bundle Seismic Loads," General Atomic Company, unpublished data
- 10-15. Starr, J. E., "CSTRES Revision: Generalized Three-Plate Version," General Atomic Company, unpublished data.
- 10-16. Yampolsky, J., et al., "Investigation of Turbocirculator for TARGET, a 1000-MW(e) Reactor (Methodology)," General Dynamics, General Atomic Division, October 1964.
- 10-17. Lund, J. W., "Rotor Bearing Dynamics Design Technology, Part 5," Air Force Aero Propulsion Laboratory Report TR-64 45, May 1965.
- 10-18. "GCFR Helium Purification System - Design Criteria," General Atomic Company, unpublished data.
- 10-19. "Report of Witness Test of KSV-16-T Engine Generator Set," Cooper Energy Services Corporation, unpublished data.

11. CIRCULATOR TEST FACILITY (189a No. 00586)

The objective of this task is to generate a facility for development and qualification testing of the GCFR main helium circulator. The scope of this task involves (1) evaluation of alternative test facility concepts in terms of technical feasibility and cost, (2) identification of the most promising test facility concept, (3) an architect/engineer conceptual design, and (4) final design, construction, and checkout of the facility.

The conceptual design and cost estimate for the GCFR helium circulator test facility was completed (Ref. 11-1). The facility tentatively has been located in Sorrento Valley, California, near GA. It is situated on an 85 x 146 m landscaped plot of land and consists of a 32 x 36 m building, a switchyard, a transformer station, air-cooled heat exchangers, gas storage, and paved areas (Fig. 11-1). The helium loop, an electric motor circulator drive, and supporting facilities for circulator disassembly and testing are contained within the 1668-m² building. A 14.3-m-deep pit at one end of the building contains the circulator test vessel and service system modules; a smaller 6.7-m pit is provided for circulator drive motor maintenance.

The helium loop is entirely contained within the heavy-walled circulator test vessel, which is internally partitioned to form a continuous helium flow path (Fig. 11-2). The circulator and its drive motor are mounted on flanges located on top of the vessel. This loop design permits testing of the circulator at the power, speed, pressure, temperature, and flow conditions in the development plant.

The electric motor circulator drive consists of a main motor, pony motor, main motor controller, and pony motor controller (Fig. 11-2).

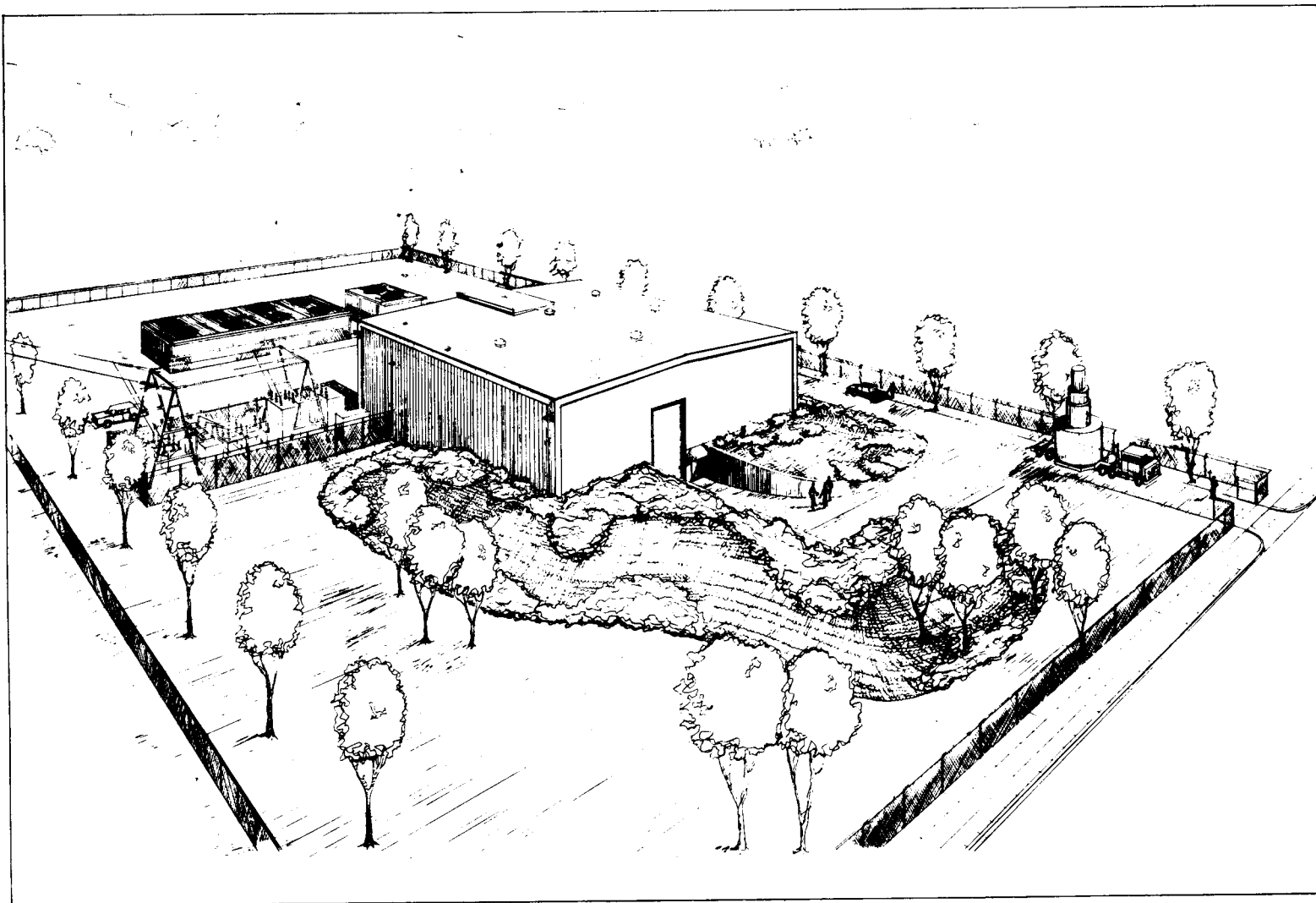


Fig. 11-1, GCFR helium circulator test facility

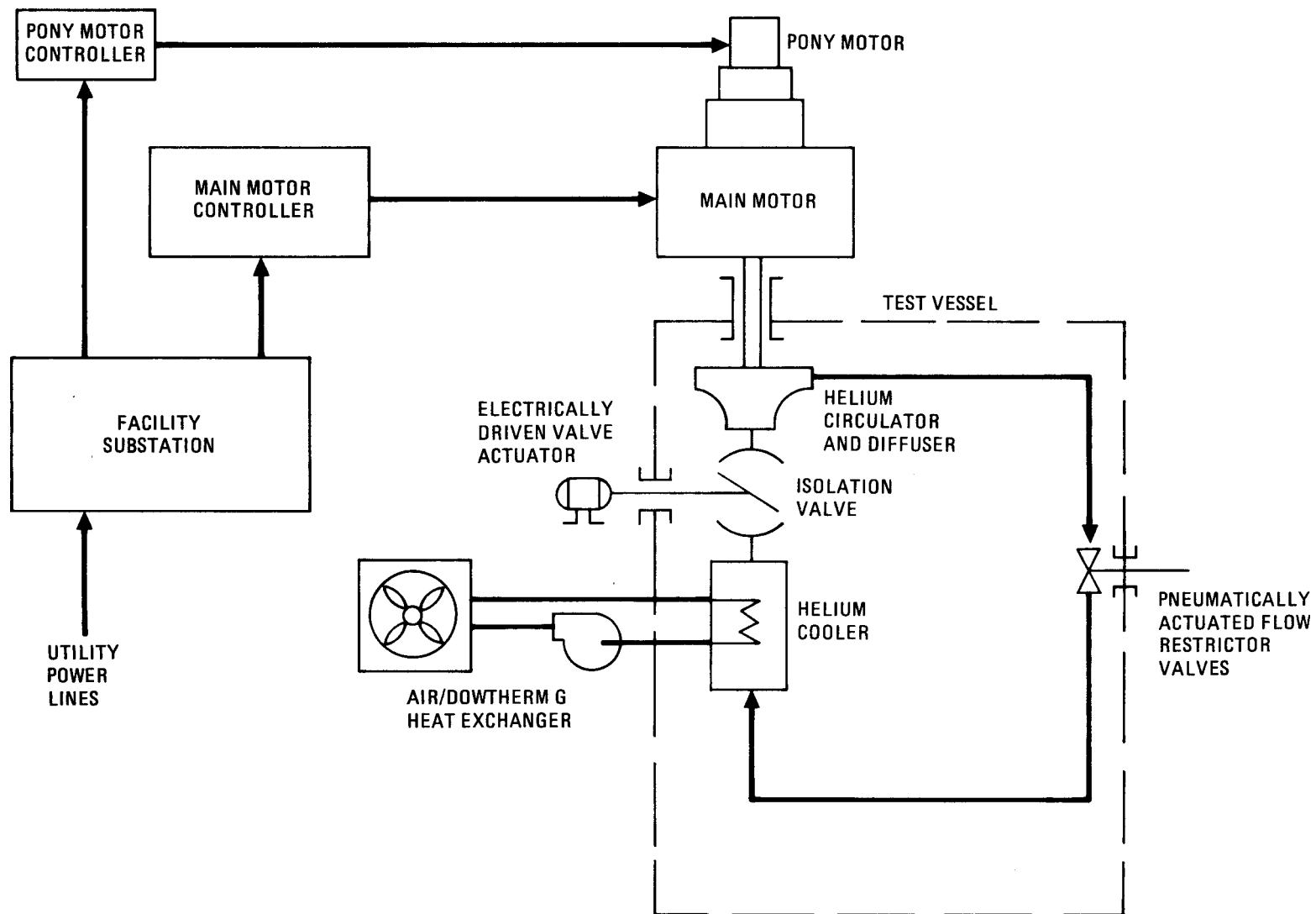


Fig. 11-2. Schematic diagram of GCFR helium circulator test facility

Electrical power is taken from a local utility 138,000-V line and transformed by the facility 32-MVA substation to 24,000 and 4,160 V. The 24,000 V will be used exclusively for the main motor controller, and the 4,160 V will supply the facility service systems.

REFERENCE

- 11-1. "Gas-Cooled Fast Breeder Reactor - Conceptual Design for a Helium Circulator Test Facility," DOE Report GA-A14999, General Atomic Company, June 1978.

12. PLANT DYNAMICS (189a No. 00638)

12.1. CONTROL SYSTEMS

12.1.1. Plant Dynamic Modeling

Two versions of the normal operational plant simulation (25% to 100% power levels) have been completed, one for each of the two helium circulator drive configurations. The simulation was developed primarily for use in the design and analysis of plant control systems. Therefore, its emphasis is to provide accurate predictions at a macroscopic level of overall plant dynamic responses to anticipated plant operating conditions. At this time, it is an open-loop plant simulation; i.e., a plant control system model is not included.

The modeling schematics of the steam-turbine-driven circulator and the electric-motor-driven circulator version are illustrated in Figs. 12-1 and 12-2, respectively. The system simulation was generated using lumped parameter modeling techniques, and it has a modular structure formed by coupling individual component models together. As shown in Figs. 12-1 and 12-2, the three main cooling loops are modeled as a single equivalent loop on both the primary coolant (helium) side and the second coolant (water/steam) side. The major plant components or systems are individually modeled, with emphasis on accurate prediction of input/output relationships over a wide range of operating characteristics. The models included are the reactor core, helium circulator (including gas process flow model) and driver, steam generator, feedwater system (including high-pressure heaters and steam-turbine driven pump), and main steam system (including high-pressure turbine). Provisions for control valves at appropriate locations in the steam-side flow path are also included.

