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**FAST FLUX TEST FACILITY
BACKUP DESIGN PROJECT
SECOND QUARTERLY REPORT
SEPTEMBER-NOVEMBER 1967**

U. S. ATOMIC ENERGY COMMISSION
CONTRACT NUMBER BDR-341
BETWEEN PACIFIC NORTHWEST LABORATORY
AND THE GENERAL ELECTRIC COMPANY

ADVANCED PRODUCTS OPERATION

GENERAL ELECTRIC

SUNNYVALE, CALIFORNIA

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FAST FLUX TEST FACILITY
BACKUP DESIGN PROJECT
SECOND QUARTERLY REPORT

September 1, 1967—November 30, 1967

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SECTION I

INTRODUCTION

This quarterly report documents the work conducted on the FFTF Backup Design on Contract BDR-341 between Pacific Northwest Laboratory and the General Electric Company during the period of September 1, 1967 through November 30, 1967.

The objective of the FFTF Backup Design is to provide the capability of replacing the reference FFTF split conical core design at any point in its evolution up to and including initial operation, with an open lattice vertical core and associated handling system, instrumentation, control rod and top head configuration making use of the reference design vessel, primary piped coolant system, hot cell configuration, auxiliaries, containment arrangement, and central control system.

The major backup design effort during the quarter involved that work relating to the establishment of an envelope of responsibility and functional specifications for the definition of the interface between the backup design and the reference design.

Work on defining the engineering development required in support of the backup design continued. Design checklists and a calculation data base were established in draft form.

Backup conceptual design primary coolant loop cost effectiveness studies were reevaluated utilizing PNL's system cost estimates. This reevaluation has led to a recommendation of three primary coolant loops each of 133 MWt capacity. This recommendation represents a change from the earlier recommendation of three 200 MWt loops. The results of evaluating four 133 MWt loops showed this case to be barely outside the range of uncertainty when compared with three 133 MWt loops, indicating

that the curve is relatively flat and selection of four 133 MWt loops could be made based on a lower absolute unavailability.

Further detail work is in progress in the areas of core parameter studies, core clamping methods, core instrumentation, vessel shield plug and control rod drives. The refueling operations and refueling equipment are also being looked at in greater detail.

Parametric studies of the effect of variations in fuel pin diameter, fuel channel wall thickness and materials, and closed test loop volume showed only small peak neutron flux degradations from the base case peak neutron flux of 1.02×10^{16} n/cm²-sec. A comparison of PNL and G. E. neutronics calculations showed close agreement. PNL's calculation of the backup design reflector control rod worth by means of transport calculation methods confirmed earlier reflector rod worth calculations by G. E. based on diffusion theory.

Work was initiated in cooperation with PNL to evaluate the feasibility of including a backup design critical experiment in the phase B reference design initial experiment series. A recommended backup design critical program is presently being evaluated by PNL.

Transient accident analysis calculations are in progress in the safeguards area. An interpretation of the AEC licensing criteria (No. 44) has led to a safeguards requirement of two independent emergency coolant sources being available after the loss of a single primary coolant loop.

Project reviews were held at Richland with PNL on October 23-24, and in Washington, D. C. with PNL and RDT on November 16-17.

SECTION II

PLANT PERFORMANCE

Figure 2-1 shows a pictorial of the FFTF backup design. The Plant Performance Table shows the latest design parameters for the reference design. Those items marked with an asterisk (*) have been changed from the values reported in the previous quarterly report. ⁽¹⁾

2.1 PLANT PERFORMANCE TABLE

2.1.1 Test Loops

Closed Loops	6
Location	1 central 3 midcore 2 periphery
Open Loops	2
Flux	
Peak Level	1.02×10^{16} *
Fraction above 0.1 MeV	0.66
Loop Cells	
Number	6
Size	8000 ft ³

2.2 REACTOR

2.2.1 Core

Type	Open Lattice, vertical
Power	400 MWt
Dimensions	
Length	33 inches
Equivalent Diameter	42 inches
Composition	
Fuel	PuO ₂ -UO ₂
Structure	Stainless
Coolant	Sodium

Core Channels

Fuel	127
Closed Loops	6
Open Loops	2
Backup Control	3
Total	138

Fuel Channel

Pins/Channel	127
Dimension	
Across Flats	3.301 inches*
Channel Material	Inconel 800

Fuel Pins

Diameter	0.210 in. o.d.
Cladding Material	316 Stainless Steel
Cladding Thickness	0.015 inch
P/D	1.26

Fuel Cycle

90 days
(Preliminary—
based upon 0.8
plant factor and
70,000 MWd/T,
20% of core
elements are
replaced each
cycle.)

2.2.2 Reflector

Dimension

Top	12.0 inches
Bottom	12.0 inches
Radial	10.5 inches

Material

Nickel

2.2.3 Control

Primary Control

Location	Reflector first row
Number	
Single Element	11
Double Element	16
Total	27

*Indicates change from last report period.

Worth					Design	Operation
Single Element	\$0.7		Grid Plate		1000° F	700° F
Double Element	\$1.1		Reactor Vessel		1200° F	1000° F
Total Worth in Reflector	\$20 to \$30		Vessel Head		1000° F	200° F
Material	B ₄ C (natural or partially enriched)					
Drive	G. E. compact mechanical drive		2.2.8 Power Distribution			
Backup Control			Radial Flux Peaking		1.4	
	Location	In-core	Axial Flux Peaking		1.2	
	Number	3	Overpower Allowance		1.2	
	Worth	\$1—\$2.7 each \$3—\$8 total	Fuel Burnup		1.05*	
	Material	B ₄ C	Fuel Nonuniformity		1.04*	
Drive	Fluidic system or G. E. compact drive		2.2.9 Hot Channel Factor			
2.2.4 Vessel			Statistical		1.1	
Diameter	16.0 ft.		2.3 SYSTEM			
Thickness	0.75 inch		2.3.1 Primary Coolant			
Material	Stainless Steel Type 304		Number of Loops		3*	
2.2.5 Coolant			Rating		133 MWt/each*	
Flow	15.1 × 10 ⁶ lb/h		Piping			
Inlet Temperature	700° F		Material		Type 304 Stainless Steel	
Outlet Temperature	1000° F		Size			
Core ΔP (nominal Plenum to Plenum)	100 psi*		Reactor Outlet		20 inches*	
2.2.6 Cover Gas			Reactor Inlet		18 inches*	
Type	Argon		Heat Exchanger Rating		133 MWt/loop*	
Pressure—operating	±10 in. w.g.		Coolant Loop Unavailability		7.8%	
2.2.7 Temperature			Temperature			
			Design		1200° F	
			Operation		1000° F	
			2.3.2 Containment Vessel			
Peak Channel	100% Power	120% Power	Type		Cylindrical	
Fuel—maximum	4600° F*	5200° F*	Diameter		135 feet	
Fuel surface—maximum	1875° F*	1990° F*	Thickness		1.1 inches	
Cladding—maximum (i. d.) (hot spot)	1225° F*		2.3.3 Emergency Coolant			
Sodium—maximum (hot stream)	1080° F*		Type		NaK	
			Heat Load		Approximately 4 MWt	

*Indicates change from last report period.

2.3.4 Vessel Cavity

Gas	Nitrogen
Pressure	±10 in. w.g.
Liner	Steel

Design Operating Temperature	100° F
------------------------------	--------

Design Pressure	
Estimated Maximum	10 psig
Estimated Minimum	-6 in. w.g.

2.3.5 Equipment Cell

Gas	Nitrogen
Pressure	±10 in. w.g.

Operating Pressure	
High	-2 ±1 in. w.g.
Low	+2 ±1 in. w.g.

Leakage (estimated maximum)	1% cell volume/day at design pressure
-----------------------------	---------------------------------------

2.3.6 Loop Cell

Gas	Nitrogen
Pressure	±10 in. w.g.
Floor Liner	Steel

2.4.6 Refueling Time

Shutdown—Preparation	8 hours
Reactor Servicing	44
Driver Refueling	42
Control Rod Replacement	8
Startup Preparation	8

Total	110 hours
-------	-----------

2.4 HOT CELL2.4.1 Size

Length	72 feet
Width	22 feet
Height	78 feet
Volume	124,000 ft ³

2.4.7 Major Equipment

Bridge Crane Mounted Telescoping Tube Hoists With Fuel Grapple Mechanisms	2
Bridge Crane Mounted General Purpose Manipulators	2
Master-Slave Manipulators	3
Bridge Crane	25/100 T
Man Access Equipment	
Decay and Storage Tanks	
Spent Fuel Transfer Thimbles	

2.4.2 Transfer Cell

Maximum Size Transfer	
Diameter	2.8 feet
Length	37 feet

2.4.3 Fuel Handling Method

Closed, finned transfer thimble, sodium filled, natural circulation cooling.

2.5 SAFEGUARDS

2.5.1 Containment	Double
-------------------	--------

2.5.2 DBA Energy Release

Estimated Maximum	1500 MW-sec
-------------------	-------------

2.4.4 Fuel Storage Method

In-cell, sodium filled decay pools with natural circulation. Secondary cooling by forced circulation.

2.5.3 Credible Accidents

Flow Coastdown
Loss of Single Coolant Loop
Piping Leakage (Including Guillotine Pipe Failure)
Single Fuel Bundle Meltdown
Control Rod Withdrawal
Refueling Bundle Drop-In

2.4.5 Refueling Cell Atmosphere

Atmosphere	Argon
Design Temperature—Accident	250° F

2.6 NUCLEAR

2.6.1 Core Composition

Fuel Channel Volume Fraction

Fuel	0.359
Steel	0.079
Incoloy 800	0.113
Sodium	0.449

Experimental Loop Volume Fraction

Steel	0.5
Sodium	0.5

Backup Control Rod Volume

Steel	0.33
Sodium	0.67

2.6.2 Midcycle Fuel Composition (Atom %)

Uranium	67.6%
Plutonium	28.7%
Fission Products	3.7%
Plutonium Isotopic Concentration	
Pu-239	63.7%
Pu-240	31.5%
Pu-241	3.3%
Pu-242	1.5%

Uranium Isotopic Concentration

U-235	0.2%
U-238	99.8%

2.6.3 Reactivity

Doppler

$$T \frac{dk}{dT} \text{ (Sodium In)} = -0.006^*$$

*Indicates change from last report period.

$$T \frac{dk}{dT} \text{ (Sodium Out)} = -0.004^*$$

Na Void

Maximum Positive	\$+1.5
Total Core	\$-4.0

Maximum Fuel Bundle Worth

\$+2.2

Fuel Meltdown (Top 1/3 into Mid 1/3)

\$+0.5

2.6.4 Fuel Cycle

Discharge Burnup	70 MWd/kg
Number of Batches	5
Burnup/Cycle	14 MWd/kg
Fuel Cycle Period	90 days
Operating Cycle	72 days

2.6.5 MeV/Fission 215

Fraction Energy Absorbed in Fuel	0.91
----------------------------------	------

2.7 RADIATION LEVELS

2.7.1 Dose Rate During Operation

Operating Floor	0.5 mR/h
Hot Cell Floor	50 mRem/h

2.7.2 Equipment Cells

From Na-22 (In Working Area)	1.R/h
------------------------------	-------

2.7.3 Neutron Exposure (nvt > 1 MeV, 20 Years)

Vessel Wall Core Midplane	7×10^{17}
Radial Blast Shield	5×10^{17}
Vessel Cavity Wall	7×10^{16}
Core Support Plate	8×10^{19}
Vessel Head	3×10^{10}

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FIGURE 2-1. FFTF BACKUP DESIGN

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SECTION III

PLANT SYSTEMS

The Plant Systems Design effort is directed toward providing a concept compatible with the backup core design, consistent with the overall broad objectives of the FFTF Program, as provided for by agreement between GE and PNL. The initial phase of the systems work involved selection of a conceptual design, trade-off studies and initial cost effectiveness evaluations. These studies are reported in References (1), (2) and (3). The current effort has emphasized a more detailed study of the system, and generation of system functional specifications in preparation for establishment of the system interface between PNL and G.E. At the completion of this phase of work, an effective interface will be established between G.E. and PNL. G.E. will then confine detail work to the backup core design concept as defined by the interface envelope.

Emphasis during the past quarter has been placed on developing and establishing a firm design base for the major systems associated with the backup design concept. Effort has centered upon providing supporting design information for the concept selection as reported in Reference (2), and the heat transfer system selection document, Reference (3). Individual meetings were held with PNL and RDT to assess progress of the backup design, develop information, and resolve interface areas between the reference and backup design concepts.

3.1 GENERAL

3.1.1 Plant Availability Targets

Availability targets have been apportioned for separate subsystems of the overall plant.

Availability goals will be utilized to assess each major subsystem and determine what improvement is necessary and the benefits that accrue from design changes.

The FFTF Reactor System has an overall target availability of 75%. This appears to be a difficult goal if past sodium reactor experience is used as a guide. However, to be responsive to this goal, design target availabilities have been established for the backup design. The overall plant systems have been broken down into eight major categories illustrated in Table 3-1. For each category a target unavailability has been assigned. The target unavailabilities were apportioned to meet the 75% availability directive. The practical unavailabilities are those which appear reasonable to achieve without inordinate expense. Basis for the estimates has been to rely upon past reactor operating experience, particularly, Hallam, Dresden I, and MTR, ETR history. The goal is to achieve the target availabilities in each category. However, the incentives for higher availability must be traded against incremental cost.