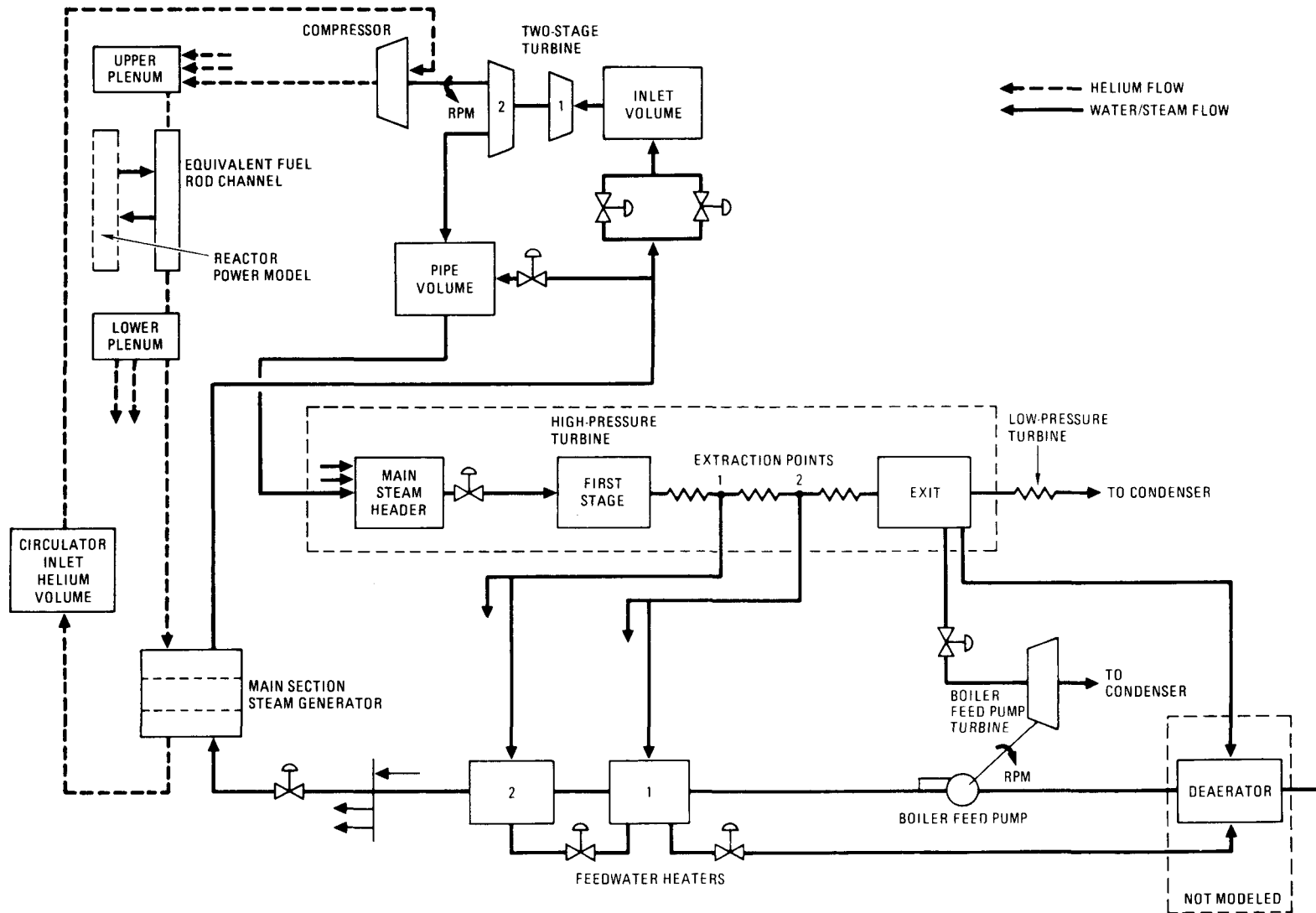


Fig. 12-1. Steam-turbine-driven circulator plant configuration

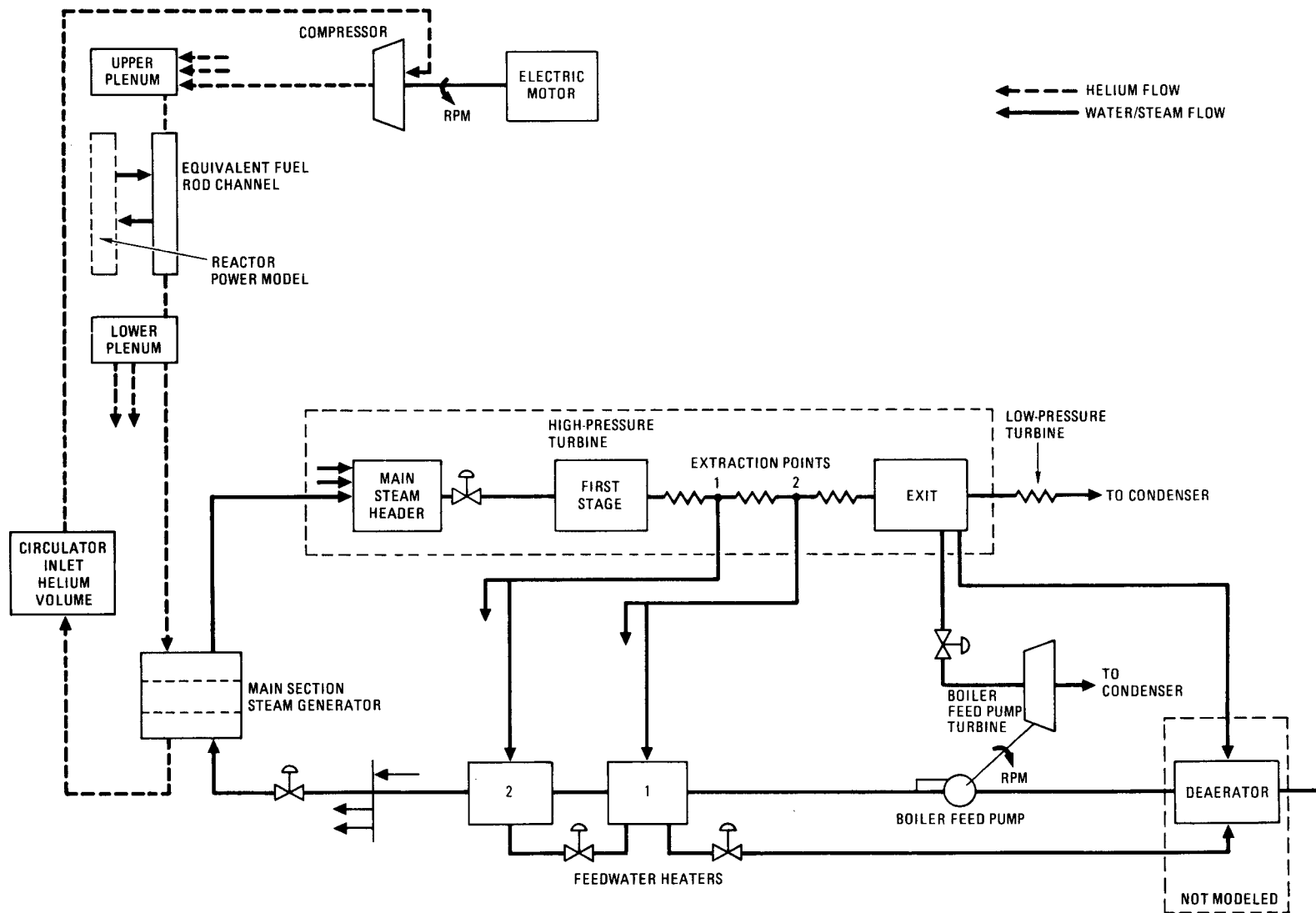


Fig. 12-2. Electric-motor-driven circulator plant configuration

The reactor core model (Fig. 12-3) is broken into three major sections. The first considers the plenum energy balances (upper and lower plenums) in which the helium dynamics are lumped along with the method for calculating reactor pressure. The second considers the thermal energy balances associated with heat transfer in the active core (bypass channel and equivalent fuel channel), and the third considers reactor power from fission and decay heat, including rod-induced and feedback reactivity mechanisms. The reactor core model assumes that (1) helium obeys the ideal gas law, (2) helium mass flow is in instantaneous equilibrium with the circulator flows and is uniform along the length of the equivalent fuel channel, and (3) perfect mixing occurs within each plenum volume.

The equivalent channel model is based upon the representative fuel rod shown in Fig. 12-4. The fuel rods are cylindrical in appearance, with an annular cylinder of stainless steel cladding serving as the fuel container. The fuel is in the form of donut-shaped pellets stacked vertically within the cladding containers. The composition of the fuel pellets is varied to affect the power distribution within the reactor as a whole. It is assumed that these variations do not affect the thermal properties of the fuel. However, the effect on axial power distribution is approximated.

As indicated in Fig. 12-4, a portion of the cladding surface is artificially roughened to improve heat transfer from cladding to coolant. The upper and lower axial regions contain blanket material with a high concentration of isotopes which produce fuel through parasitic capture of neutrons. The power produced in the axial blanket regions is considerably lower than that produced in the central region, especially during the early portions of the fuel cycle. As a consequence of these physical variations, a minimum of four axial regions appears necessary to represent the fuel rod in the equivalent channel model. In the current configuration, the region with the roughened surface is divided into three axial subdivisions. The resulting model, shown in Fig. 12-5, contains six axial regions, and each axial region contains three radial regions corresponding to fuel, cladding, and coolant. Axial heat transfer between adjacent solid nodes is assumed to be negligible compared with radial heat transfer.

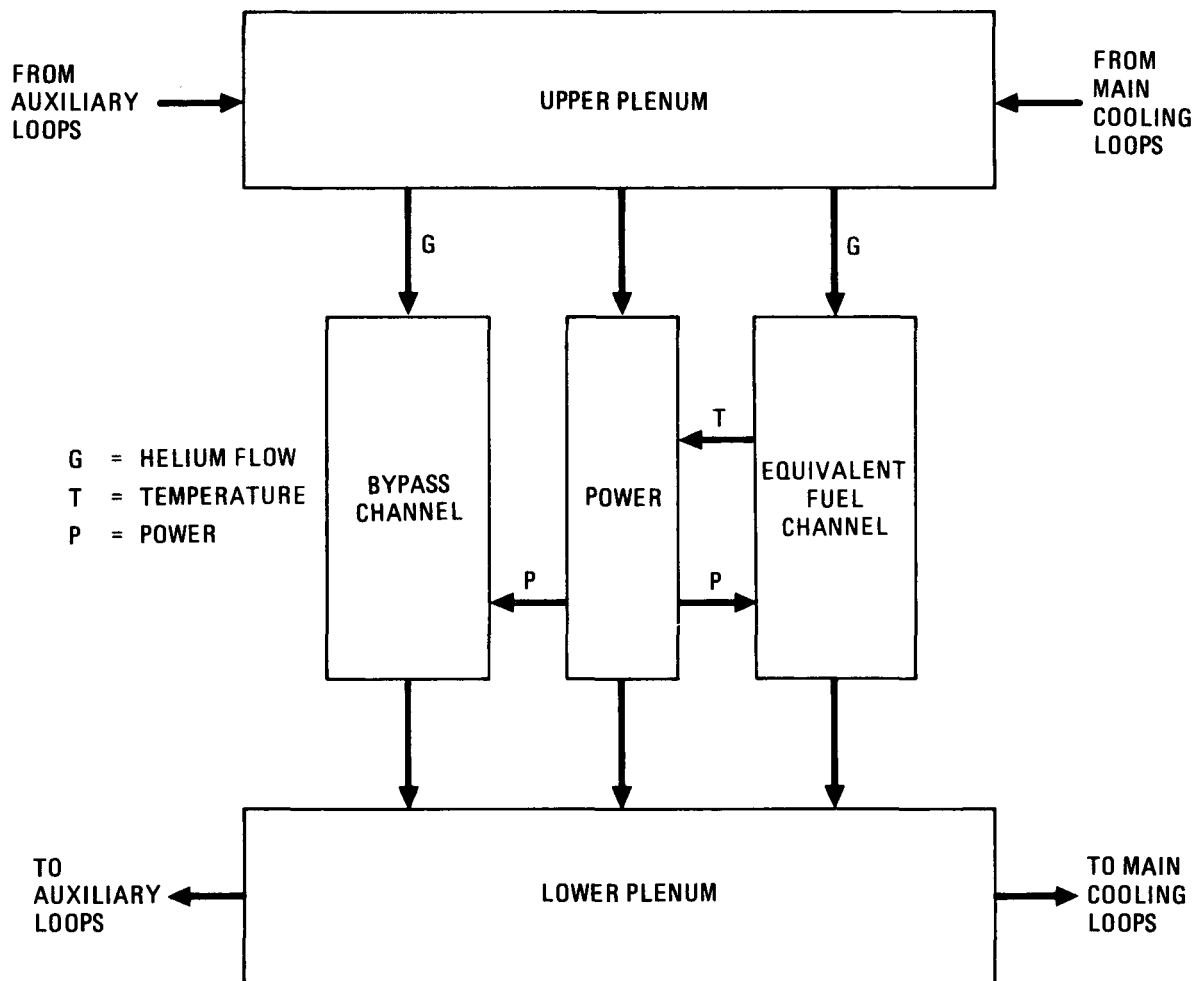


Fig. 12-3. Reactor core model

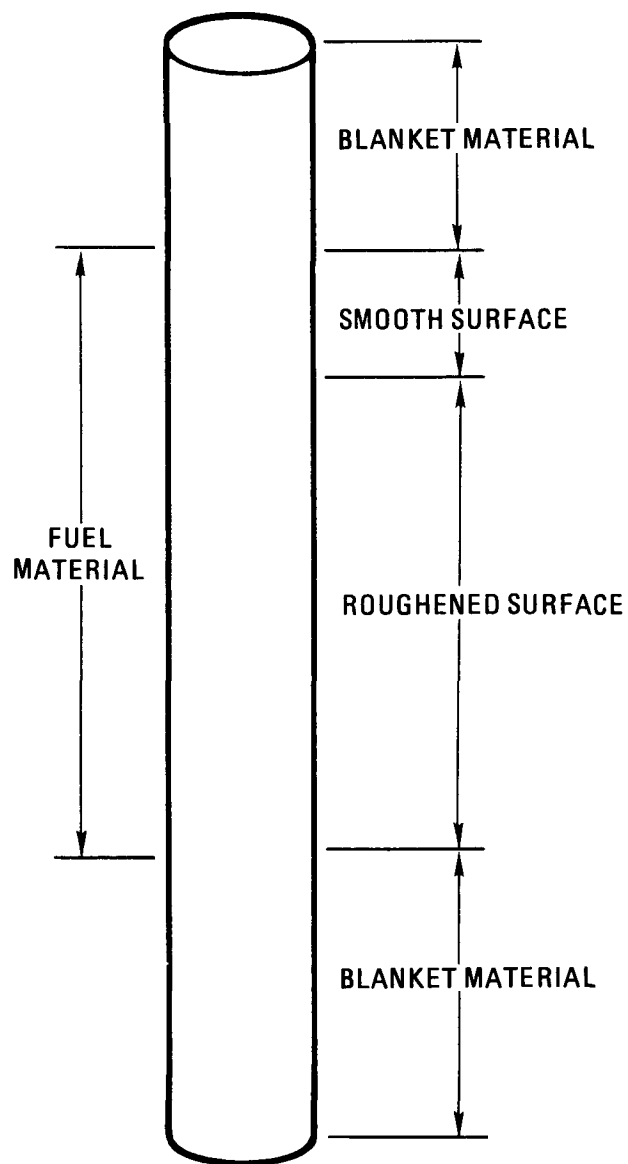


Fig. 12-4. Representative fuel rod

Q = HEAT FLOW
 TF = FUEL TEMPERATURE
 TC = CLADDING TEMPERATURE
 TU = MIXED-MEAN TEMPERATURE OF
 HELIUM IN UPPER PLENUM
 TH = HELIUM NODE WEIGHTED AVERAGE TEMPERATURE
 THO = HELIUM NODE EXIT TEMPERATURE

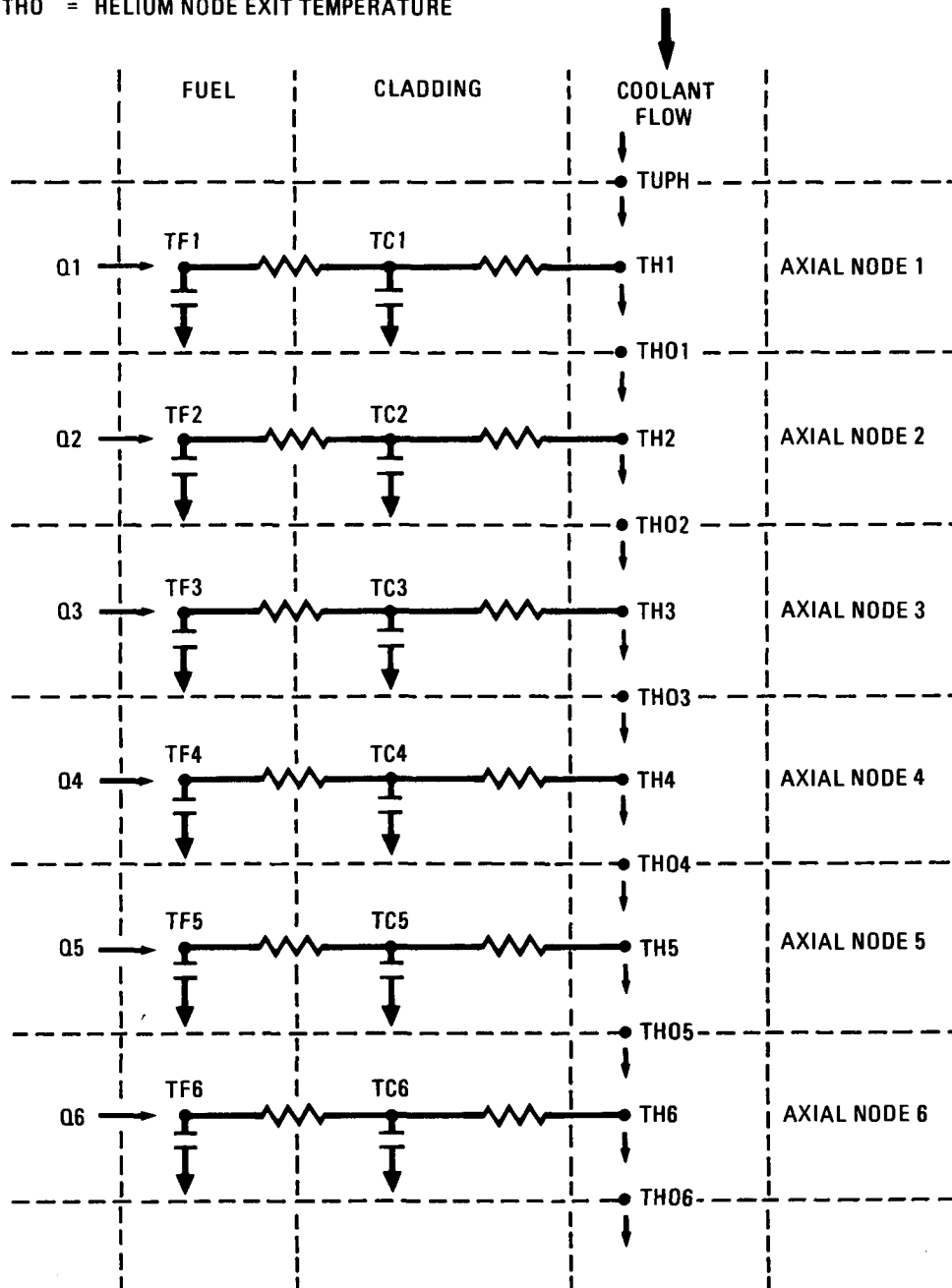


Fig. 12-5. Equivalent fuel channel thermal model

The reactor power model is based on the point kinetics equations with six delayed neutron groups in the prompt jump approximation. The rod-induced reactivity data are expressed in the form of reactivity worths and curves of fractional worth versus fractional rod position. The dynamics of the rod drive mechanisms are also modeled. The six reactivity feedback mechanisms which are modeled are Doppler effect, fuel rod axial expansion, changes in helium coolant density, core radial thermal bowing, grid plate mechanical distortion, and grid plate thermal expansion. Decay heat generation following reactor scram is based on thermal reactor fission product decay data and the reactor power level just prior to scram.