Particular attention should be given to systems 4.0 and 1.0 which appear to be areas where improvement can achieve the most beneficial results.

3.1.2 Design Check Lists

Design Check Lists were completed to assist in the planning and development of the preliminary design following completion of the conceptual effort. The objective of the Systems Design Check List is to outline design effort necessary to provide a preliminary design of the plant systems. The

TABLE 3-1

SYSTEM AVAILABILITY TARGETS

	Unavailability—U		Basis for Selection
	U = $\frac{\text{Down Time}}{\text{Total Time}}$		
	Target	Practical	
1.0 EXPERIMENTAL FACILITIES Closed Loop Heat Transfer (six loops) Closed Loop Fuel Test (six tests) Capsules Open Loop Systems Test Handling System Test Examination System	0.085	0.20	MTR-ETR Operating History
2.0 MAIN HEAT TRANSFER SYSTEM Main Primary Main Secondary Air Cooler Sodium Service Fill/Drain	0.03	0.05	GEAP-5552 Reference (2)
3.0 AUXILIARY SYSTEMS Argon Gas Distribution Nitrogen Gas Distribution Nitrogen Cell Cooling Argon Cell Purification Argon Cell Cooling	0.01	0.01	
4.0 DRIVER FUEL SYSTEMS Instrumentation—(T/CS ~ 0.0006) Cladding Channel and Structure Fuel Fuel Handling System Fuel Storage System	0.10	0.15	Backup Design First Quarterly Report GEAP-5550 Reference (1)
5.0 PLANT CONTROL AND REACTOR SAFETY SYSTEM Reactor Control Instrumentation Reactor Safety System Reactor Control Rods and Drives Operator Error	0.005	0.01	Hallam Data NAA-SR-10743

TABLE 3-1 (Continued)

	Unavailability—U		Basis for Selection
	U = $\frac{\text{Down Time}}{\text{Total Time}}$		
	Target	Practical	
6.0 CONTAINMENT AND PLANT SAFEGUARDS	0.005	0.005	Dresden I
Building Containment System			
Inner Containment Barrier			
Radiation Detection and Control			
Radioactive Liquid Waste			
Radioactive Gas Waste			
7.0 STRUCTURES	0.01	0.01	Dresden I
Reactor Vessel			Hallam
Vessel Internals			NAA-SR-
Shielding			10743
Reflector			
Structure			
Refueling Cell			
8.0 ELECTRICAL AND UTILITIES	0.005	0.005	
Normal Plant Power			
Emergency Power			
Building Air Conditioning			
TOTAL	0.25	0.44	

end product of the preliminary design phase involves a flow sheet for each system, outline dimension drawings for major components, plant system arrangement drawings, a data sheet for each major item and a system description. The systems are identified and organized into functional groupings. Each system and each major component will have a check list. The systems are organized as follows:

0.0 General Plant Systems

These systems are general and tend to involve all other reactor systems.

0.1 Reactor and Main Heat Transfer System Control

0.2 Reactor Plant Containment System

0.3 Building Access and Radiation Control

0.4 System Interface with PNL

0.5 Plant Heat Balance

0.6 Plant Availability

0.7 Development Programs

- 1.0 Reactor Vessel and Internals
- 2.0 Main Heat Transfer Systems
 - 2.1.0 Main Sodium Systems
 - 2.1.1 Main Primary Sodium System
 - 2.1.2 Main Primary Sodium Service System
 - 2.1.3 Main Secondary Sodium System
 - 2.1.4 Main Secondary Sodium Sodium Service System
 - 2.2.0 Main Air Heat Dump System
 - 2.3.0 Safety Tank Cooling System
- 3.0 Refueling Cell and Internals
- 4.0 Experimental Facilities
 - 4.1.0 Closed Loop Facilities
 - 4.1.1 In-Core Facility Tube
 - 4.1.2 Closed Loop Primary Sodium System
 - 4.1.3 Closed Loop Secondary Sodium System
 - 4.1.4 Closed Loop Air Dump System
 - 4.2.0 Open Loop Facilities
 - 4.2.1 System with Extension for Instrumentation
 - 4.2.2 System with Instrumentation as Driver Fuel
 - 4.3.0 Other Experimental Facilities
- 5.0 Gas Service Systems
 - 5.1.0 Argon Systems
 - 5.1.1 Argon Supply
 - 5.1.2 Argon Circulating System for Refueling Cell
 - 5.1.3 Argon Purification System for Above
- 5.2.0 Nitrogen System
 - 5.2.1 Nitrogen Supply
 - 5.2.2 Nitrogen Cooling System
- 5.3.0 Breathing Air System
- 5.4.0 Instrument Air
- 6.0 Plant Instrumentation
 - 6.1.0 Reactor and Process Control System
 - 6.2.0 Reactor Safety System
 - 6.3.0 Neutron Monitoring System
 - 6.4.0 Radiation Monitoring System
 - 6.5.0 Sodium Coolant Instrumentation
 - 6.6.0 Sodium Service Instrumentation
 - 6.7.0 Argon System Instrumentation
 - 6.8.0 Nitrogen System Instrumentation
 - 6.9.0 Waste Gas Instrumentation
 - 6.10.0 Waste Liquid Instrumentation
 - 6.11.0 Miscellaneous Instrumentation
- 7.0 Reactor Plant Services
 - 7.1.0 Site Electrical Power Distribution
 - 7.2.0 Emergency Power Supply
 - 7.3.0 Building Refrigeration
- 8.0 Structures and Arrangement
 - 8.1.0 Containment Vessel
- 9.0 Radioactive Waste Systems
 - 9.1.0 Radioactive Gas Waste System
 - 9.2.0 Radioactive Liquid Waste System

3.2 SODIUM SYSTEMS

The Plant and Coolant Systems arrangement is illustrated in the system isometric shown in Figure 2-1. Detail of the system arrangement is described in Reference (2).

The main coolant system consists of three main primary piping loops elevated above the core. Reactor outlet coolant is directed from the core through the reactor outlet piping to the suction side of the free surface centrifugal pump. Sodium enters the suction side of the pump and discharges through a pipe to the heat exchanger. A small amount of discharge sodium is directed to the hydrostatic bearing. Excess sodium from the bearing is allowed to rise in the case and then flow through an overflow pipe back to the reactor vessel upper plenum. Main coolant enters the heat exchanger at the upper end, at 1000° F, flows through the unit transferring 133 MWt of energy to the secondary system and exits at 700° F. Return sodium passes through a check valve, enters the reactor vessel and flows into the reactor lower plenum. Figure 2-1 illustrates the loop configuration within the containment building.

3.2.1 Coolant System Accident Considerations

As the design developed past the conceptual stage, more detailed study was devoted to the safety aspects of the main coolant system. In particular, the assurance of core cooling capability following a coolant system leakage accident must be established. Safeguards considerations were reviewed and it was determined that a minimum of two coolant loops must be available to remove core decay heat following a leakage accident. This requires a minimum of three operating loops at all times (see Section X).

The following discussion is intended to bring into focus some of the considerations

involved in a gross sodium leakage accident. The main heat transfer system for the FFTF backup concept is designed to provide emergency cooling to the core for any of the following coolant system accidents.

- a. Loss of site power.
- b. Loss of single coolant loop without leakage.
- c. Sodium leakage from a single loop.
- d. Guillotine pipe failure.
- e. Vessel leakage.

The first three accidents are expected situations in which the consequences can be readily limited by the built-in capabilities of the primary coolant system. The others represent catastrophic low probability situations. It is assumed that a reactor scram is initiated following any of the five accidents. Scram may be initiated by any of the following:

- a. Loss of main electric power.
- b. Flow deviation in any single primary loop.
- c. Low coolant level in the reactor vessel.
- d. High reactor fuel channel outlet temperature.
- e. High IHX outlet temperature.
- f. High reactor flux level, or short period.

At the instant of scram, power is removed from the pumps and flow coastdown is initiated.

3.2.1.1 System Design Features

Specific design features are built-in to the main heat transfer system to provide the necessary core protection in the event of a guillotine pipe failure. The major features are:

- a. Each primary and secondary loop provides an independent coolant path. This prevents propagation of the source of the accident to the other loops.
- b. The primary loops are elevated to enhance the natural convection core cooling capability. Core decay heat is removed without relying upon forced flow mechanisms.
- c. Two primary loops are available at all times to provide the emergency cooling function. Redundancy in core cooling capability provides an additional safeguard.
- d. A pump level control system is provided to maintain pump level in the event of loss of coolant level in the reactor. Each level control system is independent for each pump. Level control is obtained by adjusting the pressure in the gas space above the coolant free surface. The system is passive in that a loss of coolant in the reactor vessel to the minimum level will not lower the pump case level sufficiently to interrupt the coolant flow path. The level control system makes up for any small gas leakages from the pump case. A schematic of the pump level control is shown in Figure 3-1.
- e. Sodium volume above the minimum elevation is sufficient to fill the annular region between the safety

tank and core vessel, keeping the level in the reactor above the vessel outlet nozzles.

- f. Pump coastdown following a guillotine pipe failure in coolant accidents is limited so that as the coolant level drops to a point near the minimum level, no driving forces are present which could cause additional coolant loss.
- g. Pressure relief paths are required to prevent a rapid differential pressure transient between the reactor vessel and the heat transfer cell of sufficient magnitude to remove coolant from the core. This protection is provided by controlling the vessel and cell to near 1 atmosphere pressure. The vessel pressure is maintained through communication with the large volume fill/drain tank. Cell pressure is relieved with the aid of rupture disks (2—3 psig).

The following discussion is intended to focus upon the events occurring during either a guillotine pipe failure, or vessel rupture.

3.2.1.2 System Response to Leakage

Since the main loops are to provide the emergency cooling function following a leakage accident, there must be assurance of a reliable natural convection path through any one of the unaffected heat transfer coolant system loops. A presentation of the results of a preliminary evaluation follows. Consideration was given to a single failure of any 200 MWt heat transfer loop (see Figure 3-2).

a. Guillotine Pipe Failure at Reactor Outlet Nozzle (A)

Gross sodium leakage is detected by: (1) loss of liquid level in reactor

FIGURE 3-1. PUMP LEVEL CONTROL SYSTEM

FIGURE 3-2. FFTF MAIN PRIMARY HEAT TRANSFER SYSTEM

vessel. (2) flow deviation in any primary circuit, or (3) high fuel channel outlet temperature. Sensing will occur within a few seconds for loss of level, and will be essentially instantaneous for flow deviation. Scram and removal of power to all pumps would occur within four seconds.

System response is illustrated in Figure 3-3. The maximum outflow is limited by maximum flow through the reactor outlet nozzle under the prevailing pressure conditions. The curve represents the case with gravity drainage. Actual response would be slower because of the slightly higher pressure within the heat transfer cell. Actual pressure response is illustrated in Figure 3-6. Pressure will equalize through the hot leg nozzle. The coolant level is maintained above the minimum and decay heat is removed through the unaffected heat transfer loop(s).

b. Guillotine Pipe Failure in Pump Discharge Pipe (B)

Pipe failure in the pump discharge line will result in lowering the sodium level in the upper plenum because of the combination of gravity drain and pump coastdown pumping effect. Leakage is detected as above (Figure 3-4 illustrates this condition). Maximum system outflow is determined by the maximum flow at which pump cavitation occurs and represents a bounding condition. Likewise gravity drainage (~100 sec) represents the minimum outflow condition. The real flow coastdown curve would fall between the two limits as illustrated.

During normal reactor scram, a certain amount of pump flywheel inertia is required to match reactor power decay with system flow decay. The inertia required for normal scram will be built into the pump. A guillotine pipe failure at the pump discharge will alter the system characteristics so that a longer pump coastdown time will result because of the lower flow resistance of the system. This longer coastdown may result in an undesirable pumping action tending to lower the coolant level below the minimum elevation. If this is true, a mechanism to limit the coastdown may be required. At this stage of development, the detailed pump coastdown characteristic under these circumstances has not yet been determined and thus the necessity for a coastdown limiting device has not been established. Several practical means are available to provide a coastdown limiting feature, if required. Following scram, pump coastdown will occur in all loops. When the coolant level in the reactor drops significantly, say 6 ft, a resisting force would be applied to all pumps terminating the pump coastdown. This mechanism could be of the form:

1. Mechanical Brake
2. Electrical Signal to Coupling
3. Decouple Pump Motor from Shaft Removing Inertia Forces

Figure 3-4 illustrates that even under the most conservative assumptions, sufficient time is available to take appropriate action following a gross rupture.

UPPER POOL LEVEL DECAY

1. GRAVITY DRAINAGE THROUGH MAIN OUTLET NOZZLE
2. NEGLECTING GAS PRESSURE EFFECTS

FIGURE 3-3. PIPE BREAK AT REACTOR OUTLET NOZZLE

1. MAXIMUM OUTFLOW—IF PUMP CONTINUES OPERATION AT CAVITATION POINT
2. NEGLECT GAS PRESSURE EFFECTS

FIGURE 3-4. PIPE BREAK AT PUMP DISCHARGE

Core cooling is maintained through the natural circulation capability of the unaffected heat transfer loop(s).

c. Guillotine Pipe Failure in Reactor Inlet Pipe (C)

Pipe failure in the vessel inlet line past the check valve is similar in effect to the failure at the pump discharge, except that flow into the cell is from both ends of the pipe, and flow per loop is reduced due to increased piping resistance. Figure 3-5 illustrates the limiting coolant leakage considerations. The limiting system outflow is bounded by the pump cavitation parameter and the gravity drain situation. As in the failure at the pump discharge, a mechanism to limit pump coastdown may be necessary. This signal could be actuated by level loss and the pump coastdown terminated with sufficient margin to ensure core cooling capability is provided.

A careful evaluation of the core cooling mechanisms available during the leakage transient is necessary to determine if the core damage criteria is met.

d. Gross Vessel Rupture (D)

Coolant leakage following vessel rupture is detected in the same manner as described previously in (a). Level response would be determined by the size of the rupture, and maximum flow path is through the system piping. The expected response would be similar to Figure 3-5 for an inlet piping break. Coolant flows outward through the vessel rupture, into the safety tank surrounding the reactor vessel. An equilibrium coolant

level is reached between the vessel and safety tank which is above the minimum elevation. Core cooling is accomplished by natural convection through the unaffected heat transfer loop(s). Again, careful evaluation of the core cooling mechanisms must be made to determine if the core damage criteria is met.

3.2.1.3 Coolant Accident—System Pressure Control

During a coolant leakage transient, reactor coolant level is lost rather rapidly and dumped into the heat transfer cell. It is desirable to preclude a serious imbalance of pressure between the reactor vessel and the heat transfer cell in order to prevent a blowdown expulsion from the core or a gas bubble passing into the piping and upward through the core. In particular, it is desirable to keep a slightly higher pressure in the heat transfer cell, but less than the ΔP necessary to force a gas bubble down the inlet pipe to the elevation of the lower plenum. This amounts to limiting the pressure differential to about 6 psig for the backup design. During normal reactor operation, reactor cover gas pressure is controlled at near 1 atmosphere (0—1psig), by standard pressure control devices. The cover gas is allowed to communicate freely through a 12-inch line with a large volume tank of about 11,000 ft³ size. This volume provides a cushion of gas for normal operating temperature and level change cycles within the reactor vessel. It also supplies a source of gas during a rapid accidental loss of sodium.

Following a rapid loss of coolant with no makeup from the normal gas supply the cover gas pressure would drop within a minute, as shown in Figure 3-6. If the normal makeup gas were available (200 cfm), the pressure would then rise over the next few minutes as makeup gas replenishes the volume of coolant lost.

1. MAXIMUM OUTFLOW—TWO PUMPS CONTINUE OPERATION AT CAVITATION POINT.
2. NEGLECT GAS PRESSURE EFFECTS

FIGURE 3-5. PIPE BREAK AT REACTOR INLET

FIGURE 3-6. PRESSURE RESPONSE BETWEEN REACTOR VESSEL AND CELL FOR GUILLOTING PIPE FAILURE

Normal pressure in the nitrogen inerted heat transfer cells is controlled at a slightly positive pressure of 0—6 in. w.g. during reactor operation. Following a coolant system leakage accident, where sodium is dumped rapidly into the cell, a certain amount of thermal energy from the sodium will be rejected to the surroundings. Part of the heat will be transferred to the nitrogen gas causing the cell pressure to increase. Pressure increase may be limited by normal heat transfer mechanisms and by the use of pressure relief devices. It is planned to utilize rupture disks (2—3 psig) between the heat transfer cells and the refueling cell and other areas if required. Figure 3-6 illustrates the pressure rise in the heat transfer cell for a pool of sodium at 1000° F covering the entire floor of the cell (~1500 ft²). A rupture device set at 2.5 psig is assumed. Pressure is relieved into the refueling cell and through the hot leg piping. In a real situation, gas will pass quite readily through the hot leg piping equalizing any pressure difference between the vessel and cell atmospheres. The rupture device acts as added insurance to avoid any possibility of gas passage through the core region. Figure 3-6 illustrates that sufficient time is available to actuate the required relief devices to prevent possible gas passage through the core.

Pressure control is a feature which can be rather easily accommodated by the design, with no compromise of the normal operating functions.

3.2.2 Cost Effectiveness for Primary Coolant Loops

3.2.2.1 Discussion

A number of new considerations have evolved since the preliminary heat transport

system availability studies were completed in August. The major considerations are:

- a. Two independent sources of core cooling are required following any break in the coolant system boundary [Reference (4)]. This requirement is intended to satisfy the proposed AEC regulations 10 CFR 50, Criterion 44, relating to emergency core cooling systems.

The backup design utilizes the main coolant loop to satisfy the emergency cooling requirements. The minimum number of operable main coolant loops is three. If one loop is disabled, two remain, which will satisfy the emergency cooling criterion. It seems clear that if the main coolant system was not used for emergency cooling it would be necessary to provide two independent emergency cooling systems.

- b. The capital costs utilized for the backup design loop cost effectiveness studies and reported in Reference (2) were preliminary estimates. PNL has generated costs through the adopt code [Reference (5)].

PNL costs are being utilized in this reevaluation.

- c. In the August work, a comparison was made between redundant and nonredundant loop systems. It was assumed that most loop failures resulted in only a short reactor outage (~8 hours) while the faulty loop was isolated. Operation then continued until a normal refueling outage whereupon repairs were commenced. In the primary system

this scheme has the advantage that repair could be initiated and in some cases completed during a normal outage, thus taking advantage of a normal downtime. Also, primary loop Na-24 decay would occur during the interval between the failure and the normal refueling outage period. Likewise, primary system and IHX leakages were also assumed not to affect normal operation and credit was taken for some loop decay during operation before repair was initiated. In reevaluating the loop availabilities, it has been assumed that any sodium leakage in the primary system or heat exchanger would result in a plant shutdown for repair regardless of whether the system was redundant or not. This is a conservative, but realistic operating philosophy for the following reasons:

1. Primary leakage is difficult to locate and diagnose until the system has been drained and an entry made. Operation of the plant could not continue until the location and cause of leakage could be definitely determined.
2. Leakage of reactor cover gas through the loop defect would be an undesirable source of radioactivity and contamination in the loop heat transfer cells.
3. It would be difficult to continue operation from a safeguards standpoint, since the primary coolant pressure boundary would be breached, unless a positive mechanism for isolating the loop from the reactor system were present.

d. A comparison has been made between reported loop availability results by PNL [Reference (5)] and those utilized by G.E., Reference (2). The apparent difference for the nonredundant systems are commented on as follows:

1. Hallam data were utilized in the PNL results. The great majority of this data describe startup difficulties or problem areas which are not common to the FFTF System. The data were apparently not sifted to separate startup design problems from long-term operating failures caused by wear, cyclic fatigue, maintenance, etc. The Hallam data represent the initial 2-3 year startup period.
2. PNL utilized AI data from NAA-SR-MEMO-12091 for a second comparison of availability. The AI failure data, while quite complete, appear to be rather pessimistic particularly in the areas of expected system leakage and instrumentation malfunction. Recent advances in instrumentation reliability and use of redundancy in all critical areas should substantially reduce instrument unavailability. Thorough engineering, minimizing valves, improved inspection, quality control, and manufacturing techniques will result in very low system leakage failure rates as evidenced by current sodium reactor and sodium loop experience.

The G.E. failure data utilized in Reference (2) are based on on-line operation following the

initial checkout period. The G.E. loop availabilities compare favorably with Dresden I System following the initial startup difficulties.

Plant	Initial Plant Availability	On-Line Loop Availability
DRESDEN I	0.59	0.97
HALLAM [Reference (5)]	0.55	-
FFTF [Reference (6), AI]	-	0.76
FFTF [Reference (2), G.E.]	-	0.94

3. The availability comparison between redundant and non-redundant loops for the AI studies [Reference (6)] indicates that for all loop identified failures of a redundant system the plant could continue to run while loop decay and repair proceeded in the redundant loop. As discussed above this is probably an unrealistic assumption. Operation should be considered only as illustrated for the backup design

in the tabulation of Table 3-2. This assumption provides the basis for selecting a redundant loop system as in Reference (7).

3.2.2.2 Heat Transfer System Cost Effectiveness

The loop availability studies have been reviewed with the following considerations:

- Loop availabilities for redundant loops assume no credit for leakage in primary system. The tabulation in Figure 3-1 lists those items whose downtime is decreased by addition of redundant loop.
- Capital costs are extracted from PNL data in Reference (7).
- Cost of unavailability used is \$1700/h. Higher interest for capital, say 8% instead of 5%, would increase cost to \$1900/h. This effect is shown in Figure 3-7.

3.2.2.3 Results

Using the considerations listed above, the tabular results are as follows:

System	U	Cost of U at \$1700/h	Incremental Capital Cost and Maintenance Cost Figures 8 and 17 Reference (7)	Total + \$
2/200	0.056	10.4×10^6	20.1×10^6	31×10^6
3/133	0.078	14.5×10^6	20.8×10^6	35×10^6
3/200	0.027	5.0×10^6	34.1×10^6	39×10^6
4/133	0.036	6.8×10^6	31.0×10^6	38×10^6
4/100	0.101	18.8×10^6	22.8×10^6	42×10^6
5/100	0.045	8.4×10^6	31.0×10^6	39×10^6

TABLE 3-2

COMPONENTS WHOSE DOWN TIME IS DECREASED BY THIRD LOOP*

ITEM NO.	COMPONENT AND FAILURE MODE	λ	T_{REP}	μ_2	μ_{3A}	U_2	U_{3A}	λ = failure rate, per 10 hours
1.4.2	Check valve leaks from body	0.3	260	600	600	200	200	T_{REP} = repair time, hours
1.5.1	Primary pump, motor failure	1.2	340	340	8	400	10	μ = mean down time, hours
1.5.7	Primary pump, shaft binds	1.1	340	680	80	750	100	U = unavailability $\times 10^6$
1.5.8	Primary pump, loss of fluid to hydro. bearing	1.4	340	680	80	950	110	sub 2 = 2 loops (200 MWt)
1.5.9	Primary pump, hydrostatic bearing seizes	3.3	340	680	80	2200	250	
1.5.10-13	Primary pump, controller failures	16.6	16	16	8	260	130	sub 3A = 3 loops
1.5.15	Primary pump, operator error	8.0	72	72	72	600	600	
2.1.1	Secondary piping, leaks	1.4	200	200	8	300	10	
2.1.2	Secondary piping, guillotine failure	0.014	800	800	8	10	0.1	
2.4	Secondary bellows valves, all failures	42	170	170	8	7200	350	
2.5	Secondary pump, all failures with $\mu > 0$	37.7	---	100	8	3700	300	
4.	Radiator, all failures	30.0	---	80	8	2400	240	
5.1.1	Primary sodium service inlet or outlet valves leak	0.3	340	680	80	200	25	
5.3.2	Primary fill valve, leaks	0.3	340	680	80	200	25	
5.3	Primary fill valve, all other failures	4.6	340	340	8	1500	40	
6.1	Primary loop control	---	380	380	8	50	1	
6.2.1, 3, 5	Primary protective instruments fail	---	380	380	120	90	30	
6.5.1, 3, 4	Secondary protective instruments fail	---	72	72	8	50	5	
6.6.1	Secondary pressure transducer leaks external	0.5	170	170	8	80	4	
						21,140	2430	
						- 2,430		
						<u>18,710</u>		

*See Appendix, Reference (2), for complete item listing.

FIGURE 3-7. FFTF HEAT TRANSFER SYSTEM COST EFFECTIVENESS

The 2/200 MWt loop system must be eliminated based upon the emergency cooling requirement [Reference (4)]. Figure 3-7 illustrates the net cost of redundant and non-redundant loop systems over the two-loop system. The following conclusions are evident:

- a. The 3/200 MWt loop system is eliminated from further consideration since a minimum of three loops must be in operation, and the redundancy cannot be utilized completely.
- b. The 3/133 MWt loops are the best choice for the FFTF backup design based upon the cost effectiveness considerations presented in Figure 3-7.
- c. This "cost" of uncertainty is illustrated in Figure 3-7 and amounts to about $\pm \$2 \times 10^6$ [see Reference (2)]. In the same manner there is a range of uncertainty associated with capital costs. This was not estimated, but it could be of the same order

of magnitude as availability uncertainties at this stage of development.

- d. The 3/133 MWt, or 4/133 MWt loops appear to be nearly equal choices on a cost effectiveness basis, considering the uncertainties. Again, the curve is flat in this region and the selection of one system over another will not drastically affect the ability to meet the program objectives.