The function of the feedwater system is to provide the motive power to the secondary coolant and to utilize regenerative heating to optimize cycle performance. The feedwater configuration is shown in Fig. 12-6. Two variable-speed turbine-driven boiler feed pumps feed three steam generators with individual flow control valves through pipelines containing two high-pressure heaters each. The schematic resistances within the steam generators represent the lumped effect of distributed line losses through inlet headers and through the economizer section of each tube as well as the inlet nozzle for each steam generator tube. The model was developed for a single equivalent loop. The modeling schematic for the feedwater model is shown in Fig. 12-7.

The feedwater heaters are shell and tube heat exchangers (Fig. 12-8). Feedwater passing through the tubes is heated by the saturated steam and water in the shell. The primary source of heat is extraction steam from the main turbine. The heater drain flow is (perfectly) regulated to maintain a constant water level in each tank. The drain flow from the higher-pressure heaters is fed in series to the lower-pressure heaters to reduce extraction flow requirements.

The steam generator to be modeled is a once-through, helium-heated subcritical generator consisting of a bundle of small-diameter tubes coiled in the form of a helix. Water and steam flow through the tubes, and hot helium which has passed through the reactor core flows over the

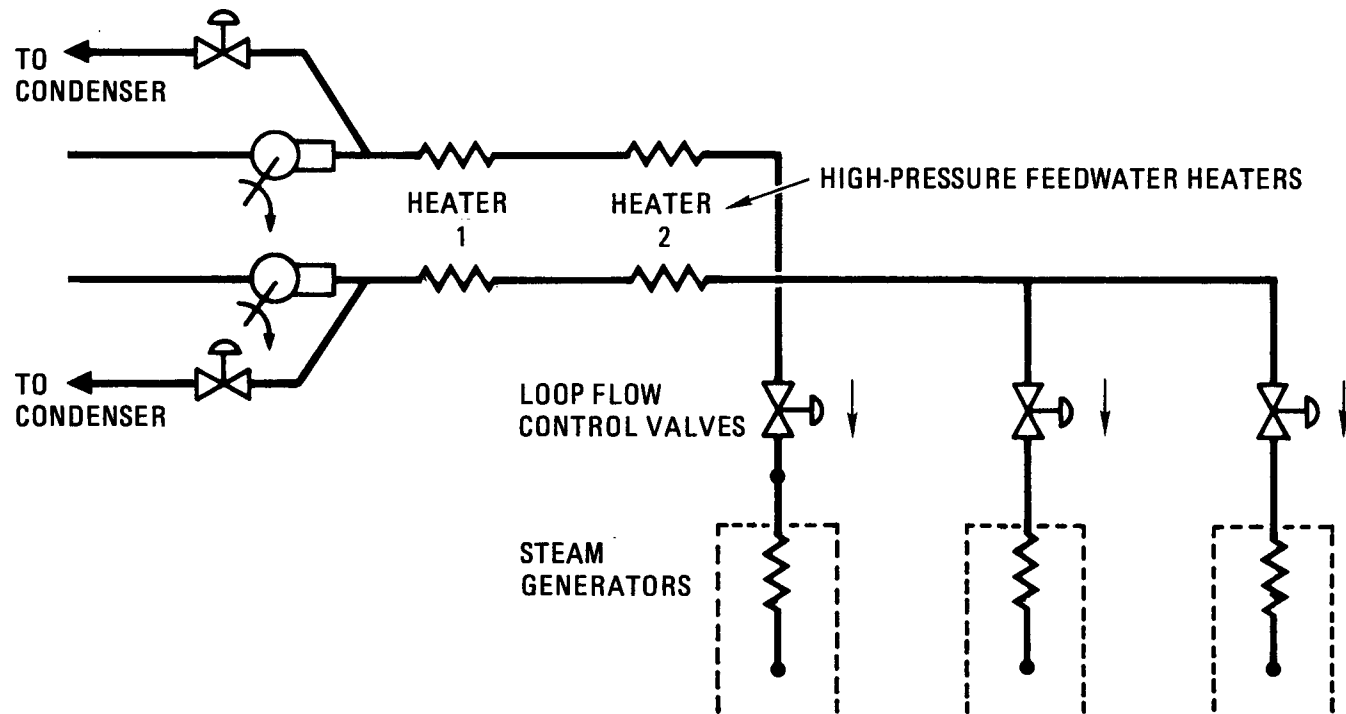


Fig. 12-6. Feedwater configuration

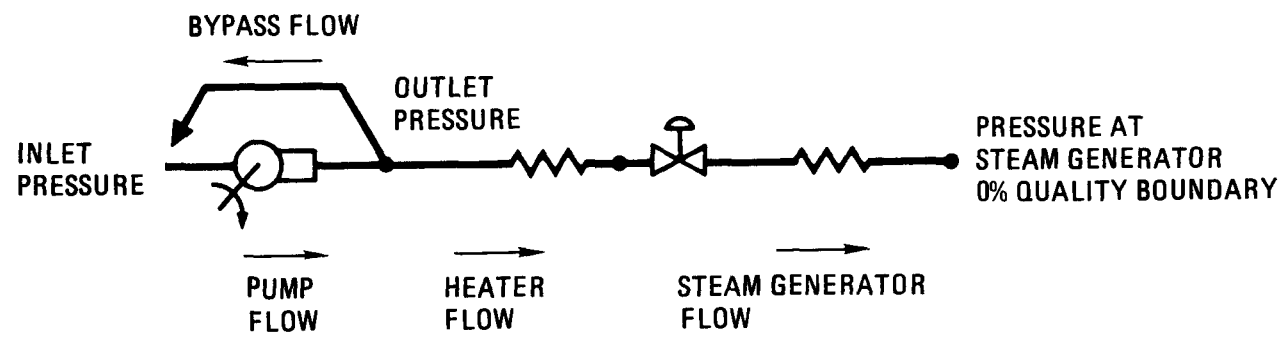


Fig. 12-7. Feedwater model

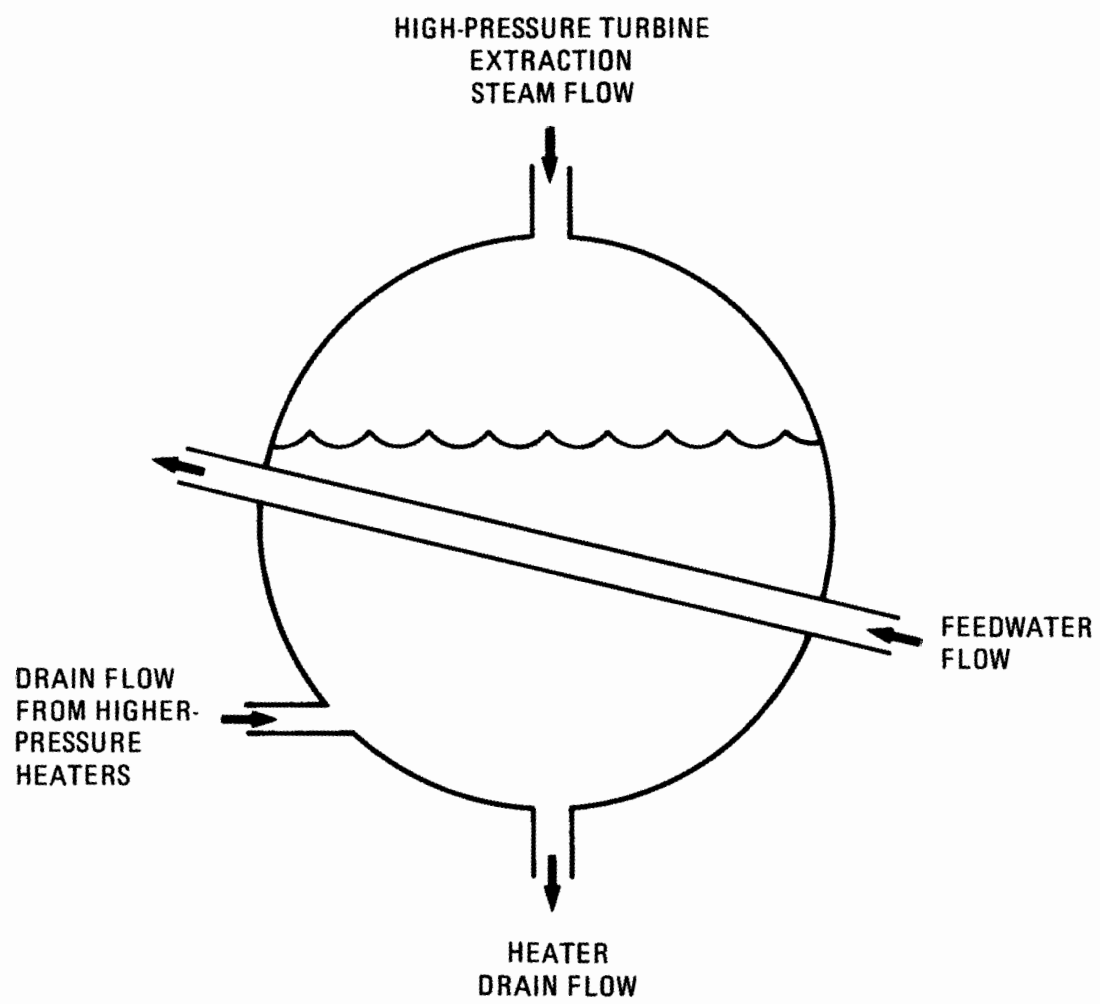


Fig. 12-8. Feedwater heater

tubes to form a countercurrent heat exchanger. The three regions (subcooled water, two-phase steam/water mixture, and dry steam) which exist on the water/steam secondary side are referred to as the economizer, evaporator, and superheater, respectively. The boundaries of these regions are set by thermodynamic properties (0% and 100% quality) and are free to move with respect to the ends of the steam generator. The location of these boundaries at any instant in time is a function of several quantities such as feed-water flow rate and temperature, helium flow rate and temperature, and steam generator back pressure.

The actual physical processes associated with heat transfer and fluid flow in a multiphase heat exchanger such as the steam generator are quite complex. To represent these processes with a low-order mathematical model, a number of assumptions were made to simplify the problem:

1. One steam generator coil can be modeled by a single average tube whose properties (area, length, steam/water inventory, heat transfer coefficients, feedwater and helium flows, and tube metal mass) represent the properties of the entire coil.
2. Fluid flow and heat transfer in the steam generator are adequately represented if the model consists of three nodes. For convenience, these nodes are the economizer (contains subcooled water and extends from steam generator inlet to 0% quality boundary), evaporator (two-phase region from 0% to 100% quality boundaries), and superheater (dry steam and extends from 100% quality line to steam generator outlet).
3. Mass, momentum, and energy storage in each node can be characterized by the rate of change of the appropriate average property.

The steam generator model is shown in Fig. 12-9.

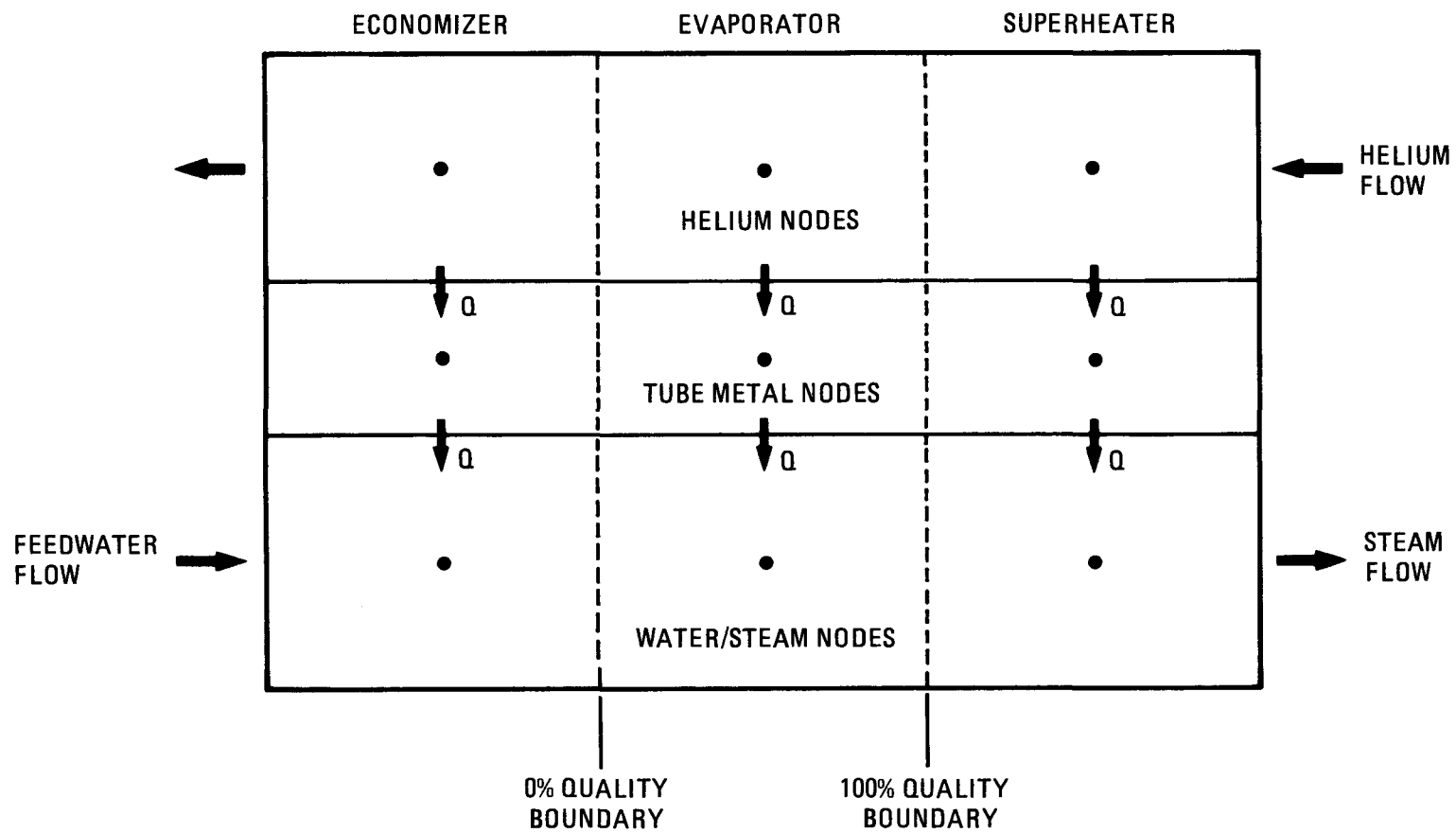


Fig. 12-9. Steam generator model

The function of the circulator system is to provide the driving mechanism to force the helium to circulate around the primary coolant loop. This system consists of the helium circulator compressor and the mechanism which provides the motive power to the compressor. There are two alternatives for providing this power: a steam turbine or an electric motor. The system model includes a steam admission valve and a bypass valve for the steam turbine drive concept. Steam is directly provided to the turbine from the steam generators, and the steam from the circulator turbine flows directly to the high-pressure turbine. The electric motor drive is not modeled because information is not currently available on the motor type and characteristics. Instead, motor speed is characterized as a single-order lag on speed demand.

The compressor model is shown in Fig. 12-10. The following assumptions are made for this model: (1) the primary coolant behaves as an ideal gas; (2) primary coolant flow throughout the primary loop is in equilibrium with the compressor speed at every instant in time, and local pressure losses are influenced by the temperature distribution along the primary coolant path; (3) the summation of pressure losses around the entire loop is relatively insensitive since the primary coolant inventory is constant; (4) the recirculation flow rate is specified as input; and (5) the loop ΔP is small compared with absolute system pressure. The second assumption requires that the entire primary coolant process flow model be lumped with the compressor model to determine primary coolant flows such that circulator pressure rise and system pressure losses are in balance. The compressor properties are obtained from performance curves.

The circulator turbine drive model is shown in Fig. 12-11. The piping between the steam generator lead-outs and the turbine inlet is included as a modeling convenience. The turbine and compressor models are isolated by assuming that the only mode of energy transfer between the turbine and the compressor is through the mechanism of shaft work. The turbine properties are obtained from performance maps.