3.2.3 Piping Flexibility Analysis

Preliminary piping flexibility analysis was performed using piping layout drawing 153F674. The analysis was performed to determine if reasonable conceptual design layouts were being established. Expansion stresses and pressure stresses were evaluated and determined to be within the allowable stress range of the ASA Code for pressure piping ASA B31.1. Piping reaction loads on the adjoining vessels were evaluated. Stresses were found to be within the allowable stress range of Section III of the ASME nuclear pressure vessel code. Piping expansion stresses are tabulated as follows:

Pipe Run	Design Temperature (°F)	Design Pressure	Pipe Size (in.)	Schedule	Material Stainless Steel	Allowable Expansion Stress SA (psi)	Maximum Calculated Stress Piping (psi)
1. Reactor—Pump	1200	50	24	10	304	24,550	10,546
2. Pump—IHX	1200	200	20	20	316	25,140	25,070
3. IHX—Reactor	900	200	20	10	304	25,800	25,240

In addition, the required piping wall thicknesses were compared for the primary

pressure loadings and found to be acceptable. Results indicated the following:

Pipe Run	Design Temperature (°F)	Material, Stainless Steel	Allowable S_m (psi)	Calculated Required Wall Thickness (in.)	Actual Wall Thickness (in.)
Reactor—Pump	1200	304	4500	0.133	0.25
Pump—IHX	1200	316	6800	0.280	0.375
IHX—Reactor	900	304	9400	0.211	0.25

The component reaction forces which resulted from the piping expansion were applied to the components and vessels utilizing relationships developed by

P. P. Bijlaard.⁽⁸⁾ The combined peak stresses were found to be within the allowable stress range, as shown in the following tabulation:

Location	Material, Stainless Steel	Temperature (°F)	Vessel Maximum Stress Caused by Piping Loads		Allowable Stress Range $3S_m$
			σ_x (psi)	σ_y (psi)	
Reactor Outlet	304	1200	3,730	5,000	13,500
Pump Inlet	304	1200	---	160	13,500
Pump Outlet	316	1200	---	6,510	20,400
IHX Inlet	316	1200	11,720	13,900	20,400
IHX Outlet	304	900	1,360	1,970	28,200
Reactor Inlet	304	900	10,840	11,180	28,200

The maximum allowable stress intensity includes a combination of primary and secondary stresses from various sources. These include stresses from internal pressure, earthquake loadings, thermal expansion, and dead weight. Many of these loadings cannot be evaluated at this time. The flexibility analysis was performed to indicate feasibility of the conceptual design, and is not intended to be a detailed structural evaluation. There appears to be sufficient margin in the computed reaction loadings to be relatively confident of the design feasibility.

3.3 AUXILIARY SYSTEMS

3.3.1 Plant Control

Preliminary plant control philosophy has been developed to be utilized in connection with the system transient study. Output from the study will be utilized to define the plant control functional specification and corresponding interface with the reference design. For operation at power, it is expected that the pump speed will be controlled to provide the desired reactor outlet sodium temperature and that the air

flow through the heat sink cooler for secondary sodium will be controlled to provide a constant secondary sodium cold leg temperature and, in turn, a constant reactor inlet temperature. Further study will be given to the control method, but at present, it is expected that the desire to control temperature at near full irradiation power will be a predominate factor in selecting the above control method.

For plant shutdown, there are two probable sequences: (1) a normal shutdown in which the power is run down to that corresponding to the minimum pump speed and then both the reactor and all main pumps are shut down, or (2) a reactor scram in which both the reactor and all pumps are shut down from full power. The transition from power operation to decay cooling must be accomplished in both a normal and scram shutdown. Thermal transient conditions are expected to be determined from further studies. During a transient it is probable that reactor inlet temperature would remain unchanged because of the thermal inertia and delay in the system.

The means for providing flow in the main loop for decay heat removal is not established; both natural convection and low speed pony motors on the main pumps are being considered as possibilities. The expected changes of power, temperatures, and flow are shown in Figure 3-8 for a normal plant shutdown.

After the transition to decay cooling has been accomplished, temperatures can be controlled by the air heat sink with a target cooling rate of less than 50° F/h limited by the coolant piping and structures.

For reactor refueling, the reactor outlet temperature must be reduced to 500°–400° F and the plug removed exposing this sodium and its vapor to the refueling

cell atmosphere. During this period the coolant flow will be low and might be provided either by a pony motor or by natural convection. It is not probable that it would be provided by lowering speed on the main pump motor unless (1) the main motor had special speed control equipment, or (2) the refueling operation could tolerate about 1/6 full sodium flow.

For reactor power startup the sodium temperature can be raised to normal reactor inlet temperature: (1) by low power reactor operation, (2) by electrically heating the air coolers and the sodium piping, or (3) by operation of the main pumps to introduce friction heating.

Figure 3-8 shows how the system temperature drops during the shutdown. It is assumed that heat is provided by pipe heaters during this period.

The pumps are to be brought to minimum speed during or before reactor startup. Figure 3-8 shows the conditions for the pump being started first. The transition from minimum pump speed is accomplished in the reverse control sequence of a normal plant shutdown, except that the decay cooling transient does not occur.

The following accidents are to be considered in evaluating the plant control system:

- a. Loss of site power. This would result in a reactor scram and a stored energy type of power would be used as needed.
- b. One disabled loop. The consequence of a decrease in pump discharge pressure for any reason is closure of the check valve, thus removing the loop from the circuit. The guillotine pipe failure has the same characteristic with the added feature

FIGURE 3-8. CONDITIONS DURING SHUTDOWN REFUELING AND STARTUP

that sodium level is lost in the reactor vessel. After the pump coast down, the check valve will swing open permitting decay heat removal by that loop.

- c. Redundant loop startup. The existence of a redundant loop makes this accident possible and its probability is dependent on the interlocks provided. Check valve back flow cannot be completely eliminated and thus the loop will be filled and its temperature will be less than the reactor inlet temperature. Its accidental startup would result in a cold slug at the reactor inlet. Its temperature would depend on the check valve back flow and loop insulation, both of which can be specified.
- d. Loss of secondary loop. If the secondary loop were to fail and if the controls failed to stop the flow of corresponding primary coolant flow, then a hot sodium transient would occur at the reactor inlet.
- e. Power failure during a collapsed ΔT . From shutdown to subsequent startup, there is the possibility that a main pump will be operated without reactor power (perhaps essential for plant startup) and during this period the hot leg and cold leg will be essentially at the same temperature. If the site power were to fail at this time, decay heat would have to be removed. The adequacy of natural convection during this circumstance must be evaluated.

3.3.2 Argon System

Preliminary design work is progressing on the refueling cell purification system and cell cooling system design requirements.

3.3.2.1 Refueling Cell Purification Loop

The preliminary design requires a recirculating argon purification system capable of maintaining the refueling cell argon atmosphere at less than 10 ppm oxygen, 10 ppm moisture and acceptable sodium vapor concentrations.

Evaluation of purification systems is under way with consideration being given to both the NaK bubbler demister system and the chemical catalyzer and dryer system.

Preliminary calculations of the required purification flow rate were performed based on air in-leakage rate of 0.04 volume percent per day. This rate is approximately double the rate used by the EBR-II argon cell calculations. Results indicate a flow rate of 600 SCFM will be required.

3.3.2.2 Argon Recirculating System for Refueling Cell

Plant layout considerations have dictated the Argon Recirculating System be composed of the argon cooling system and the cell pressure control system.

3.3.2.3 Refueling Cell Cooling System

This system is comprised of three cooling loops. Each loop will contain a blower, cooler, and associated ducting and be sized to control the refueling cell temperature at less than 150°F by removing one-third of the heat generated by the cell.

Preliminary calculations indicate that each loop must be sized to remove ~180,000 Btu/h at a flow rate of ~5,500 SCFM.

3.3.2.4 Cell Pressure Control System

The Pressure Control System consists of an argon reservoir tank, argon compressor, and the necessary control instrumentation to maintain cell pressure control and safety relief. The system is designed to maintain cell pressure within allowable ranges under normal cell temperature fluctuations of $\pm 10^\circ\text{F}$ and cell temperature transients of up to 40°F . Work is continuing on sizing the system components.

3.3.2.5 Interface With PNL Design

Work will be completed on designing the Refueling Cell Purification System before the interface conditions with the PNL design can be clearly defined.

3.3.3 Containment

To select the containment building shape most suitable for the FFTF backup reactor

system, several shapes and fuel transfer lock concepts have been investigated. Two basic shapes were considered along with three transfer lock concepts (Table 3-3). A preliminary structural evaluation was performed and costs of the containment were then determined on the basis of the structural requirements. The containment building internal pressure was very roughly estimated by assuming that 33% of the 1500 MW-sec DBA energy is transferred into the building atmosphere. A design pressure of 10 psig was selected for evaluation purposes. Structural evaluation indicated the shell thickness would be determined by external loads rather than the 10 psig internal pressure. This results in a heavier shell for Arrangement No. 3 (Table 3-3). To establish the building costs, the steel shell, containment lock structure, excavation and concrete foundation were considered. The results indicate that Arrangement No. 4 is favored. The cylinder contains more usable space than the sphere, and the transfer lock is located near the periphery of the building.

TABLE 3-3

SHAPES AND FUEL TRANSFER LOCK CONCEPT

Arrangement	Shape	Dimension	Transfer Lock	Relative Cost \$ $\times 10^{-6}$
1	Cylinder	130 Foot Diameter $\times$ 220 Foot Height	Side	4.8
2	Cylinder with Discontinuity for Lock	120 Foot Diameter Above Grade 140 Foot Diameter Below Grade	Side	5.0
3	Sphere	180 Foot Diameter	Bottom	4.3
4	Cylinder	130 Foot Diameter $\times$ 220 Foot Height	Bottom	3.9
5	Cylinder	Same as No. 4, More Excavation	Bottom	4.3

There would be a definite advantage if the building internal design pressure were the determining structural load. In this case the spherical building would be the most economical design.

For the present, a cylindrical containment shape will be utilized with a vertical lock passing through the bottom of the dished head.

3.4 PNL-G.E. SYSTEMS INTERFACE

A meeting between PNL and G.E. was held at PNL on October 23—24, 1967. As a result, evaluation of coolant system accident considerations has been initiated. Initial results are reported in subsection 3.2.3.

A review was made of a draft of the PNL Heat Transport System Design Description and comments were transmitted to PNL. (9)

SECTION IV

REACTOR VESSEL AND INTERNALS

4.1 GENERAL

Continuing design effort has been directed toward providing additional testing flexibility and improved driver fuel instrumentation without sacrificing desirable nuclear characteristics. Recent nuclear analyses, reported elsewhere in this report, confirm the desirability of the compact vertical core design. Calculations show that sufficient nuclear control is available in the reflector, so that only three safety rods are required in the core. Reflector control frees test volume in the core and simplifies refueling activities. See Figures 4-1 and 4-2 for elevation and plan sections of the FFTF backup design.

Utilization of more than one driver fuel space per closed test loop assembly has been considered. Minimal decrease in neutron flux accompanies the use of a closed loop occupying several adjacent core positions. Figure 4-3 shows the comparative results of the space available and peak neutron flux in the reference (one position) and multiposition test assemblies. In the first four configurations, it was assumed that there was a total of six closed loops in the core and that the multiposition test assembly was located at the center of the core.

Multiposition test assemblies can be operated as open or closed loops. As in any core containing test assemblies, appropriate nuclear and heat removal capabilities have been provided. The nuclear analysis of the largest (seven position) assembly was calculated for a core containing twelve test loop positions (two positions at each of the six reference closed loop locations). Thus there would be a

seven-position assembly in the center, two double positions on the periphery, and a single closed loop in the midradius position. Fifteen percent fuel was utilized in the nuclear calculations at each test position occupied.

Reactor and driver fuel instrumentation developments are described in the following sections.

4.2 SUPPORT SKIRT, VESSEL, AND TOP PLUGS

During this reporting period the vessel support skirt, vessel, and top plug configurations were reviewed. Figure 4-1 shows the updated conceptual design of these areas. The changes can be noted when compared to Figure 4-2 of the first quarterly report [Reference (1)].

The vessel support skirt has been simplified by relocating the safeguard tie-down bolt anchors and 16 penetrations for the instrument and closed loop coolant leads. The skirt is supported by a circular steel spoked-box type support which is partially buried in the concrete shielding structure and tied into the concrete rebar structure. The normal load on the skirt is about 800,000 pounds from the vessel and 200,000 pounds from the head. The stretch bolts are assumed to add a negligible load.

The vessel flange and shell are the same as described in the last report but the instrumentation and closed loop piping nozzles have been lowered to permit increased flexibility in the closed loop piping and to pass between the spokes of the support ring rather than through the skirt. The present location minimizes

FIGURE 4-2. PLAN VIEW OF REACTOR

CHANNEL CONFIGURATION	EQUIVALENT TEST HOLE DIAMETER (MAXIMUM FUEL DIAMETER) (inches)	PEAK FLUX (10^{16} n/cm ² sec)
3.50 (2.25) (SIX SINGLE POSITION CLOSED LOOPS)	1.02
5.00 (2.75) (THREE DOUBLE POSITION CLOSED LOOPS)	1.01
5.00 (2.75) (SIX DOUBLE POSITION CLOSED LOOPS)	0.99
6.25 (3.00) (ONE TRIPLE AND THREE SINGLE POSITION CLOSED LOOPS)	0.99
7.00 (4.25) (ONE QUADRUPLE AND TWO SINGLE POSITION CLOSED LOOPS)	0.97
9.50 (6.00) (ONE 7 POSITION, TWO DOUBLE-POSITION AND ONE SINGLE POSITION CLOSED LOOPS)	0.92

FIGURE 4-3. CLOSED LOOP ARRANGEMENTS

physical and thermal interaction with the support structures.

The overlapping flange of the shield plug has been increased in diameter. DBA* stretch bolts have been lengthened and anchored in the support ring structure. This feature is intended to allow the bolts to stretch during the DBA pressure excursion which will vent some cover gas and/or sodium to the refueling cell. Some distortion with possible leakage of the 3/4-inch-thick reactor vessel, probably between the blast shield and the top plug regions, is anticipated during the DBA accident. This vessel expansion will tend to minimize the amount of sodium and damage products released to the refueling cell.

The shield plugs, shown on Figure 4-1, indicate a strong steel structure in the upper third of the plug height. Temperature will be maintained as uniform as practical in this region and below the freezing point of sodium (208° F). The lower two-thirds of the plugs contain many layers of steel shielding (grand total of approximately 2 feet of steel through the plug). This structure also incorporates thermal insulation.

As described in the previous report, the refueling plug not only provides rapid access to the core region for test rig insertion and removal, but supports the integral backup (to the hydraulic) holddown system and driver fuel instrumentation rack, see Figure 4-4. The holddown function is provided by a plate made up of a network of steel bars so arranged that channel outlet sodium can easily get to the sodium plenum surrounding the holddown while the fuel subassemblies cannot lift more than about an inch. If they should lift the flow restrictors on the lower end of the fuel subassembly will

still be engaged with the high pressure plenum plates.

A segmented interference ring at the top of the refueling plug provides a positive means of attaching it to the shield plug. This is required to satisfy holddown and DBA requirements. The interference ring is expanded into a groove on the large plug during operation and is retracted into the refueling plug when it is to be removed.

The instrument support structure has a separate instrument package for each fuel assembly. The instrumentation package is contained in a support tube to protect the leads from flow induced vibrations. The support tubes extend into the upper end of the fuel assemblies when the support structure is in operating position.

The instrument package for each driver fuel assembly will consist of an assembly of four thermocouples. The selection of this number of sensitive elements and associated majority logic circuitry to eliminate false scrams or no warning of under cooling was presented in the last quarterly, Reference (1) as scheme "D." Additional instrumentation can be added to the instrument package, such as flow meters or fuel element leak locating devices as soon as reliable devices are developed for this application. The fuel subassembly handle has been increased in diameter so all of the flow in the assembly flows past the instrument package. The inside diameter of the handle is about 3 inches. The thermocouple assembly is estimated to be about 1/2 inch in diameter.

The primary system inlet nozzles are located below the core and are equally spaced around the periphery. The nozzles will be of the thermal sleeve type to reduce the stresses in the vessel-nozzle area. Similarly the outlet nozzles are equally spaced and are equipped

*DBA—Design Basis Accident

FIGURE 4-4. REFUELING PLUG AND INSTRUMENT RACK

with thermal sleeves to minimize the mechanical and thermal stresses during power excursions.

4.3 CORE SUPPORT AND CLAMPING

4.3.1 High Pressure Plenum

Alternate configurations for the high pressure plenum and integral core support plate are being considered. These alternates include a concept using a relatively small diameter plenum with the primary system inlet pipes extending like spokes to the vessel nozzles. This concept requires some flexibility between the plenum and nozzle which is supplied by the thermal shield type nozzle and the flexing of the relatively thin side walls of the high pressure plenum. An alternate method has the lower part of the vessel integral with the inlet plenum. This last method eliminates the need for expansion joints and adds volume to the inlet plenum which can be used to improve the flow distribution to the core but requires a low pressure tank below the lower hold-down plate to relieve the leakage sodium. This low pressure tank must be vented.

Work has begun in the hydraulic design of the inlet plenum to ensure proper flow distribution to all fuel elements. Initial calculations show that the radial pressure drop through the inlet plenum to the central fuel elements will not be a serious problem. For the case where the inlet plenum is 10 inches deep and at rated flow, the radial pressure drop is less than 2% of the core pressure drop (100 psi).

4.3.2 Core Clamping

The core clamping device must hold the core firm during all modes of reactor operation and be able to be released readily

during refueling operations. The schemes currently being considered are:

- a. Thermal expansion of the fuel channels. This method requires an outlet ΔT before the clamping takes effect. Under this ΔT condition the thermal growth at the top of the channels will be greater than the thermal growth in the support plate. Differential thermal growths eliminate the clearance between channels at the top of the core. A rigid ring surrounding the core would be used to provide radial support. This rigid ring must be operated at inlet temperature or be fabricated from a material with less thermal expansion than Incoloy 800.
- b. Expanding mechanisms on each driver fuel assembly. These mechanisms expand bearing pads into the clearance space between fuel assemblies. Each pad expands to slightly greater than half the nominal clearance between channels. This bearing pad motion allows removal and replacement of any fuel assembly by actuating the mechanism on the rod to be removed. A rigid ring surrounding the core for the radial support is required as in the above case.
- c. Contracting and expanding radial support rings around the core. The ring in the contracted position would hold the core firm while in the expanded position the core would be loose for refueling operations. Multisegment and multioperator rings are being considered. Methods being considered for actuating this

ring include differential thermal expansion, hydraulic pressure and mechanical torsion bar motion. The differential thermal expansion method would automatically tighten the core when the temperature is increased and loosen it at refueling temperatures. Pressurized sodium from external pumps would be used in the hydraulic pressure case. The mechanical and hydraulic methods provide a means for tightening the gaps between the subassemblies before going critical or increasing the core ΔT . All methods must accommodate the temperature patterns in the core and reflector and changes in power level.

4.4 REACTOR COMPONENTS

4.4.1 Design Features of the Core Parameter Studies

Except for the cylindrical sections near the driver fuel assembly (fuel channel) lifting groove and immediately above the support plate level, the (fuel channel) assembly has not been changed significantly from that reported in the last quarterly [Reference (1)—see Figure 4-5].

Two nuclear parameter studies were performed and are reported in the physics section (Section VII). These studies were performed with 400 MWt reactor power, 100 psi pressure drop across the fuel assembly, and a criticality (for control purposes) of 1.01. The first study which investigated pin diameter, core height and cladding thickness parameters indicated a slight choice for a 33-inch-high core, 0.210-inch pin diameter, 42-inch-diameter core, and for practical reasons 0.015-inch cladding thickness. The second study explored the effect of substituting Type 304 Stainless Steel channel material for the reference Incoloy 800 (with

proper adjustments for different thickness requirements). Table 4-1 presents the design parameters associated with the pin diameter and channel material studies.

4.4.2 Configuration of the Closed Loop Reactor Tubes

A closed loop is an irradiation test facility designed with a reactor test section and an out-of-vessel portion to provide the test with an isolated independent coolant system. The separate cooling system permits control of the loop coolant flow, temperature, pressure and chemistry independent of the reactor coolant system and allows testing to near failure without fear of contamination of the reactor system. Following is a description of a closed loop tube design compatible with the open lattice vertical core concept of the FFTF backup reactor design.

The experimental portion of the closed loop consists essentially of a fuel test section and its hanger rod with a total length of approximately 35 feet. The irradiation test section is inserted into the closed loop tube in the reactor core region. The loop coolant flow is down through the outer annulus of the closed loop tube then up through the experiment. The experiment hanger rod will seat in the loop tube between the inlet and the outlet connection to complete the coolant circuit in the loop tube. To comply with the criterion that the loop tests be capable of undergoing interim examination this test section is designed for removal and replacement without damage to the test section or to its instrumentation. The experiment hanger rod will be provided with the required shielding above the reactor core and in the top shield region.

The following closed loop description, based on a 3.405-inch lattice in the core and covering only the in-vessel portion of the

FIGURE 4-5. DRIVER FUEL ASSEMBLY

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TABLE 4-1

FUEL ELEMENT DESIGN PARAMETERS

Core Subassembly	Parameter	Original Design 0.210 Outside Diameter Pin Incoloy Channel	Case I 0.21 Outside Diameter Pin Incoloy Channel	Case II 0.25 Outside Diameter Pin Incoloy Channel	Case III 0.21 Outside Diameter Pin Type 304 Stainless Steel Channel	Case IV 0.25 Outside Diameter Pin Type 304 Stainless Steel Channel
Fuel Bundle	<u>Pins/Channel</u>	<u>127</u>	<u>127</u>	<u>127</u>	<u>127</u>	<u>127</u>
	Pin Diameter (in.)	0.210	0.210	0.250	0.210	0.250
	Pin Pitch (in.)	0.272	0.266	0.294	0.266	0.294
	Wire Wrap Diameter (in.)	0.062	0.056	0.044	0.056	0.044
	Pitch/Diameter Ratio	1.29	1.26	1.175	1.26	1.175
	Active Fuel Length (in.)	33	33	33	33	33
	Cladding Thickness (in.)	0.010	0.015	0.015	0.015	0.015
	Cladding-to-Fuel Diametral Gap (in.)	0.004	0.004	0.004	0.004	0.004
	O/M Ratio	1.97—1.98	1.97—1.98	1.97—1.98	1.97—1.98	1.97—1.98
	Fission Gas Release (cc gas/cc fuel at 100,000 MWd/T)	28.9	28.9	28.9	28.9	28.9
	Pellet Density (% TD)	95	95	95	95	95
	Smeared Density (% TD)	90 ±2	90 ±2	90 ±2	90 ±2	90 ±2
	Cladding-to-Fuel Bond	He	He	He	He	He
	Gap Conductance (Btu/h-ft ²)	1500	1500	1500	1500	1500

TABLE 4-1 (Continued)

Core Subassembly	Parameter	Original Design 0.21 Outside Diameter Pin Incoloy Channel	Case I 0.21 Outside Diameter Pin Incoloy Channel	Case II 0.25 Outside Diameter Pin Incoloy Channel	Case III 0.21 Outside Diameter Pin Type 304 Stainless Steel Channel	Case IV 0.25 Outside Diameter Pin Type 304 Stainless Steel Channel
Fuel Bundle (Continued)	<u>Pins/Channel</u>	<u>127</u>	<u>127</u>	<u>127</u>	<u>127</u>	<u>127</u>
	Cladding Hydrostatic Strain, Maximum (%)	9450	9450	9450	9450	9450
	Cladding Strain, Maximum (%)	2	2	2	2	2
	Gas Plenum Volume (cc)	16.5	10.2	18.4	10.2	18.4
	Gas Plenum Length Including Spring (in.)	36	26.1	30.9	26.1	30.9
	Fuel Swelling (% at Rated B. U.)	5.5	5.5	5.5	5.5	5.5
	Fission Gas Release (%)	100	100	100	100	100
	Average Exposure First Core (MWd/T)	70,000	70,000	70,000	70,000	70,000
Channel	Outside Flat-to-Flat (in.)	3.375	3.601	3.598	3.357	3.688
	Inside Flat-to-Flat (in.)	3.175	3.179	3.398	3.087	3.398
	Wall Thickness (in.)	0.100	0.092	0.100	0.135	0.145
	Pitch Center-to-Center at 70° F (in.)	3.405	3.391	3.628	3.387	3.718
	Plenum/Plenum Pressure Drop (psi)	80 to 112	100	100	100	100

loop, is submitted to show the loop envelope and composition of the neighboring driver fuel. A conceptual design of the in-vessel section of the test loop is shown on Figure 4-6. The overall length of this closed loop is approximately 41 feet 10 inches, with accommodations for a 19-pin experiment and a cooling capacity of about 2 MWt.

The reactor tube, approximately 37 feet 6 inches long, is supported on the grid plate and extends up through the refueling plug. The test loop coolant inlet and outlet connections are made just below the top shield and are routed out of the reactor vessel through one of the service nozzles. The bottom section of the reactor tube corresponds to the nose section on the driver fuel elements. As the loop is provided with its own cooling system, no holes are provided in the nose section of the loop. If permanent closed loop locations were chosen, the hydraulic holddown plates could be plugged and the reactor tube shortened. For reactor arrangement

flexibility the present concept seems preferable. To hold the loop in position, and allow for differential expansion between the loop and the reactor vessel, a spring is incorporated into the loop holddown in the refueling plug.

The reactor tube outer envelope will be double walled from the coolant inlet to the experiment position and single walled from there to the lower end. The double wall tube is used to reduce the heat transfer to the loop inlet coolant as it traverses the high temperature region of the reactor sodium pool. The annulus between the double wall may be evacuated to increase the resistance to heat flow. The inner wall of the loop tube includes a bellows section to allow for the differential expansion. The reactor tube outer envelope is single walled through the reactor core area as a safety measure. In case of failure of the loop coolant system the reactor coolant will be able to remove residual heat from the loop's experimental section.

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SECTION V

REFUELING CELL AND FUEL HANDLING

5.1 REFUELING CELL

5.1.1 The refueling cell and fuel handling system, illustrated in Figure 5-1, is based upon an open reactor, hot cell concept similar to that used in the SEFOR reactor. The refueling system may be described as visual, remote-manual since refueling operations are performed remotely by operators viewing the fuel handling equipment and the upper region of the reactor. All component transfers and servicing which involve exposure of radioactive materials will be carried out remotely utilizing visual confirmation of events to as great a degree as possible. Shielding, applied over the reactor vessel, fuel storage tanks, and primary coolant lines make direct access to the refueling cell possible to service equipment located therein.