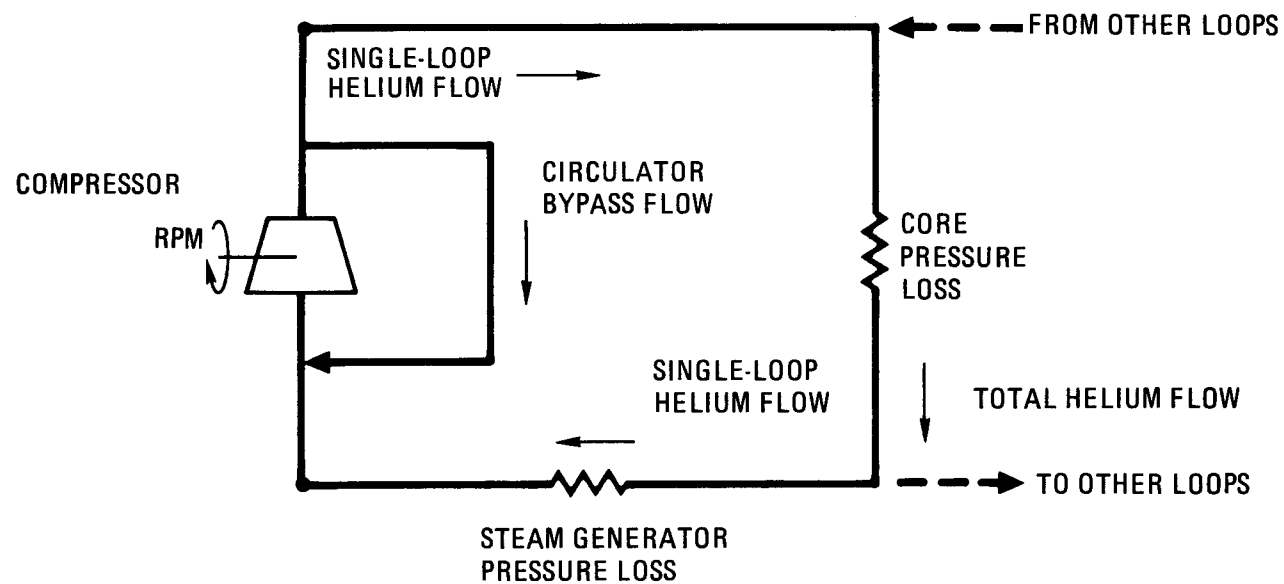


Fig. 12-10. Compressor model

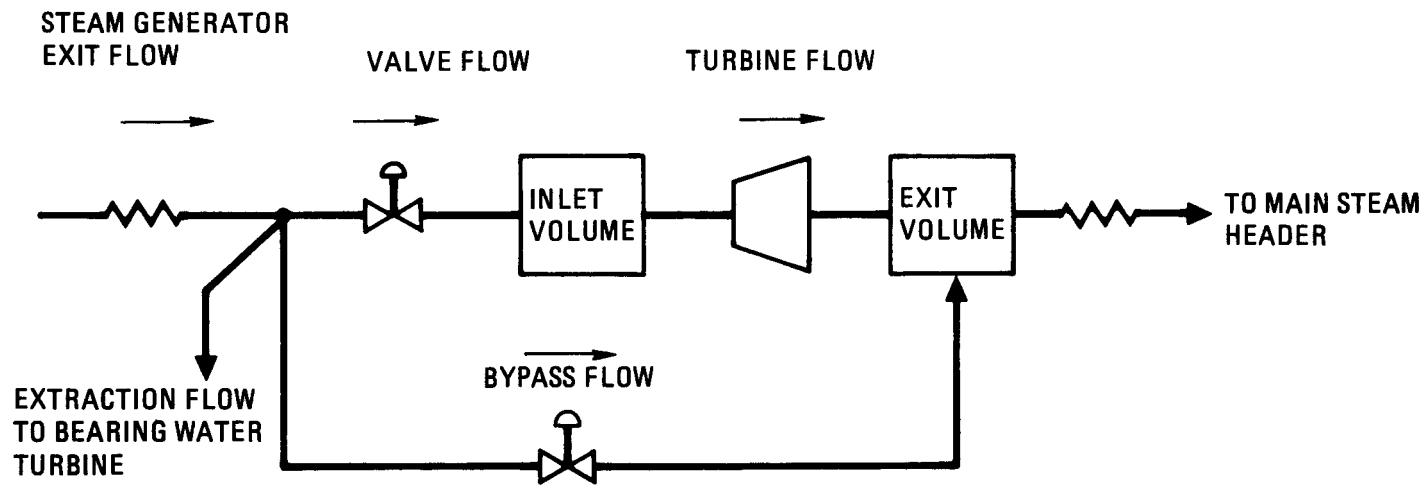


Fig. 12-11. Circulator turbine model

The function of the main steam system is to header the outlet steam from the individual steam generators and transport it to the valve chest of the high-pressure turbine. The power resulting from expansion of the steam in the turbine is delivered as output power from the electric generator. The main steam system includes the main steam header, a high-pressure turbine, and a low-pressure turbine; the model does not include the electric generator. The high-pressure turbine model includes models of the admission/governor valve(s). The low-pressure turbine is modeled with simple flow resistance, assuming choked flow, at the tail end of the cycle.

The modeling schematic for the high-pressure turbine subsystem is given in Fig. 12-12. The main steam header volume is lumped at the turbine inlet to provide inlet conditions, and the high-pressure turbine dynamics are lumped at the first stage and the exit (crossover to the low-pressure turbine). The first-stage node lumps the turbine volume from the throttle valve to the exit or crossover. An exit node was added to accommodate possible additional steam reheat between the high- and low-pressure turbines in an alternate design. The volume associated with this node is larger than can probably be justified in the current design and was chosen to achieve numerical stability at the integration step size desired.

The model of a turbine-governing stage is generally well suited to a turbine employing full arc admission with an impulse-type first stage. The model is entirely algebraic. The dynamics must be externally supplied by actuator models and volume lumping as appropriate. For this model it is assumed that (1) flow does not choke across the impulse stage, (2) expansion through the governor valve is isenthalpic, and (3) expansion efficiency across the impulse stage is constant.

12.1.2. System Synthesis

Control system analyses are continuing for the electric-motor-driven circulator plant configuration. The objectives of these analyses are to synthesize a rudimentary on-line plant control system for obtaining

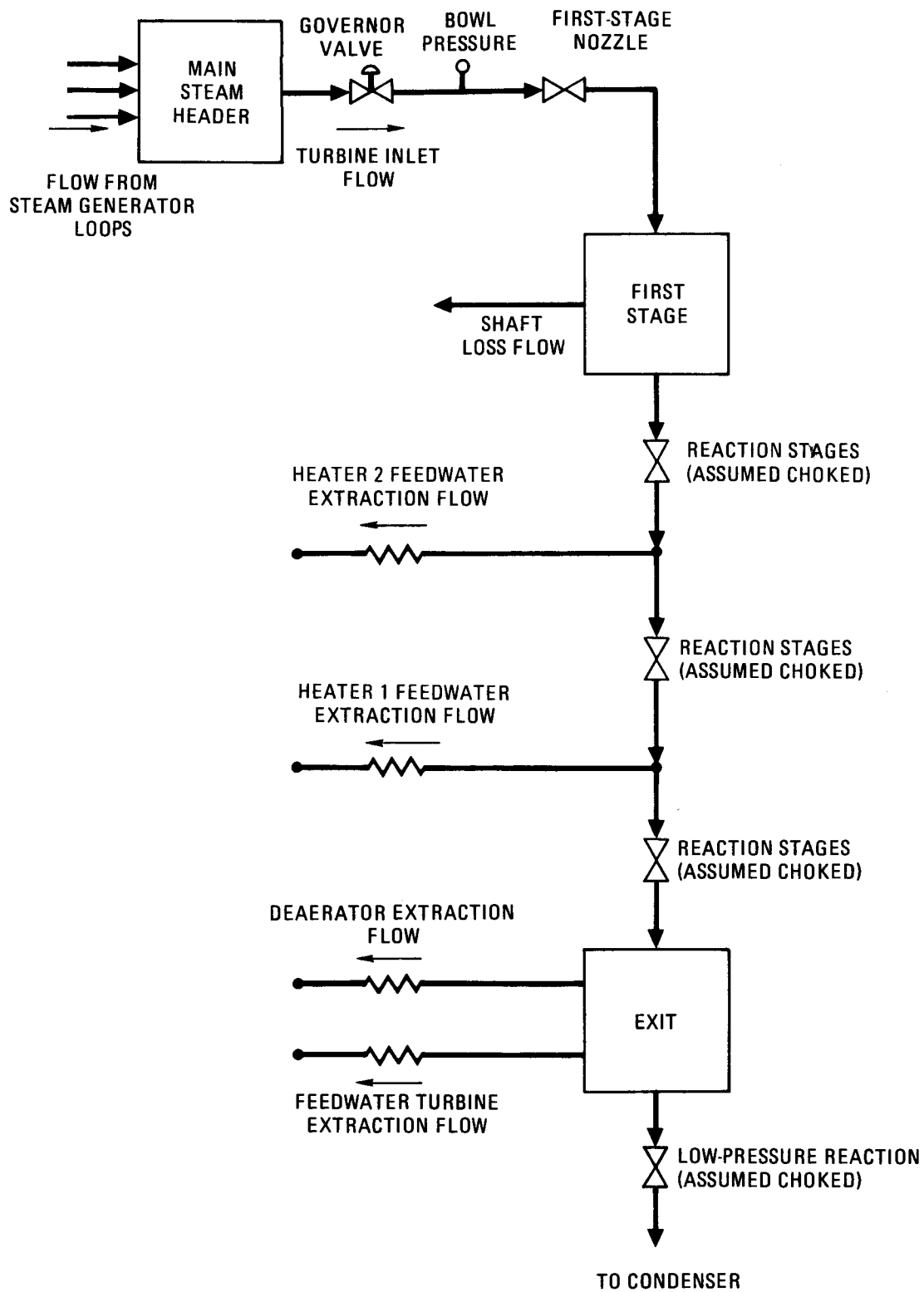


Fig. 12-12. High-pressure turbine model

preliminary information on expected normal plant operating transients, to investigate the feasibility of particular plant control schemes, and to determine potential major plant control problem areas.

12.2. SEISMIC ENGINEERING

Work on the development of an overall plant seismic model for the GCFR was initiated during this quarter.

12.3. FLOW-INDUCED AND ACOUSTICALLY INDUCED VIBRATIONS

There was no activity on this task during this quarter.

13. REACTOR SAFETY, ENVIRONMENT, AND RISK ANALYSIS
(189a No. 00589)

The purpose of this task is to investigate and quantify GCFR safety characteristics. A liaison and coordination subtask integrates the DOE-sponsored GCFR safety work at GA and the national laboratories into a national GCFR safety program which is responsive to the need for GCFR safety research. A GCFR safety program plan is being developed to define the safety research and schedule needed for the demonstration plant. Safety research at GA includes probabilistic accident analysis, core accident consequence analysis, postaccident fuel containment (PAFC) analyses, and radiological/environmental analyses.

Logical probabilistic methods are employed to determine the probabilities associated with various accident initiation and progression sequences and to identify potential design modifications which would help reduce risks. A methodology for integrating reliability considerations into the GCFR engineering effort at the system, subsystem, and component levels is being developed for trial use on a selected system, with the objective of determining the optimal use of reliability engineering methods in the GCFR. Core accident consequence analyses are concentrating on the protected loss of flow (PLOF) accident sequence, which has been identified as a dominant event sequence leading to core melting. Recriticality in this accident sequence is the phenomenon which could most significantly influence consequences. Determination of the conditions which may lead to recriticality are therefore the principal objective of current investigations. PAFC analyses are being performed to assess the capability of the design and to identify potential modifications which could improve the molten fuel containment capability.

13.1. REACTOR SAFETY PROGRAM COORDINATION

The GCFR safety program plan which defines the safety program structure and the individual major safety tasks was completed last quarter (Ref. 13-1). This structure has been incorporated into the GCFR work breakdown structure, and detailed planning of each task through development plant acceptance has been completed consistent with the requirements for RECS. The individual task plans are being programmed into RECS.

A cursory review of safety analysis computerized reactor data (SACRD) revealed only partial inclusion of GA helium data. The current SACRD helium data will be replaced with the GA data.

13.2. PROBABILISTIC SAFETY ANALYSIS

Accident initiation and progression analysis (AIPA) techniques are being applied to probabilistic analysis of potential accident sequences leading to low-probability, high-consequence outcomes. The consequences of these sequences are also under study at ANL and at GA under other sub-tasks. The objective of this work is to assess the risks of these accident chains in the GCFR.

Reference 13-2 analyzes the reliability of the main loop cooling system RHR function. The allocated reliability target of less than 10^{-2} /yr could not be met, principally because a number of single failure points were identified in the main system and particularly in the support systems. Improved main loop RHR system designs have been developed and are currently being analyzed to identify the improvement in reliability which can be gained by elimination of all single failure points. Preliminary results indicate that the main loop reliability target can be met.

13.3. CORE ACCIDENT ANALYSIS

13.3.1. Introduction

The consequences of low-probability accident sequences leading to core damage are investigated under this subtask to determine the capability of the core, PCRV, and containment building barriers to mitigate the release of radioactive material to the environment. The first step is to study the expected behavior of the GCFR core. Particular emphasis is given to the PLOF accident which has been shown to be the dominant contributor to the probability of a loss of coolable core geometry. Analyses of unprotected accidents are currently being emphasized at ANL.

During this quarter results were obtained on the spatial and temporal distribution of material heat-up and melting over the entire core during a PLOF. Modifications of the BOXRAD computer program reported last quarter (Ref. 13-1) formed the basis for the current investigation. The present whole-core heat-up analysis concluded that fuel slumping will occur over a large fraction of the core prior to melting of the outer ring of duct walls.

13.3.2. Core-Wide Material Melting Sequence During a Protected Loss of Flow Accident

Previous analyses (Refs. 13-1, 13-3) have addressed the progression of material melting within the GCFR core during a PLOF accident. These analyses did not consider the duct walls adjacent to the radial blanket. Circumferential duct melting has been calculated to begin prior to incipient fuel melting in all fuel assemblies, except possibly those adjacent to the radial blanket. Simple calculations indicate that at temperatures below 1260°C, the outer ring of duct walls adjacent to the radial blanket can support the entire core. At this temperature, the core weight causes the minimum tensile strength of these outer duct walls to be exceeded, and the fallaway of a large portion of the core from the control rods becomes a source of recriticality. However, the delay in

melting of the duct wall along the core-blanket interface may allow time for fuel slumping and recriticality to occur prior to core fallaway.

The BOXRAD code (Ref. 13-1) was used to study the progression of cladding, duct wall, and fuel melting within the entire GCFR core during a PLOF accident. A line of assemblies running radially from the center of the core to the outer row of blanket assemblies was modeled, and two initial conditions were used. Case 1 assumes that immediately upon reactor shutdown the circulator undergoes a 40-s coastdown to a complete loss of forced flow; case 2 assumes that a loss of forced circulation occurs one week after reactor shutdown. Figures 13-1 and 13-2 present the material melting sequence for cases 1 and 2, respectively; the radial position range of each assembly is denoted by ASM number for easy orientation. ASM-2 is the assembly at the center of the core, and ASM-37 is the assembly at the core and radial blanket interface. The lines connecting the data points for the duct wall are provided to guide the eye and do not represent numerical results. In both figures, delay of duct wall melting at the core and radial blanket interface results in fuel slumping before all duct walls within the core have melted. Figure 13-2 shows a relative increase in the time required to melt fuel under lower decay heat conditions.

13.4. POSTACCIDENT FUEL CONTAINMENT

A heavy metal bath system for molten fuel containment was assessed in Ref. 13-1. During this quarter additional results were obtained which showed some favorable characteristics of the heavy metal bath concept. A scoping study of the up-flow core PAFC was also completed. Meltdown scenarios, extent of core melting, and core retention considerations are discussed.

13.4.1. Assessment of a Heavy Metal Bath System for Molten Fuel Containment

The short melting time due to the low heat capacity of uranium alloys and the nonuniform heat distribution over the lower and sideward cavity liner are considered to be disadvantages of the heavy metal bath concept.