5.1.2 Telescoping Tube Hoist Mounted Handling and Servicing Equipment

A visit was made to Programmed and Remote Systems Corporation (PaR) to discuss refueling cell applications of telescoping tube hoist mounted handling and servicing equipment. The discussions were primarily centered around telescoping tube hoist features such as configuration, positioning accuracy, load indication, protective measures, and cell coverage. PaR has fabricated tube hoists with vertical travel capability comparable to that required for the FFTF backup design (30 + feet). Furthermore, they have attained greater positioning accuracy than that required for this application ($\pm 1/16$ inch as compared to ± 1 inch).

The typical tube hoist application, where headroom is limited, utilizes a number of short telescoping tubes. PaR pointed out that use of fewer and longer tubes, both of which may be possible in the refueling cell application, would result in a more accurate, rugged, and economical configuration. Their approach toward maintaining tube-end position accuracy, and a near zero clearance guidance system over the full vertical travel would be to accurately mount and align tracks (shim and dowel) on commercial tolerance tubing. The inherent flexibility of the extended tubes would enable "lead-in" engagement of the fuel element handle (up to ~ 1 inch required) without imposing significant lateral forces. There was a four-tube assembly being fabricated at the time of the visit. No "play" could be felt in this assembly either collapsed or extended even though the guide roller spacing for each tube was only 6 to 7 inches.

Direct load readout as well as safety interlocks are beneficial if not essential for the vertical motion. This is relatively easy to provide. An equivalent system in the lateral and rotational motions would be more difficult. A logical alternate for these motions would be electrical "slip clutches" in the drive train which can be remotely adjusted for the desired force. This is reasonable since the prime objective is equipment protection as compared to transient quantitative operational information.

The concept of remotely interchanging tube hoist assemblies on bridge structures was discussed. This concept is being considered since it has the potential of (1) resulting in less congestion in the cell

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FIGURE 5-1. REFUELING CELL AND FUEL HANDLING SYSTEM

(fewer major assemblies), (2) giving better cell coverage and thus greater versatility (enable operating the equipment at different elevations or close to any wall without interference with another bridge mounted device), and (3) greater plant availability (a replacement or backup device can be easily brought into operation). The bridge crane mounted high in the cell provides the handling capability for moving these tube hoist assemblies. PaR's suggestion regarding the electrical leads was to have the connection made at the bridge to hoist assembly interface. Electrical cables to the carriage and bridge structures could utilize either a "festooning system" or "cable play out" into a cable tray. Wall mounted take-up reels are not suitable for the refueling cell size.

The possibility of mounting the grapple drive motors at the top of the hoist was discussed for those cases where grappling must be performed below the sodium surface. This would require transmitting the drive motions by some extensible means such as telescoping shafts. Two major advantages of this arrangement are that the drive motors are more accessible and the problems associated with reaching many feet below the sodium level are minimized.

5.2 CLOSED TEST LOOP: HEAT TRANSFER DURING HANDLING

Calculations were performed to demonstrate under what conditions it might be proper to handle test loops by direct removal from the core (i.e., without using transfer thimble).

A reference case has been analyzed based upon a 19-pin bundle with a typical spacing configuration and a decay heat load of 5 kW. This is equivalent to approximately 150% of current FFTF driver fuel.

The analysis indicates that removing such an assembly directly from the core

without a transfer thimble does not result in excessive sodium or fuel temperatures.

The following assumptions were made in the thermal hydraulic analysis:

a. Test Fuel Bundle

1. 19 pins, $P/D = 1.35$, pin outside diameter = 0.21 inch
2. $\frac{\text{Power density of test fuel}}{\text{Power density of FFTF fuel}} = 1.5$

3. Decay power after two days

$$Q = \frac{19}{127} (19 \text{ kW}) (1.5) = 4.27 \text{ kW}$$

where

19 kW = decay power of a 127 pin FFTF fuel bundle after two days

$$Q_{\text{decay}} = \frac{5 \text{ kW}}{\text{--- calculations ---}} \text{ (value used in calculations)}$$

b. Closed Loop Capsule

1. Natural circulation height—26 feet

c. Heat Transfer Coefficients

1. $h = 1 \text{ Btu/h-ft}^2 \cdot ^\circ\text{F}$ stainless steel to gas atmosphere
2. $\epsilon = 0.1$, emissivity of stainless steel.

The results of the analysis are shown in Table 5-1. The maximum sodium temperature in the fuel region is 1067° F and the maximum sodium temperature next to the outer wall is 1039° F. The natural circulation flow rate is about 400 lb/h. The pressure loss across the test fuel bundle represents most of the total pressure loss. There is an optimum pin spacing to get the minimum total pressure loss and the maximum natural circulation flow. Maximizing the natural circulation

TABLE 5-1

SODIUM TEMPERATURE DISTRIBUTION
IN A CLOSED LOOP CAPSULE

<u>Height (ft)</u>	<u>° F</u>	<u>° F</u>
26	446	446
25	447	447
24	448	446
23	452	447
22	459	451
21	468	458
20	480	467
19	495	479
18	513	494
17	534	512
16	558	533
15	586	557
14	617	584
13	656	616
12	692	651
11	737	691
10	786	735
9	842	784
8	903	840
7	972	901
6	1050	970
fuel 5	1067	1023
4	1047	1039
3	992	1019
2	968	986
1	954	963
0	950	

Note: The decay heat rate is 5 kW and the natural circulation flow rate is 400 lb/h. The height of natural circulation loop is 26 feet.

flow keeps the range of temperature distribution (for case analyzed temperature ranged

from 950° F at bottom to 446° F at the top of the capsule) to a minimum.

SECTION VI

PLANT AVAILABILITY
AND SAFEGUARDS RELIABILITY

Availability work during the reporting period consisted of establishing procedures, methods of analysis, and data sources for plant availability and safeguards reliability evaluations. This work is reported in this section. Specific plant availability work is reported in subsection 3.1.1, Plant Availability Targets.

6.1 DESIGN CHECK LISTS

6.1.1 General6.1.1.1 Introduction

The FFTF reactor has a difficult availability requirement of 75%. In order that General Electric make a best effort to meet this requirement, a "systems availability engineer" will be appointed. This engineer will have the responsibility of leading and coordinating all work on availability prediction and improvement for the FFTF. However, he must have close cooperation from the design engineers, and it is important that the design engineers understand how their separate efforts on availability relate to overall system availability. To those ends, this procedure is being established.

At the same time, a similar procedure is being established for safeguards reliability. There will be appointed a "safeguards reliability engineer," with responsibility analogous to the systems availability engineer. These two men will be working with the same design engineers.

6.1.1.2 Systems Availability Procedure

The systems availability procedure is outlined in Table 6-1. The first step is the

establishing of availability bogeys for the FFTF subsystems; this is to be done by the systems availability engineer. Using advice from design engineers, he will make a rough estimate of subsystem availabilities; data will come from operating experience* and from predictions such as the Atomics International and Westinghouse availability predictions for FFTF. The allowable 25% unavailability will then be apportioned among the subsystems, to form their individual bogeys.

Next, the responsible design engineer for each subsystem determines the depth of availability work to be done. Guide lines will be the bogey (more attention to high-unavailability subsystems), obtainability of information (more availability prediction work is necessary where data are scanty), design experience (more attention to known problem areas), and the state of development (more attention to undeveloped equipment).

A failure-modes-and-effects analysis is then performed on each subsystem by its responsible design engineer. Drawing upon his knowledge of the system design, the design engineer conceives of all failure modes in his equipment and traces their consequences through the system. Those failure modes whose consequence results in no system outage can be omitted from further consideration. Depending upon the depth of analysis, the failure modes may be at a high level (e.g., pump freezes) or at a low level (e.g., seizure of hydrostatic bearing in pump).

*For estimates of availability drawn from reactor operating experience, see two reports by D.R. Riley: "Reliability Estimates in Reactor Development," Report No. GEAO-0028; and "Nuclear Plant Availability—Theory and Experience," Report No. GEAO-0029.

TABLE 6-1

SYSTEMS AVAILABILITY PROCEDURE

Step No.	Description	Responsible Engineer	Data Sources
1.	Establish availability bogeys for subsystems	System availability engineer.	Estimates from past performance; AI report.
2.	Determine depth of availability work on each subsystem	Responsible design engineer.	Bogey; obtainability of information; design experience; state of development.
3.	Failure-modes-and-effects analyses	Design engineer.	System design
4.	Maintainability analyses	Design engineer.	Experience with repair.
5.	Obtain failure-rate data	Design engineer.	FARADA; Edison Electric Institute; APED work; reactor experience.
6.	Calculate subsystem availabilities	Design engineer.	Steps 3, 4, 5
7.	Rectify if bogeys not met (change bogey, use redundancy, or redesign)	Design engineer and system availability engineer.	Steps 1, 6
8.	Calculate system availability	System availability engineer	Step 6

Next, a maintainability analysis is conducted upon each subsystem by its design engineer. By considering repair experience in operation, he estimates average values for each of these time intervals: time to detect and diagnose failure, decay time (if applicable), repair time (including logistics), and restart time. The sum of these times is the mean-time-to-repair (MTTR).

Failure-rate data must then be obtained by the design engineer. Various sources are suggested in Table 6-1. Consultants from the G. E. Research and Development Center will assist as necessary by the interpreting failure rate data or suggesting other sources.

Subsystem availabilities (or unavailabilities, if preferred) can then be calculated by the design engineers. For serial equip-

ment (equipment so arranged that all parts must be operable for the equipment to be available), an excellent approximation for unavailability U is $U = \sum \lambda_i \mu_i$, where λ_i is the failure rate and μ_i is the MTTR for the "ith" component. For equipment including redundancy or alternate modes, formulas for unavailability will have to be developed.*

The next step is rather obvious: rectify the equipment if the availability calculation shows the bogey will not be met. Courses of action might be redesign or use of redundancy; or possibly some relief can be obtained on the bogey. Whenever a more expensive redesign is

*For availability formulae see two reports by P. F. Albrecht, both entitled "Steady-State Availability Model for a System with Identical Units," R&DC Reliability Memos 54 and 54a, dated May 8, 1967 and May 15, 1967.

contemplated, its incremental cost should be balanced against the monetary worth of the unavailability improvements; this should be investigated jointly by the design engineer and the systems availability engineer.

Lastly, the systems availability engineer will calculate the overall system availability. His inputs will be the subsystem unavailabilities, as calculated by the design engineers.

6.1.1.3 Safeguards Reliability Procedure

Refer to Table 6-2. The first step is to be performed by the safeguards reliability engineer: translate Atomic Energy Commission (AEC) safety criteria into qualitative requirements upon each subsystem. The safeguards reliability engineer may, in addition, impose such further requirements as he deems necessary.

Next, the safeguards reliability engineer defines the accidents whose probabilities are to be analyzed. This may include only the design basis accident (DBA), or it may include other accidents of lesser or greater serverity.

The design engineers responsible for the various subsystems then determine the depth of safeguards reliability work in their areas. This decision will be based upon design experience (likelihood of accidents), state of development (degree of certainty of meeting criteria), and obtainability of reliability information.

A fault tree analysis is then conducted on each subsystem by its design engineer. This is similar to a failure-modes-and-effect analysis, (FM&EA), except that the fault tree analysis works backwards from faults (accidents) to the equipment failures, rather than tracing forward from equipment failures to their consequences. The fault trees should give a clear indication of which failure modes can bring about each accident.

Construction of a fault tree is facilitated if the corresponding FM&EA has previously been done.

The next step is examination of the fault trees to show that the AEC criteria are being met. With proper design, this step should be almost automatic.

Next, failure-rate data are to be obtained for all failure modes connected with accidents. Sources of these data are the same as for availability prediction, and indeed the failure modes would usually also be considered in the availability analysis.

As the next step, the design engineers calculate the probabilities that their subsystems participate in causing the various accidents. If an accident occurs only when two specific failure modes occur, the per-year accident probability P_A can be approximately calculated as

$$P_A = 8760 \lambda_1 \lambda_2 T_I,$$

where λ_1 and λ_2 are the failure rates for the two failure modes, and T_I is the interval between equipment inspection (all units in hours). For other situations, probability formulae will have to be developed.

Finally, the overall probability for each defined accident will be calculated by the safeguards reliability engineer. He will use the subsystem accident probabilities, as calculated by the cognizant engineers.

6.2 COMPUTATIONS BASE

6.2.1 Analysis Failure Mode and Effects

6.2.1.1 Introduction

The development of an analytical reliability model requires a systematic

TABLE 6-2

SAFEGUARDS RELIABILITY PROCEDURE

Step No.	Description	Responsible Engineer	Data Sources
1.	Establish qualitative subsystem requirements	Safeguards reliability engineer.	AEC criteria
2.	Define accidents to be considered	Safeguards reliability engineer.	Experience
3.	Determine depth of safeguards reliability work on each subsystem	Responsible design engineer.	Obtainability of information; design experience; state of development.
4.	Fault tree analyses	Design engineer	System design
5.	Determine that AEC criteria are met by each subsystem	Design engineer.*	Steps 1 and 4
6.	Obtain failure-rate data	Design engineer.	FARADA; Edison Electric Institute; APED work; reactor experience.
7.	Calculate accident probabilities for each subsystem	Design engineer.	Steps 4 and 6
8.	Calculate system accident probabilities	Safeguards reliability engineer	Step 7

*Final approval of Safeguards Reliability Engineer must be obtained.

examination and documentation of all modes of equipment failure and the categorization of their effects by seriousness or criticality. This type of procedure is commonly called a "Failure Mode and Effects Analysis (FMEA)." The primary purpose of such an analysis is to establish a disciplined approach ensuring recognition of failure modes inherent in the equipment design and their cascading effect on equipment or system operation. FMEA's are also applicable to performing hazards or safety type analysis by including external inputs resulting in accidents not related to equipment malfunction.

There are numerous examples of FMEA's procedures and forms in the technical literature. Different products

and customer needs may dictate some variation in format. A procedure that has been used by the Reliability Branch of the R&D Center* and the format used is described in this memo.

6.2.1.2 Procedure

- a. For each major subsystem document the functional and operational data pertaining to the subsystem. Develop data information flow charts and signal sequence charts to depict the flow and sequence of intelligence through the system. Emphasis should be placed on the transmission of commands and responses within the system.

*General Electric Company, Schenectady, New York

- b. Perform similar studies down to the replaceable module level, assuring that there is clear identification of module with function. This breakdown will result in pyramids, in which the functions to be performed appear at the tops and the lower modules associated with performance of these functions will appear at the bottoms. Modules associated with more than one function will appear in more than one pyramid.

- c. Initiate the FMEA, utilizing form of Table 6-3, by placing each module on the form and noting its failure modes and consequences.

In considering failure modes for each of the elements in the lowest tier, both external (input) failure modes and internal failure modes must be considered.

(1) External (Input) Failure Modes

Analysis of failure modes in this category shall proceed as follows:

- (a) Consider all possible modes of failure of an equipment input.*
- (b) Assume that the equipment is operating correctly, then analyze the effect of each input failure mode upon the output of the equipment.
- (c) Repeat (a) and (b) for each equipment input.

*When used for safety analysis, emphasis should be placed on improper human inputs as well as equipment inputs.

(2) Internal (Output) Failure Modes

Analysis of failure modes in this category shall proceed as follows:

- (a) Consider all possible modes of failure to an equipment output due to some internal defect.
- (b) Analyze the effect of each output failure mode upon the operational effectiveness of the equipment under study. The specific cause(s) of the failure modes may not be determinable at this time.
- (c) Repeat (a) and (b) for each equipment output.

Note that an internal failure within a module gives rise to an internal failure mode, which in turn becomes an external failure mode for the next module in the functional chain. Thus, the performance of external and internal failure modes and effects analysis provides a means of tying the lateral effects of failure modes together so as to assure complete severage of the subsystem.

To summarize this procedure, each equipment or system shall be considered as consisting of a group of functional components. A mode of failure at the highest functional assembly level will manifest itself as an effect on equipment output. The analysis should therefore start at this highest functional assembly level and each external and internal failure mode analyzed in the manner previously outlined.

TABLE 6-3

FAILURE MODE AND EFFECTS ANALYSIS

[illegible]

It should then progress to lower levels of equipment breakdown until major modules or replaceable assemblies are reached. It is recognized that during early design phases, it may only be possible to carry this down to major assemblies, but after detail design has been accomplished it can be refined to include replaceable modules, or even piece parts.

Failure mode pyramids should be prepared which graphically illustrate the cascading effects of lower level failures into failures of the subsystem. These pyramids will consist of interrelated blocks showing the series-parallel relationships of particular failure modes on the specific subsystem output failure. Each failure mode block in the failure pyramid should be traceable to the FMEA by identifying the specific sheet of the FMEA on which this failure mode appears and the estimated rate of occurrence. The FMEA sheets and the associated failure pyramids comprise the failure modes and effects analysis for each subsystem.

- d. Calculation of failure rates for each failure mode. There are numerous sources for failure rate information. Preferably such information should come from experience with related equipment. When this information has not previously been developed, examination of product service data, customer complaints, engineering and qualification tests, and similar sources can be utilized to develop such data. Where such information is completely lacking or inadequate, sources of generic failure rate data

such as MIL-HDBK-217A, MIL-STD-756A, FARADA information from the U.S. Navy Ordnance Laboratory, or similar sources may be utilized.* In some cases component vendors may supply appropriate failure rate data.

It may be more difficult to assign failure mode probabilities than to obtain total failure rates on components, but this can often be accomplished through information available in the existing literature or on the basis of test and evaluation experience with similar equipment. Occasionally such data must be developed through analogous failure mechanisms on other equipment. Table 6-4 shows a typical development of such failure rates for a fluid amplifier control.

6.2.1.3 Completing the Failure Mode and Effects Analysis (FMEA) Form

To adequately analyze failure modes and their effects, formal documentation should be made utilizing Table 6-3. Separate sheets should be completed for major assemblies or functional groups and detail analysis performed to the lowest practical level. The following information should be contained within each of the columns on the FMEA form.

1. Item number—Assign an item number to each component or module examined so that this can be utilized in identifying blocks in the failure pyramid.

*Generic data by its very nature tends to be imprecise and the user must recognize some of the inherent weaknesses.

TABLE 6-4

VAPCOM FAILURE RATES—
FLUID AMPLIFIER CONTROL

<u>Component</u>	<u>Failure Mechanism</u>	<u>Failures/hour × 10⁶</u>
Fluid Amplifier	Contamination per 20 mil port	0.35
	Leakage, crossport per element	0.02
	Leakage, external per connection	0.03
	Breakage, external per connection	0.01
Variable Helmholtz	Fatigue	2.0
Fixed Helmholtz, Capacitor	Contamination—fouling, plugging	0.15
3-D Cam and Follower	Abrasion	3.0 gradual
	Change position	1.0 sudden
Restrictors—Resistors, Capillary, Pitot tube	Contamination—fouling, plugging	0.45
Frictionless pivot	Fatigue	0.15
	Plastic deformation	0.05
Check Valve	Seat or face abrasion	0.30
	Spring fatigue	0.15
Reed Tuning fork	Fatigue	0.15
Rack and Pinion Gear	Fatigue, fouling, abrasion	4.0/set

2. Name—Identify component utilizing the same terminology used on functional block diagrams or schematics.
3. Failure mode—The component failure mode is defined as the manner in which given component or module can fail. Thus electrical components may fail catastrophically as in an open or shorted mode or may degrade because of drift characteristics. Mechanical components fail because of

rupture, jamming, or bursting, for example. For some failure modes there may be only one underlying mechanism of failure responsible whereas for others different mechanisms may contribute to the same failure mode.

4. Failure mechanism—This describes the fundamental mechanisms that may be responsible for the potential mode of failure. Its appearance in the FMEA is important as a means

of identifying those environmental and operational factors that are the major contributors to potential failure modes. It is not anticipated that specific failure rates can always be identified with a failure mechanism if multiple mechanisms contribute to the referenced failure mode.

5. System effect on failure—This should categorize the effect of a component or module failure on the subsystem and where possible, also indicate the ultimate effect on the entire system.
6. Rate of failure effect—Indicate whether the particular failure mode under consideration and its associated system effect occurs in a gradual detectable manner or whether the mode occurs suddenly in a catastrophic manner.
7. Failure class—This column tries to categorize the effect of failure into specific categories depending upon the consequence or criticality of the failure. The most severe failure is usually categorized as a Class I failure, and the least critical as the Class "N," when N categories are used. It will normally be necessary to establish different categories for different products dependent on the nature and consequence of failures in a specific product. In some products, failure may result in loss of human life or injury. Such factors should always be assigned the highest criticality level. In other products, this may never be a credible factor, but categorization may be in terms of full mode and degraded mode operations, or resulting customer reactions.

Experience seems to indicate that at least three categories should be considered, and in many cases five or six categories may achieve greater insight into the problem.

8. Remarks—Indicate special design considerations relating to steps taken to reduce failure consequence or occurrence. Catastrophic effects upon equipment should be noted here.
9. Failure risks—Assign failure probabilities to each of the failure modes. Consider at least two system states:

System state 1—control operating.
System state 2—control dormant.

An example of a typical sheet from a failure mode and effects analysis (in this case the same fluid amplifier control described in the discussion of failure rate determination), is shown in Table 6-5.

After the FMEA has been completed and the interaction of failure modes from module level to subsystem level modelled by means of a logic diagram or failure pyramid, overall subsystem failure rate determination should be made for each of the five categories identified. A typical example of such a failure rate summation by various categories is shown for the aforementioned fluid amplifier control computer in Table 6-6.

6.3 DATA SOURCES OF FAILURE RATES

Some sources of published failure rate data are briefly summarized in the following sections, indicating the nature and the type of information available.

TABLE 6-5

FAILURE MODE AND EFFECTS ANALYSIS

C - TEMPERATURE CONTROL CIRCUIT

Page 5

(1) Item No.	(2) Name	(3) Failure Mode	(4) Failure Mechanism	(5) System Effect of Failure	(6) Rate of Failure Effect		(7) Failure Class (consequence)	(8) Remarks (Compensating features, etc.)	(9) Failure Risk									Maint.	
									System State			System State			System State				
									Critical			Major			Minor				
					λ	t	λt	λ	t	λt	λ	t	λt						
5	Beat Detector, V5	No output to V6	C1, C4, B4	Fuel Flow not responsive to T4			1		0.71										
				Sluggish response to T4			4											0.73	
		Incorrect output to V6	C6, L6	Incorrect fuel flow			2					0.37							
6	Transmission Line	No output to V6	Plugged	Fuel flow not responsive to T4			1		0.45										
		No output to V6	Broken	Fuel flow not responsive to T4			1		0.01										
7	Restrictor, R6	Low input to V6	Plugged	Sluggish response to T4			4											0.45	
8	Restrictor, R7	Same as Item 7					4											0.45	
9	Capacitor, C3	Low input to V6	Contamination	Sluggish response to T4			4											0.15	
10	Decoupler, V6	No output to V7	C1	Fuel flow not responsive to T4			1		0.35										
		Low output to V7	C2, C3, C4, C5, L2, L3, B2, B3	Sluggish Response to T4			4											1.48	
		Incorrect output to V7	C6, L6	Incorrect fuel flow			2					0.37							
11	Amplifier, V7	No output to M3	C1	Fuel flow not responsive to T4			1		0.35										
		Low output to M3	C2, C3, C4, C5, C6, L6	Sluggish response to T4			4											1.40	
		Incorrect output to M3		Incorrect fuel flow			2					0.37							
12	Temperature	No output		W_f not responsive to T4			1		2.10										
	Circuit, M3	Low output (Gain)	See detail FME Sheets	Incorrect fuel flow			2					6.94							
		Low output, Neg. Bias	p. 23, 24	Max. W_f			1		2.11										
		Low output, Pos. Bias		Min. W_f			1		2.11										

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TABLE 6-6

SUMMARY OF FAILURE RATE DATA

Function	FME Sheet No.	Failure Rate per 10 ⁶ Hours			
		Critical	Major	Minor	Maint.
I. Computer					
A. Pressure Ratio Control	1-2	0	9.18	3.00	0
B. Speed Control	3	0	24.58	9.99	0
C. Temperature Control	4-8	32.57	19.04	3.18	7.54
D. Fan Pressure Ratio Over-Ride	9-10	2.77	3.00	0	5.12
E. Compressor Pressure Ratio Over-Ride	11	2.47	3.45	0	5.87
F. Fan Speed Over-Ride	12	2.32	3.00	0	5.12
G. Compressor Speed Over-Ride	13	2.32	3.00	0	5.42
H. Low Fan Pressure Ratio Over-Ride	14-15	2.32	3.15	0	4.42
I. Low Comp. Pressure Ratio Over-Ride	16	2.32	3.15	0	3.97
J. Over-Ride Logic	17	0	0	0	3.89
K. Start Control	18-22	0	0	0	48.60
L. T ₂₃ Sensor	28-29	11.50	7.22	3.0	9.78
Total, Computer		58.59	80.77	19.17	99.73
II. Servo Valves	30	18.10	54.30	0	0
III. Fuel Metering	31	43.7	0	14.0	27.0

6.3.1 Published Data

- a. MIL-HDBK-217 (Principal use—electronic modules and systems.)

The most widely used source of generic failure rate data on electronic parts is MIL-Handbook 217A-Reliability Stress and Failure Rate Data for Electronic Equipment. This document is for sale by the Superintendent of Documents, U.S. Government Printing Office, Washington, D.C. It is often called for in military-type contracts as the obligatory source of failure rate data in reliability estimates unless

the contractor can provide evidence of more pertinent source information. The R&DC Reliability Branch has on a number of occasions utilized 217 for prediction purposes and compared results with actual field experience. Where this has been done on utility-type equipment such as Pittsfield built voltage regulators, results have indicated 217 to provide very pessimistic results and actual experience may be as high as an order of magnitude better than predicted. This difference is attributed to the relatively high stability of utility equipment designs and the relatively benign operational

environment and lack of weight and space restraints compared to aerospace equipment.