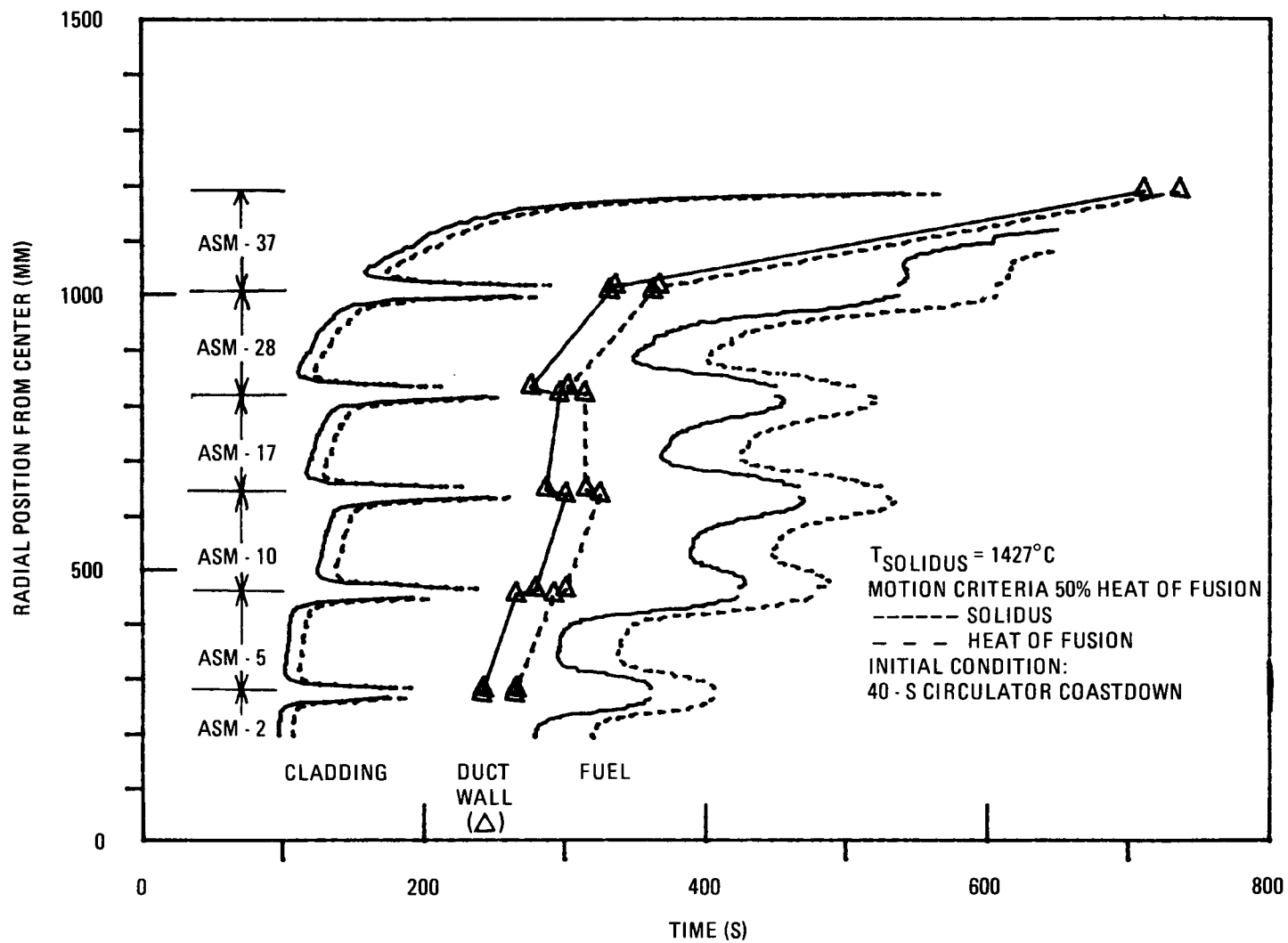


Fig. 13-1. Circulator coast-down in 40 s

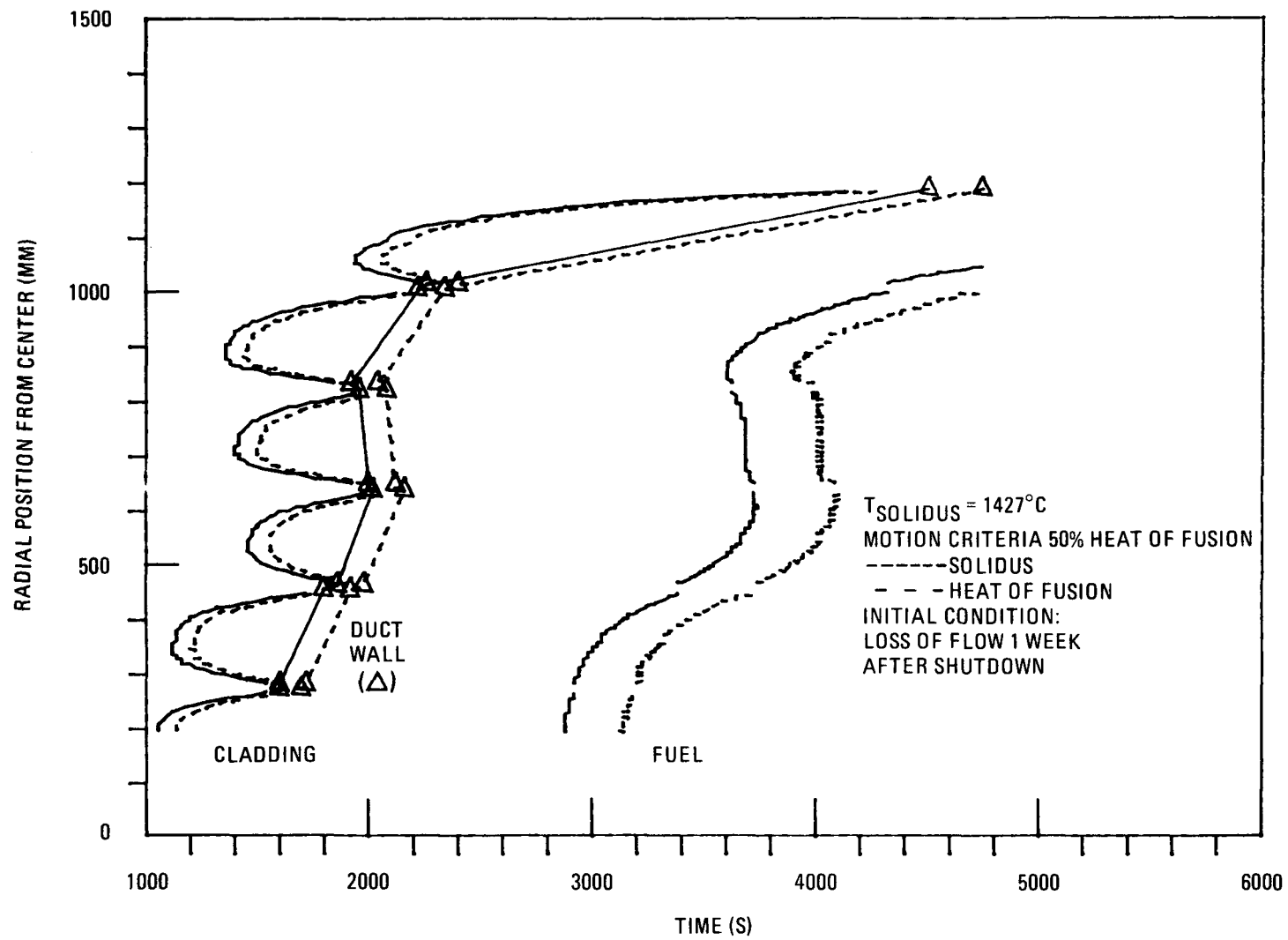


Fig. 13-2. Loss of flow 1 week after shutdown

Therefore, a study was performed to explore methods of alleviating these drawbacks. First, the pool bulk temperature was raised to 1600°C instead of 1400°C as in the previous calculation. The pool conditions and heat transfer results at a pool bulk temperature of 1600°C are listed in Table 13-1. To reach a higher pool temperature, the sideward magnesia thickness had to be increased and the downward magnesia thickness reduced to enhance downward heat removal. The total heat removed from the lower cavity liner (sideward heat + downward heat) was reduced from 60% to 38% of the total heat generation, and the time to reach a quasi-steady-state condition was extended to 4 hr. The new heat distribution is nearly uniform, and the cavity liner temperature is much lower everywhere than the failure limit ($\sim 450^{\circ}\text{C}$). A penalty of this modification is that the percentage of heat flowing upward is increased, which may require forced or natural convection helium cooling for some time. It appears practical to optimize the pool temperature to obtain a relatively uniform heat flux and a lower liner temperature with a reasonable amount of upward heat flow.

Sideward melting of the solid uranium may be slowed down by control of the pool growth pattern. The ideal containment condition in a heavy metal bath core catcher is to have the molten metal contained in a crucible formed by its own solid material. However, since the compound solution is less dense than the unmelted solid, pool growth occurs preferentially sideward (Ref. 13-4). Table 13-1 indicates that at the quasi-steady state, the compound solution is already in contact with the sideward magnesia wall, since for the specified pool temperature, the sideward pool surface temperature is higher than the melting point.

Since a dissolving process is usually much slower than a melting process, it is possible to utilize this special characteristic to control the pool growth pattern by placing a metal with a melting point higher than the pool temperature where a slower pool growth rate is required. In a heavy metal bath core catcher, uranium alloys with different melting temperatures may be installed at different locations to alter the original pool growth pattern. If, for example, pure uranium is chosen as the heavy

TABLE 13-1
POOL CONDITIONS AND HEAT TRANSFER RESULTS
AT A POOL BULK TEMPERATURE OF 1600°C

Stored heat (J)	7.35×10^{11}
Time to reach quasi-steady state (hr)	4.0
Decay heat rate (MW)	41.7
Heat generation from pool (MW)	29.2
Volumetric heat generation (W/m^3)	2.0×10^5
Kulacki-Goldstein Rayleigh number	1.19×10^{12}
Upper pool surface temperature (°C)	1560
Sideward pool surface temperature (°C)	1575
Lower pool surface temperature (°C)	1100
Upward heat flow (MW)	18.2
Sideward heat flow (MW)	6.8
Downward heat flow (MW)	4.2
Sideward heat flux (kW/m^2)	94.7
Downward heat flux (kW/m^2)	68.9
Sideward liner temperature (°C)	276
Lower liner temperature (°C)	231
Sideward magnesia thickness (m)	0.12
Downward magnesia thickness (m)	0.06
Percentage of heat flowing upward from pool	62

metal material and pool temperature is expected not to exceed 1200°C, a layer of epsilon phase U-35% Fe (melting point ~ 1230°C) may be placed at the inner side next to the sideward magnesia wall to slow down the sideward pool growth. The amount of U-35% Fe should be sufficient to survive until the quasi-steady-state condition is reached, where the pool size is maximum. For expected pool temperatures up to 1400°C, stainless steel (melting point ~ 1430°C) may be chosen as the sidewall material. For even higher pool temperatures it is still possible, but more difficult, to find a suitable sidewall material.

13.4.2. Scoping Postaccident Fuel Containment Study for the Up-Flow Core Design

A scoping PAFC study for the up-flow core design was completed. The conclusions of this study are summarized below.

1. Three distinct PAFC conditions may develop. Detailed analyses are planned to predict the scenario most likely to occur. For scenarios a and b, the grid plate with the core barrel and shielding assemblies may still melt down because of thermal radiation from the debris pool formed above the cavity floor.
 - a. If only molten fuel and molten stainless steel from the core and axial blankets drain through the grid plate, only a small pool of fuel and stainless steel is formed above the cavity floor.
 - b. If the radial blanket also melts and drains through the grid plate, a larger pool will form above the cavity floor; but it will develop at a slower rate.

- c. If the core support grid plate can fail during the accident sequence owing to exposure to high-temperature fuel, a large molten pool containing the core, blankets, shielding assemblies, and grid plate can develop.
2. If the core catcher is designed according to the most conservative meltdown condition (i.e., a full core meltdown including the grid plate and shielding assemblies), a total mass of more than 40 m^3 (four times the amount of core debris for the down-flow core) must be contained by the core catcher. A 2.5-m height of core debris may be accumulated above the cavity floor, so that a big space must be provided for this debris mass, which means that the cost of installing a core catcher for the up-flow core design is comparatively high. However, with a thick layer of graphite (0.6 m) formed above the debris pool, upward heat removal is likely to be insignificant and will not depend on helium convection. Blockage of the helium flow path may thus be permitted, and core catcher volume can be greatly reduced with a minimum dropping distance for the core debris. Even with a large core catcher volume, the overall PCRV height is still determined by the steam generator cavity.
3. During normal operation the gamma heating rate in the lower shield of the up-flow core design is about one-tenth of that for the down-flow core design. The requirement for the lower shield is thus very much relaxed. Furthermore, in the lower plenum region, the low helium inlet temperature also reduces the requirement for the thermal barrier in the lower cavity region. Therefore, in the core catcher design (as well as the lower shield design), more considerations can be given to PAFC conditions instead of normal operating conditions.

4. Several PAFC analyses have been planned for the up-flow core design.
 - a. A one-dimensional up/down heat split analysis will examine the downward melting progression and estimate the amount of upward heat removal required.
 - b. Two-dimensional upward heat removal analysis will determine whether the grid plate with the core barrel will melt down owing to thermal radiation from the debris pool surface after a partial core meltdown.
 - c. An assessment of core catcher concepts will determine suitable core retention schemes for up-flow core configurations.

13.5. ENGINEERING RELIABILITY INTEGRATION

A trial program for integrating the design and reliability engineering functions has been initiated. As a first step in the program, a reliability analysis will be performed by a designer on a sample component, and an evaluation will be made of the results and the designer's assessment of the design improvements possible with design reliability techniques. The main loop isolation valve was chosen for the sample problem for the following reasons:

1. The component is simple enough to perform sufficient reliability analyses to evaluate the component within the limited time allocated to do the work.
2. The component is a vital part of the RHR system, so the work will have important safety-related results.

3. The main loop isolation valve has reached a sufficiently advanced stage of design, so the analysis can be reasonably performed.
4. Alternate designs for the component are being considered, and the reliability analysis can help in the selection of a final reference design.

The designer selected for the analysis will have no background in reliability. The reason for this is to see how rapidly a design engineer can become competent in applying FMEA and fault tree methods. It is not expected that the design engineer will become proficient in advanced reliability analysis methods. Reference 13-5 will be the designer's main reference tool for the methods necessary for performing the sample problem, and consultation with experienced reliability engineers will be available.

13.6. GAS-COOLED REACTOR RELIABILITY DATA BANK

The functions of the data bank are to obtain, supply, and store reliability data in support of the probabilistic accident analysis performed under the GCFR and HTGR probabilistic accident and risk analysis tasks. The raw data generated by currently operational collection systems are utilized as data sources.

13.6.1. Data Sources

Periodically updated sources as well as collections and summarized data from reports and periodicals are used to generate the recommended component and system level total failure rates, common cause fractions, and repair times for various failure modes. Data source examples are presented in Table 13-2. In addition to these "normal" data sources, other data collections have been obtained and are kept in the data bank files.

A generic component failure rate data listing has been obtained through the systems reliability service for 72 major component categories.

TABLE 13-2
ACTIVE RELIABILITY DATA COLLECTION SOURCES
FOR ELECTRICAL POWER PLANT APPLICATION

Data Source	Available From	Comments
Nuclear Plant Reliability Data System - 1976 Annual Report of System and Component Reliability (May 1977)	Joint publication of American National Standards Institute (ANSI) (subcommittee N18-20), American Public Power Association (APPA), Edison Electric Institute (EEI), and NRC; prepared by Southwest Research Institute, San Antonio, Texas	Includes more than two years of voluntary reports submitted by about 30 nuclear plants and contains detailed equipment categories and statistical analysis for failure rate; applies directly to nuclear power plants.
Operating Units Status Report (Gray Books)	Issued monthly by NRC from National Technical Information Service, Springfield, Virginia	Contains summary information on failures which caused plant outage and docket report summaries on safety-related equipment and includes plant-level statistics for all licensed U.S. reactors; docket reports are mandatory for operating licenses
Systems Reliability Service	Reliability analysis reports and data available through membership in the Systems Reliability Service, United Kingdom Atomic Energy Authority	Includes special reports on reliability analysis; computer searches for data from gas-cooled reactors in the U.K. and Europe are available to members
IEEE Guide to the Collection and Presentation of Electrical, Electronic, and Sensing Component Reliability Data for Nuclear Power Generating Stations (July 29, 1977)	Institute of Electrical and Electronics Engineers, Inc., 345 East 47 Street, New York, N.Y. 10017	This recently published volume includes component failure rate data gathered through the "Delphi" process of synthesizing expert opinion. The data are organized so that generic component class data can be used when specific component data are not available. The data apply directly to nuclear power plant equipment
EEI Report on Equipment Availability Task Force of the Prime Movers Committee (1973)	Cause Code Summaries available from Edison Electric Institute upon request	Includes plant outage occurrences and down-times allocated by size class to each component in fossil-fired power plants with over 2000 plant years of experience
Nuclear Power Experience, Incorporated	Published monthly by B. J. Verna, P.O. Box 544, Encino, California; available by subscription	Incident information from sources such as docket reports, etc., are classified by plant system for PWRs and BWRs. Although no statistical evaluations are included, order of magnitude failure rates can be estimated on the basis of operating experience in the reporting period. The data are extremely useful for determining failure mode frequency ratios.

This listing includes the number of faults experienced during both operating and calendar time, and in some component categories, cyclic and demand failure rates are given. The generic component classifications have as many as 28 subcategories, such as valves. However, one difficulty in using these data is that considerable engineering judgment is required to match the component definitions in the bank with the components and systems being evaluated.

The IEEE survey of electrical equipment reliability in industrial plants (Ref. 13-6), which served as a major source for the IEEE nuclear reliability data manual (Ref. 13-7), has been entered into the data bank files. Reference 13-6 contains excellent information on the length of plant down-time following equipment failures. Some failure rate discrepancies between the data in Ref. 13-7 and those derived from the failure frequency of components in actual use have been discovered. Additional study of the basic reasons for these discrepancies is needed so that these data can be consistently applied in probabilistic risk assessments.

The preliminary format guide for the 1977 Nuclear Plant Reliability Data System Annual Report (Table 13-2) has been received and it was noted that the system failure category "loss of redundancy" has been added to the failure mode listing. This is a significant step toward obtaining additional data related to common mode and common cause failures from outside sources.

A summary of the data bank activities was completed (Ref. 13-8), and a number of contacts were made with persons engaged in the collection of reliability data. Some failure rate data have been obtained as a result of these contacts.

13.6.2. Data Analysis

"Raw" data sources from reliability data bank collections are evaluated to derive fault tree and initiating event occurrence frequencies which fit the conditions of the analysis. The reliability data evaluated

in this way are then stored in the GA data bank. Current evaluations include fluid cooling reliability, control rod experience in Central Electricity Generating Board Magnox reactors (Ref. 13-9), expected number of plant shutdowns for a demonstration plant, and common cause failure.

13.6.2.1. Expected Number of Shutdowns for a Demonstration Plant. The demand frequency for an RHR system is a function of number of plant shutdowns. Since RHR system reliability depends on the expected frequency of these shutdowns, it is necessary to analyze shutdown frequency data for first-of-a-kind plants, with particular emphasis on the rise-to-power phase, when the frequency of plant outages is somewhat greater than that for a mature plant. Classification of data from dockets and Gray Book LWR plant shutdown experiences (see Table 13-2) has been initiated.

13.6.2.2. Abrupt Loss of Circulators. As a follow-up to the preliminary analysis of abrupt common mode failures of the circulators, both the total unit failure rate derived from severe turbine-generator failure experience and the common mode fraction derived from airline experience with multiple engines are being examined. Additional detailed information from the primary source of the airline data and new data from an independent source maintained in the U.S. are being sought. Obtaining these data has proven somewhat more difficult than originally anticipated; however, considerable new data on the frequency of very severe turbine failures are available, and the preliminary evaluation remains valid.

REFERENCES

- 13-1. "Gas-Cooled Fast Breeder Reactor Quarterly Progress Report for the Period February 1, 1978 Through April 30, 1978," DOE Report GA-A14928, General Atomic Company, May 1978.
- 13-2. Kelley, A. P., Jr., and T. Taniguichi, "A Reliability Analysis of the Residual Heat Removal Systems for a 300-MW(e) GCFR," DOE Report GA-A14653, General Atomic Company, January 1978.

- 13-3. Torri, A., and J. L. Tomkins, "Accident Termination by Element Dropout in the GCFR," in Proceedings of the International Meeting on Fast Reactor Safety and Related Physics, October 1976, Chicago (CONF-761001).
- 13-4. Farhadieh, R., and L. Baker, J., "Simulation Experiments of the Growth of a Hot Fuel Pool in a Sacrificial Bed," Argonne National Laboratory Report ANL/RAS 76-27, September 1976.
- 13-5. Reilly, J. T., "A Review of Methods for the Integration of Reliability and Design Engineering," DOE Report GA-A14748, General Atomic Company, March 1978.
- 13-6. "IEEE Industrial Survey - Report on the Reliability of Industrial Plants," IEEE Transactions on Industry and Applications, Vol. 1A through 10, March/April 1974.
- 13-7. "IEEE Guide to the Collection and Presentation of Electrical, Electronic, and Sensing Component Reliability Data for Nuclear Power Generating Stations," IEEE Standard 500-1977, July 29, 1977.
- 13-8. Hannaman, G. W., and A. P. Kelley, Jr., "Synthesis of Experience Data for Risk Assessment and Design Improvement of Gas-Cooled Reactors," DOE Report GA-A14924, General Atomic Company, May 1978.
- 13-9. Dixon, F., and H. K. Simons, "The Central Electricity Generating Board's Nuclear Power Stations: A Review of the First 10 Years of Magnox Reactor Plant Performance and Reliability," J. Brit. Nucl. Energy Soc. 13 (1974).

14. GCFR SAFETY TEST PROGRAM (189a No. 00588)

Two large-scale experimental test programs are being established to support the development and verification of GCFR accident analysis methods. The GRIST-2 facility is an in-pile transient helium test facility to be installed in the TREAT Upgrade reactor for testing of GCFR fuel under rapid, high-power transient conditions predicted to occur during unprotected loss of flow (LOF) and reactivity insertion transients [transient overpower (TOP)]. The duct melting and fallaway test (DMFT) program is an out-of-pile test program to investigate the behavior of cladding and assembly duct walls during protected loss of flow (PLOF) accident sequences. The major objective of this program is to characterize molten cladding refreezing in the lower blanket and to define conditions for duct fallaway following circumferential duct melting.

14.1. GRIST-2 PROGRAM

14.1.1. Organizational Responsibilities

GRIST program participants met to establish the GRIST-2 work breakdown structure (WBS), to identify the major interface requirements among WBS activities, and to identify appropriate WBS responsibilities. It was the consensus that the GA-proposed WBS identifies the major work tasks and provides a basis for establishing organizational work assignments. The possible inclusion of two additional tasks, i.e., support system and intra-GRIST technical integration (systems engineering), was discussed, and separation of these tasks from existing tasks is being evaluated by HBA.

Task responsibilities, interface requirements, and potential interface problem areas were discussed, and interfacing activities involving the in-pile tube design appear to be the most demanding and complex. The in-pile tube task has major interfaces with the helium system, test train, and test reactor as well as the supporting tasks, i.e., physics, safety, and facility integration. Other tasks having moderately complex interfaces are the physics, safety, and facility integration work areas. Interim organizational task responsibilities based on those proposed by DOE (Ref. 14-1) have been established.

14.1.2. Trade Study

The results of the EG&G trade study have been examined. The objective of this study was to develop a basis for comparing the costs of several alternate GRIST-2 system designs. The designs considered had a bundle size of 7, 19, and 37 rods with either a recirculating, a 1-min blowdown, or a 2-min blowdown helium cooling system. The current design of a 37-rod bundle with recirculating primary and secondary helium cooling loops formed the basis for comparison and had a cost of \$17.75 million (unescalated 1978 dollars without contingency). A cost savings of only \$3.4 million could be achieved by reducing the bundle size to 7 rods and having a 1-min blowdown primary helium system and a passive secondary heat removal system. Although the latter system would be less flexible than a recirculating system, it should adequately serve the needs of the GRIST program during the initial testing phase. The basic reason for the small difference in costs among the various options considered by EG&G is that engineering and analysis are a significant fraction of the total cost, and thus configuration changes have only slight effects on the total cost. HBA is in the process of examining the data presented by EG&G and is expected to make a recommendation on the GRIST-2 bundle size and system configuration in the near future.

14.1.3. Fuel Requirements

ANL has made a strong recommendation for a 7-rod bundle test facility. This recommendation was based on the need for higher power densities and more stringent power flatness profiles than are required for similar LMFBR tests. Furthermore, ANL pointed out that requirements for preirradiated fuel rods would be much easier to fulfill with the smaller bundle size. According to ANL, certain fuel rod requirements need to be met to properly simulate the hypothetical core disruptive accident conditions under study in the GRIST program, and ANL has indicated that some of the following factors may have a major effect on preirradiation facility selection. Because of TREAT Upgrade test reactor power limitations, LOF tests would require highly enriched fuel rods to achieve the very high power densities required to vaporize the fuel. Preirradiation should be conducted in high-pressure helium to achieve the proper gas content and distribution within the fuel. Preirradiation in a thermal reactor would be sufficient for LOF tests since the cladding melts before the fuel columns significantly change configuration, and thus the cladding irradiation history would be unimportant. Preirradiation in a thermal reactor may also have some technical merit since the TREAT reactor is also a thermal reactor, and compatible neutron filtering in both the preirradiation facility and the TREAT reactor may produce the optimum rod burnup profiles needed to achieve the required flat power distributions. TOP tests, on the other hand, may require that preirradiation be conducted in prototypical fast reactor neutron environments because of the need to simulate cladding neutron damage effects. Because of the high power densities in current fast reactor test facilities, it is highly unlikely that they will be able to be used to irradiate fully enriched fuel rods. Therefore, it may be necessary to preirradiate GRIST-2 fuel rods in two separate facilities: a thermal reactor for LOF tests and a fast reactor for TOP tests.

Cost estimates have been made for procuring the fuel needed to carry out the initial 5 yr of GRIST-2 testing. Minimum and optimum test program fuel needs were estimated for 7-, 19-, and 37-rod GRIST-2 systems, and

sample cost estimates were developed assuming three different preirradiation facilities for which some prior cost data existed: BR-2 (HELM), engineering test reactor [ETR (GRIST-1)], and fast test reactor [FTR (CLIRA)]. Fuel fabrication and preirradiation costs were a minimum of \$12 million for the BR-2 reactor because the HELM loop is installed and operative. If it is necessary to conduct irradiations in a fast reactor, then an additional \$48 million would be required to construct and operate a CLIRA facility in the FTR. To supply the number of preirradiated rods for the GRIST-2 test program outlined in Section 14.1.5, BR-2 and FTR irradiation facilities would be needed because of the preirradiation loop bundle size limitations (12-rod bundle in HELM and 13-rod bundle in CLIRA). If only thermal reactor preirradiation of the GRIST-2 fuel was required, then the ETR could adequately supply the GRIST-2 program with irradiated fuel. The estimated cost to construct and operate a loop of adequate size (37-rod bundle) in the ETR to supply the GRIST-2 program outlined in Section 14.1.4 would be approximately \$39 million.

14.1.4. Preliminary GRIST-2 Program Test Plan

The preliminary version of the GRIST-2 test plan has been developed for the initial five years (1983-1988) of TREAT upgrade in-pile tests. These tests will be employed to study the phenomena and integral behavior of GCFR core disruptive accidents (CDA) and to support the CDA analyses for GCFR demonstration plant licensing needs. The preliminary test plan is summarized in Table 14-1. As shown in the table, test phases I and II are for fresh fuel CDA behavior at BOL, and phase III is for the reactor state containing irradiated fuel with a saturated content of fission gas. Phase II may therefore represent CDA behavior of significantly irradiated fuel during either BOEC or EOEC. Phase IV is to obtain some scaling effects for larger bundle tests. The one-month preirradiation requirement for phase II tests is tentative, pending confirmation of possible large amounts of helium sorption in fuel early in life owing to dynamic resolution. The seven-rod bundle size was chosen for two major reasons: (1) TREAT Upgrade energy

TABLE 14-1
GRIST-2 PRELIMINARY FIVE-YEAR TEST PLAN

Test Designation	Type	Bundle Size	Test Fuel Condition	Fuel Type	Rod Power During Preirradiation ^(a)	LOF Fuel Failure State	TOP Ramp Rate (\$/s)
Phase I: fresh fuel tests							
GL1	LOF	7	No preconditioning	UO ₂	NA	Molten	1
GL2	LOF	7	No preconditioning	UO ₂	NA	Vapor	
GT1	TOP	7	No preconditioning	UO ₂	NA		
Phase II: preconditioned BOL fuel tests							
GT2	TOP	7	1-month preirradiation	Mixed oxide	LP		1
GT3	TOP	7	1-month preirradiation, restructured	Mixed oxide	HP		1
GL3	LOF	7	1-month preirradiation	Mixed oxide	LP	Molten	Vapor
GL4	LOF	7	1-month preirradiation, restructured	Mixed oxide	HP		
Phase III: preirradiated fuel tests							
GL5	LOF	7	9-month preirradiation	Mixed oxide	HP	Molten	Vapor
GL6	LOF	7	9-month pre-irradiation	Mixed oxide	HP		
GL7	LOF	3	15-month preirradiation	Mixed oxide	LP	First melting	Molten
GL8	LOF	7	15-month preirradiation	Mixed oxide	LP		

TABLE 14-1 (Continued)

Test Designation	Type	Bundle Size	Test Fuel Condition	Fuel Type	Rod Power During Preirradiation ^(a)	LOF Fuel Failure State	TOP Ramp Rate (\$/s)
Phase III: preirradiated fuel tests (continued)							
GT4	TOP	3	9-month preirradiation	Mixed oxide	HP	Rod failure	1
GT5	TOP	7	9-month preirradiation	Mixed oxide	HP		1
GT6	TOP	7	15-month preirradiation	Mixed oxide	LP		1
GT7	TOP	7	9-month preirradiation	Mixed oxide	HP		0.1
GT8	TOP	7	15-month preirradiation	Mixed oxide	LP		0.1
Phase IV: scaling tests							
GL9	LOF	19	No preconditioning	UO ₂	NA	Molten or vapor	
GT9	TOP	19	No preconditioning	UO ₂	NA		1

(a) NA = not applicable; LP = low power; HP = high power.

deposition in the test bundle may not be sufficient to vaporize fuel for a bundle size larger than seven rods, and (2) a preirradiation facility may not be available to meet both the preirradiation requirements and schedule for a bundle size of 19 rods or larger.

The test plan is designed to extract optimum amounts (in quantity and quality) of in-pile experimental information. As can be seen from Table 14-1, the proposed test plan takes into account the parametric variation of several important factors:

1. Type of fuel (UO_2 , mixed oxide).
2. Reactor power region (high- and low-power rods).
3. Type of unprotected accidents (LOF, TOP).
4. Fuel failure state of LOF case (fuel melting, vaporization).
5. Reactivity insertion ramp rate ($\$/s$, $\$0.10/s$).

14.1.5. GRIST-2 Technical Requirements Review

The GRIST-2 test loop requirements document (Ref. 14-2) has been updated. This document forms the technical basis for evaluating the GRIST-2 system design options developed in the EG&G trade study. Since continuing developments in core design and thermal-hydraulic research on roughened fuel rod bundles will lead to further variations in fuel and bundle parameters over the next several years, the following approach was adopted for revising the GRIST-2 test loop requirements:

1. Use current GCFR reference design parameters as the thermo-hydraulically balanced and consistent basis for GRIST bundles.
2. Assign a limiting value or, where appropriate, a range to each parameter which bounds with reasonable confidence the values that each parameter could take on as a result of design or data improvements.
3. Require that the GRIST test loop be capable of accommodating the limiting values for all parameters designated.

This approach provides adequate flexibility for GRIST-2, eliminates the need to adapt the facility design to ongoing design variations, and does not impose undue hardship in terms of its impact on facility complexity and cost.

Major changes made in the requirements include (1) a broader range of fuel rod length (600 to 1220 mm); (2) a reduced flow rate per fuel rod (maximum 0.025 kg/s per fuel rod); (3) a more stringent power flatness requirement [<1.2 maximum/minimum (local)]; (4) a new axial power shape accuracy (to be defined by ANL); and (5) an increased test bundle pressure drop allowance (maximum 0.32 MPa). The requirement for a 4°C/s bundle inlet temperature change rate has been eliminated.

14.2. DUCT MELTING AND FALLAWAY TEST PROGRAM

14.2.1. FLS-1 Experiment

Testing of the first 37-rod full-length subgroup (FLS-1) began at LASL. LASL selected the seven-test sequence of Table 14-2 (preceded by a vacuum bake-out), which was in general agreement with GA recommendations. Vacuum bake-out and tests 1 through 3 have been completed. Test 4 suffered from a heater rod input power electrical short, but is considered by LASL to have been successfully completed. Once the electrical fault is resolved, the test will continue.

14.2.2. FLS-1 Analytical Support

A complete set of thermocouple temperature predictions for all FLS-1 tests was calculated using the DMFT version of the BOXRAD computer code (Ref. 14-3). These data have been transmitted to LASL for their use in running the tests. Post-test data are being analyzed at GA; however, some problems have been encountered in transcribing the data from the LASL disc to the GA computer. A minicomputer similar to that being used by LASL is being used at GA to read and edit the data for further processing.

TABLE 14-2
FLS-1 TEST SEQUENCE

Test No.	Pressure (MPa)	Power Pulse Duration (s)	Maximum Cladding Temperature (K)	Zero-Power Convection Test Period (s)
1	0.1	120	675	1800
2	9.1	120	675	1800
3	0.1	120	1075	1800
4	9.1	120	1075	1800
5	0.1	120	675	1800
6	9.1	120	1075	1800
7	9.1	(~100)/300 ^(a)	1075/1670 ^(a)	--

^(a) Values correspond to two power input phases: preheat to 1075K and reactor decay power simulation.

REFERENCES

- 14-1. Erb, D. E., "GRIST-2 Interfaces and Organizational Responsibilities," DOE, unpublished data.
- 14-2. "GRIST-2 Test Loop Project Objectives and Requirements - Revised July 22, 1977," EG&G Technical Report PG-G-77-001, July 1977.
- 14-3. "Gas-Cooled Fast Breeder Reactor Quarterly Progress Report for the Period February 1, 1978 to April 30, 1978," DOE Report GA-A14928, General Atomic Company, May 1978.

15. GCFR NUCLEAR ISLAND DESIGN (189a No. 00615)

The purpose of this task is to develop preliminary system designs and general arrangements of the nuclear island so that the feasibility of several nuclear island concepts can be evaluated and the major dimensions of the buildings can be established.

An analysis was performed which indicated the feasibility of a battery power source for the core auxiliary cooling system during a 2-hr station blackout. For each circulator, approximately three hundred thirty 1500-ampere-hour cells will be required with appropriate rectifiers, inverters, and switchgear. Three separate Seismic Category I rooms about 102 m^2 (1100 ft^2) each will house the batteries and auxiliary equipment.

Work has been initiated to supply the structural and system information needed for the RHR study, and RECS schedule input was prepared for the up-flow core study.

Layout requirements were established for the main circulator motor controls. It was decided to locate the motor controls in the switchyard in accordance with the criterion which specifies that the equipment be located close (adjacent) to the main transformer and auxiliary transformer.

Information was provided to Westinghouse regarding the location of the main and auxiliary circulator motor control centers and routing of electric busses/cables together with the proposed scheme for separation of IE and non-IE circuits. Westinghouse requested the information to assist them in preparing the electric drive motor feasibility study.

Calculations were made to determine the pressure inside the confinement building as a consequence of a postulated high-energy (CAHE cooling water)

pipe rupture. Using conservative assumptions for total (initial) water volume and partial (final) pressures for steam/air, the pressure increase is approximately 0.024 MPa (3.5 psig). This resultant low internal pressure (approximately equivalent to a negative external pressure utilized for tornado design) substantiates the square building configuration proposed for the confinement structure.

A study was conducted to determine the impact of the up-flow core concept on BOP, and results of a study on top-mounted and bottom-mounted circulator motors confirmed a preference for the top-mounted configuration. The latter study investigated the feasibility of the configurations for

1. Satisfying the access requirements for construction, maintenance, and in-service inspection.
2. Routing auxiliary services: piping, electric bus/cable, and ducting.
3. Providing adequate separation of IE and non-IE piping.

16. ALTERNATE DESIGN STUDY (189a No. 00759)

The objective of this task is to develop and evaluate alternate design concepts for the GCFR 300-MW(e) plant NSSS configuration, components, and related plant facilities and equipment.

16.1. REFUELING SYSTEMS

For the GCFR alternative design study (Ref. 16-1), bottom refueling was not an option because of a ground rule eliminating refueling penetrations in the bottom of the PCRV. One alternative refueling concept applicable to the up-flow core was studied (Ref. 16-2).

16.2. INTEGRATED INDIVIDUAL PCRV SYSTEM

The concrete pressure vessels investigated for the GCFR alternative design required prestressing of relatively small-diameter units [~ 12.9 -m (43-ft) diameter], which could not be performed using the GA-designed wire-winding machine. Because of this and the problematic design and vulnerability to shear mode failure of the exposed steel cross ducts connecting the PCRV with the steel vessels in a nonintegrated design, an alternative NSSS configuration concept with integrated individual concrete vessels for the reactor core and the steam generators was developed. Wire winding for circumferential prestressing is not possible owing to the irregular geometry of the design. As a consequence, a vessel arrangement based on individual concrete pressure vessels which are radially prestressed by hoop tendons and axially prestressed by longitudinal tendons has been developed (Ref. 16-2). The prestressed vessels are structurally connected either by special bars equipped with shear anchors or by cross tendons. Compared with the multicavity reference configuration, this arrangement considerably reduces the concrete volume. This reduction

varies from approximately 50% for the three-loop arrangement to approximately 30% for the six-loop commercial plant. This system also permits the use of tendons for the circumferential prestressing system (CPS), which would allow retensioning and replacement during the life of the plant.

This design concept offers the possibility of greater freedom in the arrangement of plant components than is possible with a fully integrated PCRV design. The height of the prestressed concrete pressure vessels may remain essentially the same for either type of core, but the location relative to the steam generator vessel may differ with an insignificant change in prestressed concrete pressure vessel volume, depending on the core configuration. The necessity for such design flexibility will depend upon cooling requirements and location of components.

16.3. ALTERNATIVE CONFIGURATIONS

As a result of the alternative configuration having an integrated individual prestressed concrete pressure vessel system and the space limitation imposed by this concept, incorporation of the CACS loops into the main loops was considered. This has the advantage of reducing the number of cross duct entries into the reactor cavity. A large number of cross duct entries could create a design space problem for a large plant with more steam generator loops. In addition, incorporating the CACS in the main loop offers the possibility of improving the primary coolant flow path. This improvement is of particular interest for (1) conditions following loss of a main loop, when the ability to transfer flow quickly from the main to the auxiliary loop, preferably with a minimum number of valve operations, is essential and (2) the application of natural circulation cooling. A configuration for a six-loop up-flow core design was developed in which three core auxiliary heat exchangers (CAHEs) were placed in a duct in the center of the steam generator tube bundle. This arrangement was also applied to a three-loop up-flow core demonstration plant design. Other concepts include a configuration with

a helical bundle CAHE above the steam generator; to provide redundant and diverse circulator drive systems, two 50%-capacity circulators are proposed for each loop. The circulators could be placed in free-standing steel vessels connected by cross ducts embedded in the concrete base mat, or they could be installed within or external to (below) the steam generator prestressed concrete pressure vessel.

Another configuration investigated incorporates an up-flow core and hanging steam generators. Each straight-tube CAHE is arranged within the duct in the center of a steam generator, and the twin circulators are placed in free-standing steel vessels on either side of the steam generator prestressed concrete pressure vessels. The circulators are connected to the steam generator cavities by concentric ducts embedded in the PCRV base mat. The circulator drive may be a combination of electric motors and steam turbines driven by different power sources to provide an independent and diverse circulator drive system. The circulators would run continuously during normal operating conditions, and the auxiliary loop flow would be immediately available if required. The availability and response time required for the CAHE and core auxiliary cooling water system to reach operational status needs to be assessed. Reference 16-1 discusses the general safety and licensing aspects of this concept.

Replacement of the integrated multicavity PCRV recommended for the GCFR by these alternate configurations with integrated/individual prestressed concrete pressure vessel systems is not advocated, and it is considered unlikely that subsequent development and refinement of the design will affect this recommendation. However, there is interest in these design concepts from the point of view of arrangement of the main and auxiliary loop components and their possible application to the multicavity PCRV configuration.

16.4. ALTERNATE RESIDUAL HEAT REMOVAL SYSTEM STUDY

This subtask was initiated to develop and evaluate alternate RHR systems for the GCFR, with the objective of reviewing and possibly

improving the present reference design, particularly with regard to such aspects of the design as safety and reliability. A major requirement of the RHR system is that it provide the necessary core cooling by the main or auxiliary loops following reactor trip and normal shutdown after a DBDA or during depressurized refueling. It is especially important to ensure cooling of the core in the event of loss of main loop circulation and during transfer of the cooling function to the auxiliary loops.

During this quarter various RHR systems employed by or proposed for other reactor systems (including fast breeder reactor designs) were reviewed. Requirements and criteria for the GCFR were also examined. A number of alternate system options have been prepared and are in the process of being screened to reduce the number to two or three options to be studied and evaluated in greater detail.

REFERENCE

- 16-1. "GCFR Alternative Design Study," DOE Report GA-14856, General Atomic Company, February 1978.
- 16-2. "GCFR Alternative Design Study Addendum," General Atomic Company, unpublished data, April 1978.

17. ALTERNATE FUEL CYCLES (189a No. 00759)

The purpose of this task is to determine the characteristics of a GCFR fueled with a variety of fissile and fertile materials. These characteristics will be used to evaluate various reactor fuel strategies which could be employed to improve the proliferation resistance and/or energy capability of future nuclear power systems.

A general outline for the study was established previously, and the preliminary analysis and evaluation phase has been completed (Ref. 17-1). Preliminary fuel strategy studies indicate that the fuel cycles of greatest interest for the GCFR are

1. Pu/U core with U or Th blankets.
2. Pu/Th core with Th blankets.
3. (Low-enriched) U-233/Th core with Th blankets.

During the second phase of the program, core materials and configuration studies are being performed to identify the optimum states for the various parameters for each fuel composition.

During this quarter, coarse parametric studies have been done on two of the fuel cycles of interest (Pu and Pu/Th cores). The design characteristics of large core designs for the Non-Proliferation Alternate Systems Assessment Program (NASAP) have been revised, and a preliminary Pu/U carbide core design has been completed.

17.1. APPROXIMATE SENSITIVITY ANALYSIS

In an attempt to further optimize the GCFR core design for the fuel cycles being studied, a series of crude sensitivity calculations was made with CALIOP (Ref. 17-2). From previous calculations it was known that breeding ratio, fissile production, and doubling time are sensitive to variations in fuel volume fraction or fuel rod diameter. The sensitivity calculations were performed for a 1200-MW(e) reactor by varying the fuel rod diameter from 5.5 to 10.0 mm. Core pressure drop was held constant at 289.6 kPa.

Figure 17-1 shows the sensitivity of the breeding ratio and its spatial components to variations in fuel rod diameter for a Pu/U core with all-Th blankets. As can be seen, the total breeding ratio increases monotonically with increasing rod diameter while fissile production in the blankets drops. As a result, U-233 production in the blankets decreases as total fissile production increases. Figure 17-2 presents the total breeding ratio, reactor rating, and doubling time as a function of fuel rod diameter. As can be seen, the doubling time can be expected to be reduced to a minimum at around an 8.0-mm fuel rod diameter. Figures 17-3 and 17-4 give similar information, including doubling time for a Pu/U core with uranium blankets and a Pu/Th core with thorium blankets, respectively. Although the magnitude of the values changes appreciably, the doubling time minimum remains near 8.0 mm.

Based on these approximate calculations, an 8.0-mm fuel rod diameter was chosen to submit to NASAP for the Pu/U core for both Th and U blankets. Recognizing that a Pu/Th core is primarily a Pu to U-233 transmuter, the doubling time criterion is of little value. To maximize U-233 production, a 10.0-mm fuel rod diameter was selected for the fuel cycle core design.

17.2. MASS FLOW INFORMATION

Table 17-1 presents some of the characteristics of the original submittal to NASAP (Ref. 17-3). Subsequent refinements of the ground rules

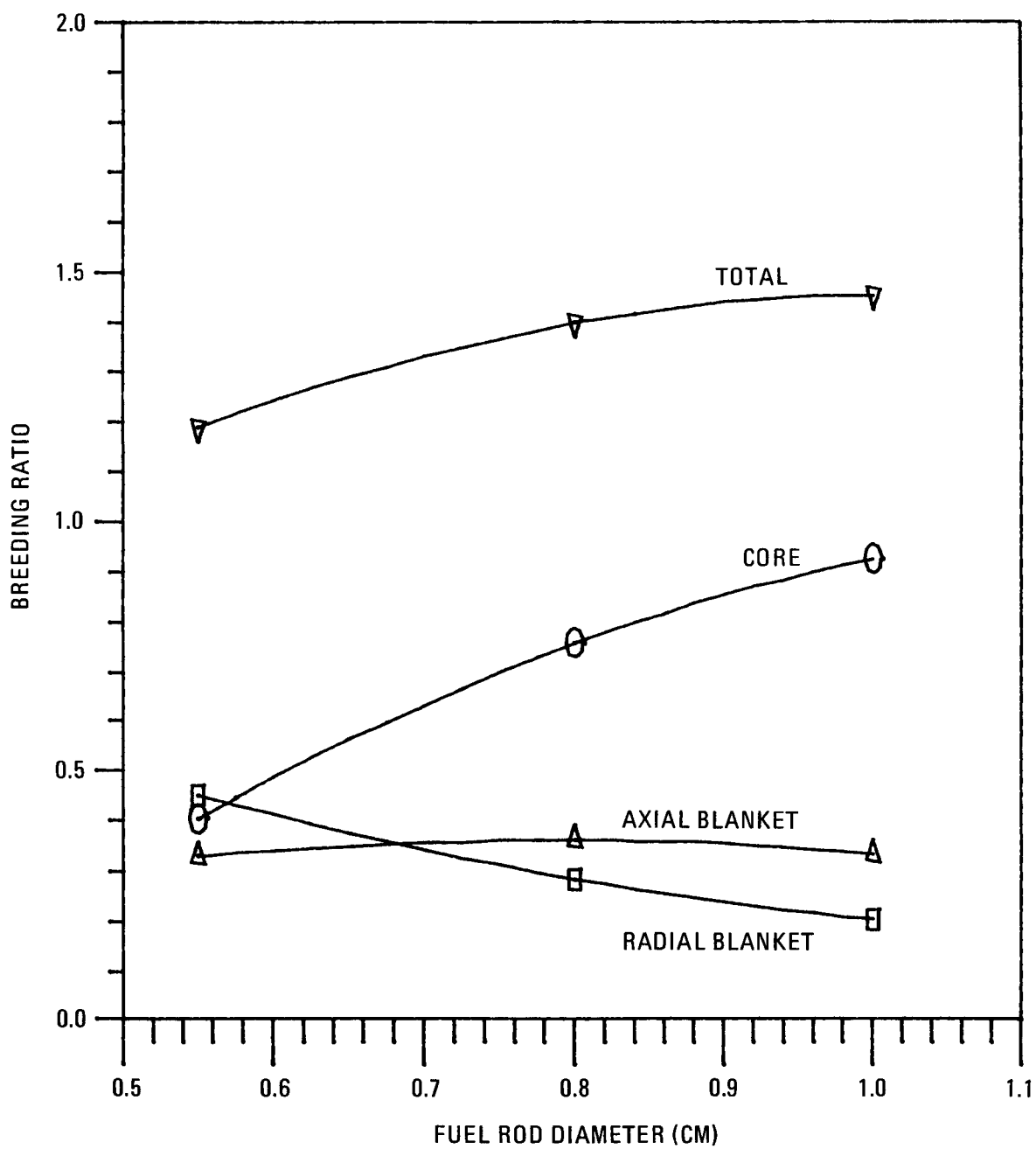


Fig. 17-1. Approximate characteristics for Pu/U core, Th axial blanket, Th radial blanket

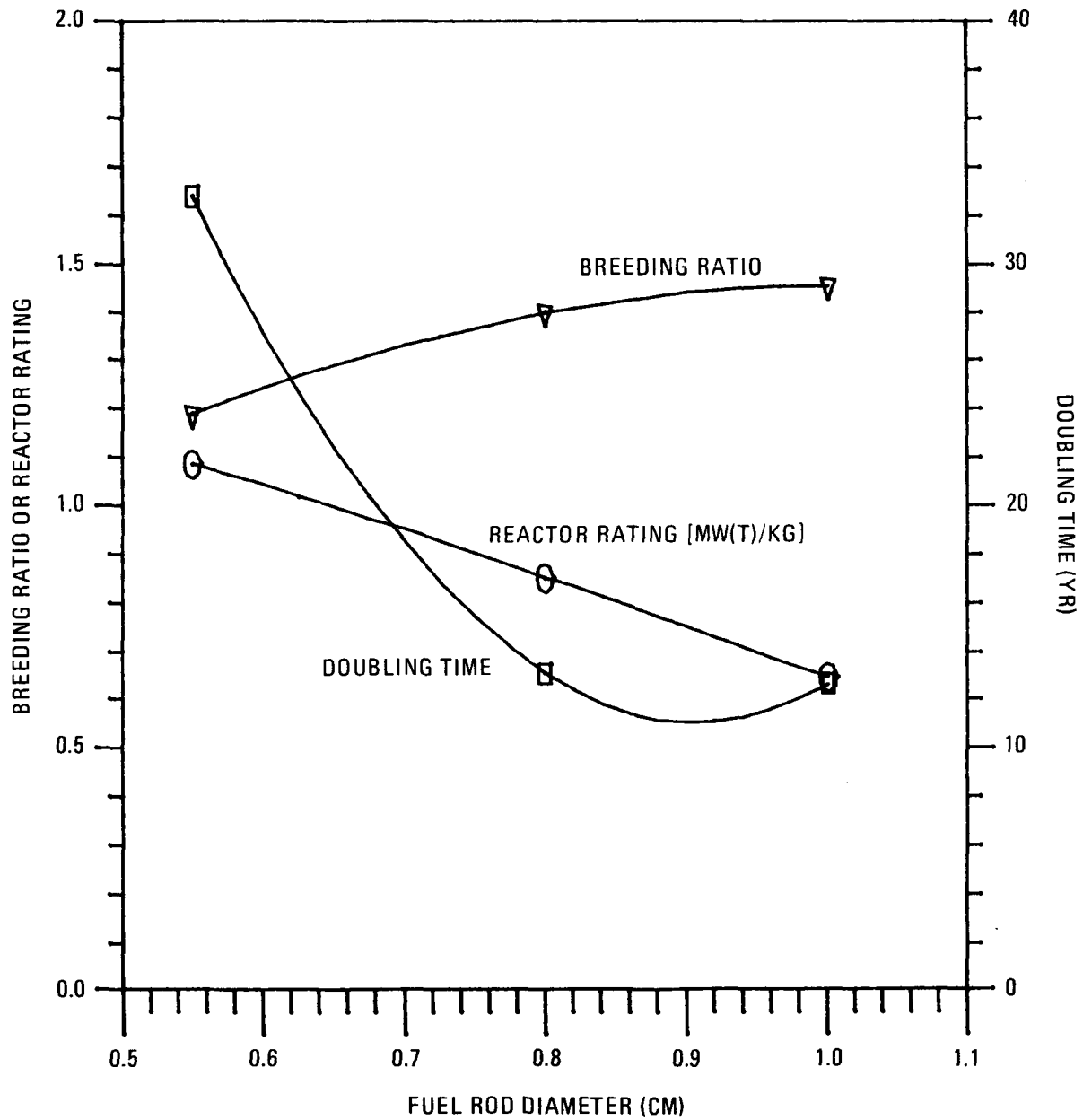


Fig. 17-2. Approximate characteristics for Pu/U core, Th axial blanket, Th radial blanket

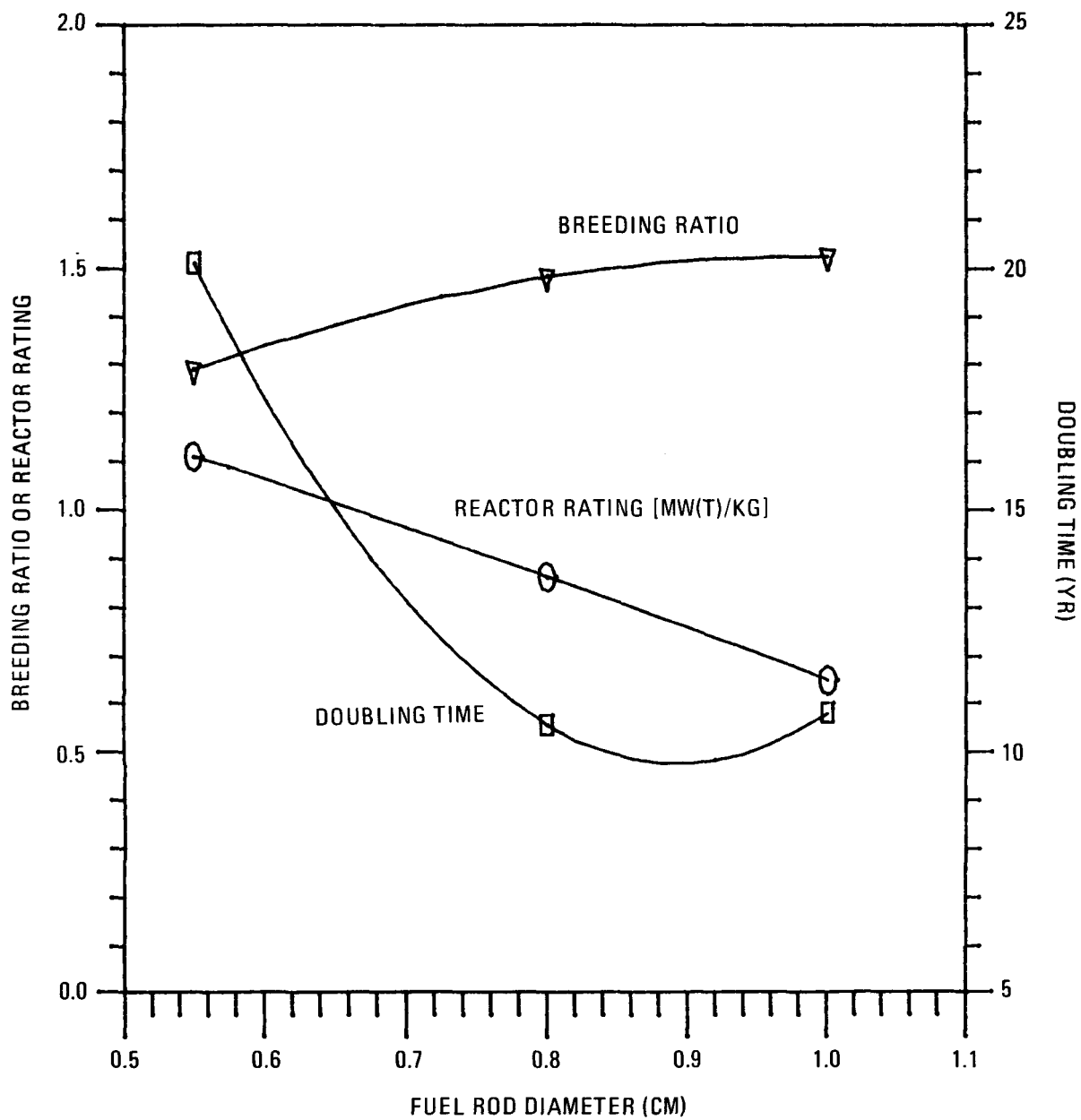


Fig. 17-3. Approximate characteristics for Pu/U core, U axial blanket, U radial blanket

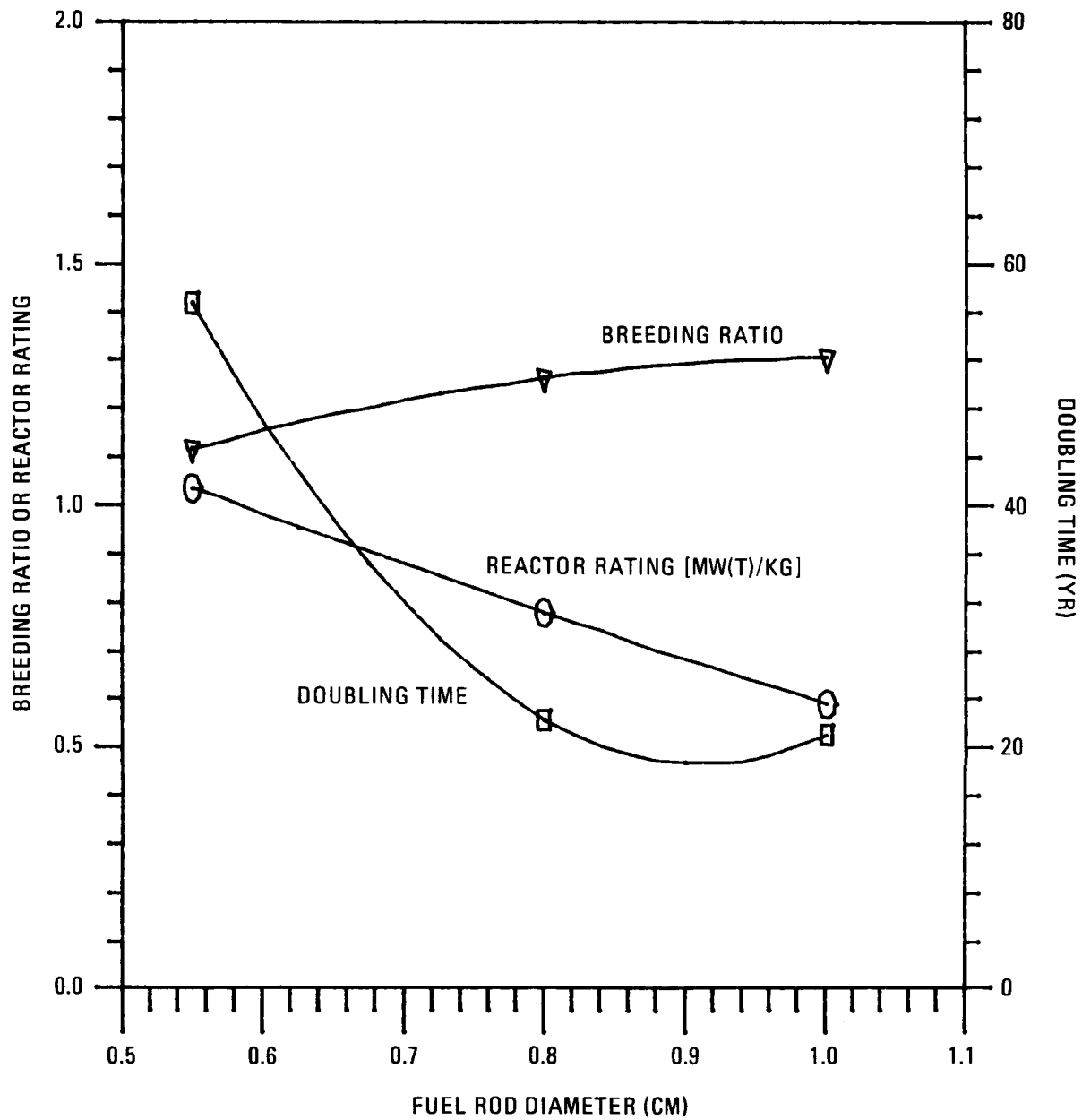


Fig. 17-4. Approximate characteristics for Pu/Th core, Th axial blanket, Th radial blanket

TABLE 17-1
FUEL CYCLE AND CORE DESIGN CHARACTERISTICS
FOR GCFR ALTERNATE FUEL CYCLE DESIGNS

	Design 1	Design 2	Design 3
Fuel material			
Core	PuO ₂ /UO ₂	PuO ₂ /UO ₂	PuO ₂ /ThO ₂
Axial blanket	ThO ₂	UO ₂	ThO ₂
Radial blanket	ThO ₂	UO ₂	ThO ₂
Breeding ratio (beginning of initial cycle)			
Core	0.79	0.81	0.87
Axial blanket	0.40	0.41	0.34
Radial blanket	0.28	0.30	0.20
Total	1.47	1.52	1.41
Initial fissile inventory [kg/GW(e)]	344	3300	5475
Net yearly fissile production [kg/GW(e)]			
Pu	-195	305	-635
U-233	470	--	840
Fuel management schedule			
Refueling interval (yr)	~1	~1	~1
Fraction of core replaced annually	~1/3	~1/3	~1/3
Fraction of radial blanket replaced annually	~1/6	~1/6	~1/6
Fuel residence time (yr)	~3	~3	~3
Maximum burnup (MWd/metric ton)	110,000	110,000	85,000
Capacity factor (%)	75	75	75
Electrical power [MW(e)]	1200	1200	1200
Efficiency (%)	36.5	36.5	36.5
Core height (cm)	110	110	110

TABLE 17-1 (Continued)

	Design 1	Design 2	Design 3
Axial blanket length (cm)	60	60	60
Number of assemblies			
Fuel	222	222	222
Control	31	31	31
Radial blanket	198	198	198
Total number of fuel rods	78,996	78,996	78,996
Fuel rod diameter (mm)	8	8	10

suggested by DOE for fast breeder reactor design require that these characteristics be modified to

1. Reduce linear heat rate from ~ 55 to 49 kW/m.
2. Preclude the possibility of duct-to-duct interaction.
3. Reduce burnup by $\sim 10\%$.

These changes were achieved by increasing the duct-to-duct gap and lengthening the active core by $\sim 15\%$. Breeding ratio and fissile inventory increased. The revised information for all three designs was submitted to NASAP (Ref. 17-4).

17.3. PRELIMINARY Pu/U CARBIDE CORE DESIGN

Preliminary design calculations on a Pu/C carbide core were completed. The higher conductivity allows a higher linear heat rating with carbide fuel. These characteristics permit a core with a higher fuel volume fraction for the carbide core design. As a result, the breeding ratio can be expected to increase by ~ 0.20 . The preliminary design suggests that breeding ratios as high as 1.70 can be achieved in a GCFR with carbide fuel.

17.4. ALTERNATE FUEL CYCLES MATERIALS

The objective of the alternate fuel cycle materials subtask is to provide guidance on and assessment of materials to the alternate fuel cycle core design and management studies. Material assessment for the GCFR is somewhat different from that for the LMFBR because of the use of vented fuel.

A description of the GCFR fuels and materials and irradiation programs was prepared for submission to the International Nuclear Fuel Cycle Evaluation (INFCE) study (Ref. 17-3). This description indicated that current GCFR programs are based on a reference mixed oxide $[(\text{Pu-239}, \text{U-238})\text{O}_{1.96}]$ fuel. The extensive LMFBR program and the supplementary GCFR program,

which concentrates on areas in which the GCFR fuel differs from that of the LMFBR, are expected to provide a data base adequate for licensing a GCFR demonstration plant using mixed oxide fuel. However, adoption of any alternate fuel being considered by INFCE ($^{233}\text{UO}_2$ - $^{238}\text{UO}_2$, $^{233}\text{UO}_2$ - ThO_2 , $^{239}\text{PuO}_2$ - ThO_2) would require an expanded development program which would almost certainly include vented and fast flux irradiation experiments.

REFERENCES

- 17-1. Hamilton, C. J., "A Preliminary Study of Alternate Fuel Cycles for the Gas-Cooled Fast Breeder Reactor," General Atomic Report GA-A14536, August 1977.
- 17-2. Thompson, W. I., "CALIOP - A Multichannel Design Code for Gas-Cooled Fast Reactors," General Atomic Company, unpublished data.
- 17-3. Broido, J. H., General Atomic Company, private communication, May 24, 1978.
- 17-4. Broido, J. H., General Atomic Company, private communication, June 14, 1978.