- b. FARADA (Principal use—airborne equipment of all types including electrical, electronic, hydraulic, and pneumatic systems.)

The most comprehensive compendium of "source related" failure rate data is the failure rate data program (FARADA) sponsored jointly by the Navy, Army, Air Force, and NASA. The FARADA documents, issued in six volumes, contain electrical and electronic as well as mechanical component information. Volume 3 contains source information, including the programs on which the data have been collected and the reports and individuals responsible for submitting this information to the FARADA Analysis and Evaluation Group. The FARADA manuals are generally only available to participating prime and subcontractors in the program—in other words, those that supply data to FARADA (usually as part of a contractual requirement).

A review of FARADA data will indicate that most of it comes from aircraft or missile environments and this factor must be considered when such data are supplied to the analysis of non-military systems. Examination of FARADA data will show wide variations in calculated failure rates. It is therefore necessary that the FARADA source document (FARADA Volume 3) be consulted to assure that the most relevant source is utilized for the selection of generic failure rates. In some cases, a composite failure rate reflecting the best engineering judgment may be most appropriate.

c. Symposium Transactions

Some published failure rate data are contained in the proceedings published as a result of the two major annual symposia conducted on the subject of Reliability. The two symposia are:

1. The Annual Reliability and Maintainability Conference sponsored jointly by ASME, SAE, and AIAA and copies of the transactions may usually be purchased through member societies such as SAE. In some cases publication rights have been sold to a commercial publisher but this will vary from year to year.
2. The annual symposium on Reliability is sponsored by the IEEE, IES, SNT, and ASQC. It too contains occasional papers dealing with failure rates of specific components or systems. Thirteen proceedings have been published and should be available through the IEEE. The cumulative index of papers contained in the first 10 proceedings is also available from the IEEE, 345 E. 47th St., N.Y., N.Y.

While the majority of papers in both of the symposia deal with aerospace or military systems, there are on occasion, papers describing power systems or marine propulsion systems containing some generic data useful to FFTF.

d. Periodically Issued Publications

Such publications as Evaluation Engineering published monthly by A. Verner Nelson Associates,

20 N. Wacker Drive, Chicago, Illinois, often contain articles giving specific information on the reliability of electronic components. Generally, these articles do not present sufficient detail to allow direct use of this information.

Another periodically published document is the monthly NASA Reliability Abstracts and Technical Reviews. While this abstract primarily reviews papers dealing with reliability theory and practice, it is not a major source of failure rate information but may touch on this briefly in discussion of analytical techniques and problem areas.

e. Other Government Publications

There are a number of other government publications and handbooks containing some failure rate data, but the use of these does not at the moment appear to provide a significant advantage over MIL-STD-217A. These include:

1. RADC Reliability Notebook published by the Rome Air Development Center. This document can be used as a design guidance manual for the user of failure rate data and provides more extensive discussion and reference information regarding environmental considerations and failure mode effects than MIL-Handbook-217. The Handbook has been revised several times and some of the earlier revisions are available through the Department of Commerce, Office of Technical Services, as PB-161894-1 (1959 edition), -2 (1962 revisions),

and -3 (1963 revision). A new revision is currently in process and Volume II of this revision has been issued as Air Force document RADC-TR-67-108, Vol. II. This volume contains data on electrical and electronic devices including motors, cables, and microelectronics. Radiation effects are discussed but their influence on failure rates is not quantitatively treated. Unlike the earlier editions of the RADC Reliability Notebook, this new issue is not available through the Department of Commerce and is subject to the special State Department controls on transmittal to foreign nationals or governments. This restraint must be considered by FFTF in using this document.

2. The Bureau of Ships Reliability Design Handbook prepared by the Federal Electric Corporation and issued as NAVSHIPS 94501. This is a general primer with discussion of a design guidance nature. It contains some useful general information as well as some detailed guidelines on part selection that should be primarily of interest to the circuit packaging designer.

f. The Interagency Data Exchange Program

This program, usually referred to as IDEP, lists test reports generated by participating Government and NASA contractors on specific hardware items. Its purpose is not to generate failure rate data but to provide detail test information among users. IDEP reports are periodically reviewed by other

agencies such as NASA to determine the feasibility of generating generic failure rate data from the combined results of a number of IDEP source documents. Where such combination of data is available, NASA may issue this under their PRINCE system controlled by NASA, Huntsville. PRINCE information is also fed into the FARADA system. FFTF's interest in the IDEP system will be principally with respect to detail test reports rather than quantitative data. IDEP contains not only information on component tests, but also material tests.

g. Nuclear Power Plant Operating and Safety Experience

1. The U.S. Atomic Energy Commission has sponsored a program for the acquisition of data on commercial nuclear plants in the U.S.A. A two-volume report providing useful information for nuclear power plant analysis has been issued by the contractor conducting this study for the AEC: Holmes and Narver, as Report No. HN185, "An Analysis of Nuclear Power Plant Operating and Safety Experience" by John Garrick, published December 1966.
2. A more extensive report covering operation of British reactors has been issued by the United Kingdom's Atomic Energy Authority as AHSB(c)R117, entitled, "Safety Assessment with Reference to Automatic Protection Systems for Nuclear Reactors," by A. E. Green and A. J. Bourne.

3. The United Kingdom Atomic Energy Authority has issued a study entitled, "Atomic Power Plant Reliability Data."

h. Microelectronics Failure Rate Data

TRW Document No. 9990-6183-R000. This document entitled, "Evaluation of Experience with Microelectronic Integrated Circuits," by G.R. Van Hoorde, TRW Systems, One Space Park, Redondo Beach, California (May 1967), is one of the most comprehensive sources of failure rate of microelectronic devices. Information is given by device type and packaging approach and includes application and derating data. Other important sources of reliability data of microelectronics include:

1. NASA Parts Publication NPC 275-1, Microelectronics Device Data Handbook, and
2. Various MIT Instrumentation Lab reports by Partridge, Hanley, and Hall, such as Report E-1679, "Attainable Reliability of Integrated Circuits for Systems Application."

i. Dormant (Nonoperating) Equipment

RADC-TR-67-307, "Dormant Operating and Storage Effects on Electronic Equipment and Part Reliability" (July 1967). This report encompasses 760 billion-part-hours of dormant and storage information on various electronic components. It should be useful to FFTF for analysis of control systems maintained in a standby mode.

6.4 CONCLUSION

Despite the apparent availability of reliability data from numerous sources, little confidence can be placed in the precision of results obtained when such data are applied to a specific manufacturer's equipment, unless one has developed experience in the use of specific failure rate sources and established appropriate application factors based on his experience. Where this

has been done, good correlation has been achieved on electrical and electronic parts, and in some mechanical systems, although results in the latter area have not generally been as satisfactory, and the available techniques less tractable. Where no prior data exists, prediction data are still useful for making design and system comparisons and trade-off studies, provided that sensitivity tests are made to assure that effects of uncertainty, when these are high, do not result in contradictory conclusions.

SECTION VII

NUCLEAR CHARACTERISTICS

7.1 PEAK NEUTRON FLUX

A study to investigate the effect on the peak neutron flux of changes in various design parameters has been completed. The results of this parameter study were used to choose a base case which is defined in Table 7-1. The currently calculated peak neutron flux in the base case is 1.02×10^{16} n/cm²-sec.

The base case was perturbed to investigate the effects of fuel pellet density, channel wall thickness, and channel wall material on the peak neutron flux. These results are listed in Table 7-2 with appropriate comments. Also included in Table 7-2 is an investigation of increasing the number of fuel pins per bundle from 127 to 169. This increases the closed loop volume (six closed loops) by about 9 liters. Although the total core volume is slightly increased, no detectable change is noticed in the peak neutron flux. This behavior is also noted in Table 7-3 where 12 closed loops (one channel each) were considered. When the 12 loops were uniformly distributed only a 2% reduction in peak neutron flux resulted. When seven loops are concentrated at the center, a 10% reduction in peak neutron flux results.

Also included in Table 7-3 are perturbations of the base case in fuel pin outside diameter, cladding thickness, core height, channel material, and plutonium composition. As observed in Table 7-3, the fuel pin outside diameter is the only parameter significantly influencing the peak flux. The effect of the fuel pin outside diameter, cladding thickness, and core height were determined from the following coefficients:

$$\frac{d \phi_{\max}}{d(\text{pin diameter})} = -3.5 \times 10^{16} \frac{\text{n/cm}^2 - \text{sec}}{\text{inch}}$$

$$\frac{d \phi_{\max}}{d(\text{cladding thickness})} = -1.6 \times 10^{16} \frac{\text{n/cm}^2 - \text{sec}}{\text{inch}},$$

and

$$\frac{d \phi_{\max}}{d(\text{core height})} = -0.009 \times 10^{16} \frac{\text{n/cm}^2 - \text{sec}}{\text{inch}}$$

These coefficients were determined from the calculational results listed in Table 7-4. All calculations included the following assumptions:

1. Total Power = 400 MW
2. Midburnup conditions ~35,000 MWd/T
3. No reflector B₄C control inserted
4. $k_{\text{eff}} = 1.01$, to compensate for 1% in reflector B₄C control which would normally be inserted.
5. Fuel pellet density = 0.9×11.08 gm/cc
6. A 0.002-inch gap exists between the fuel pellet and cladding
7. 127 fuel pins/bundle
8. 0.100-inch Incoloy 800 channel walls
9. 0.030-inch space between adjacent channels
10. Room temperature atom densities (except sodium) and dimensions
11. Peak kW/ft allowable, obtained from Reference (10), is reduced by 1.1 (local peaking allowance) and 1.2 (overpower allowance).

TABLE 7-1

PEAK NEUTRON FLUX BACKUP
DESIGN INITIAL LOADING

Peak Neutron Flux: $\phi_{\max} = 1.02 \times 10^{16}$

DESIGN PARAMETERS

Power	400 MW
Peak kW/ft Absorbed in Fuel at Overpower	17.8
Overpower Factor	1.2
Local Peaking Factor	1.1
MeV Produced per Fission	215 MeV
Fraction Absorbed in Fuel	0.91
Reflector Control Inserted	2\$
k_{eff}	1.00
Pressure Drop	100 psi
Core Height	33 inches
Fuel Pin Diameter	0.21 inch
Cladding Thickness	Stainless Steel, 0.015 inch
Channel	Incoloy 800, 0.100-inch thick
Pu Composition	12% Pu-240
Test Volume	Six Closed Loops, Two Open Loops, One Channel Each, One Closed Loop at Center, 15% Fuel in Closed Loops.

Neutron Spectrum: 66% of Flux Above 0.1 MeV

-
12. The fraction of power absorbed in fuel = 91% 13. Total MeV released per fission = 215 MeV	14. Pressure drop = 100 psi 15. Fuel P/d is obtained from Reference (10) for 100 psi pressure drop 16. Fuel cladding is stainless steel

TABLE 7-2

PEAK NEUTRON FLUX - PERTURBATIONS

Description	Base Case	Fuel Pellet Density = 95% ρ Theoretical	Channel Wall Thickness = 0.070 inch	Channel Wall Thickness = 0.135 inch of Stainless Steel	169 Fuel Pins/Bundle
Bundle Area (cm ²)	58.62	58.62	56.47	61.18	76.37
Closed Loop Volume (liters)	29.49	29.49	28.40	30.77	38.41
Total Core Volume (liters)	796	806	762	851	813
H/D Ratio	0.762	0.758	0.779	0.737	0.754
No. Driver Assemblies	153	155	152	157	118
Fissile Pu Enrichment (%)	19.4	18.6	19.2	20.1	19.4
Mass Fissile Pu	435	443	428	462	446
Peak kW/ft	13.48	13.48	13.48	13.48	13.48
Peak Flux (n/cm ² -sec)	1.018×10^{16}	1.013×10^{16}	1.041×10^{16}	0.984×10^{16}	1.017×10^{16}

Comments: U²³⁸ Doppler (Na in)
T dk/dT = -0.0062

Increasing the fuel density, while holding the peak kW/ft constant, slightly increases the radial peaking effect. This is somewhat compensated by a lower enrichment.

Decreasing the channel wall thickness reduces the core volume, parasitic captures, and the radial peaking factor. The net effect is a 30% reduction in channel wall results in ~2% increase in neutron flux.

The lower peak flux in this case results from increasing the channel wall thickness and using stainless steel as channel material.
U-238 Doppler (Na in)
T dk/dT = -0.0060

The primary effect here is to increase the test volume and in-core control rod volume. This does not appear to influence the peak flux.

TABLE 7-3

PEAK NEUTRON FLUX
PARAMETER STUDY

Base Case: * $\phi_{\max} = 1.01 \times 10^{16}$

Parameter	Base	Change From Base Case	$\phi_{\max}$
Fuel Pin Outside Diameter	0.21 inch	0.25 inch	0.87×10^{16}
Cladding Thickness	0.015 inch	0.010 inch	1.02×10^{16}
Core Height	33 inches	36 inches	0.98×10^{16}
Channel Material	Incoloy 800 (0.100-inch thick)	Type 304 Stainless Steel (0.100-inch thick)	1.00×10^{16}
Pu Composition	32% Pu-240	12% Pu-240	1.01×10^{16}
Test Volume	Six Closed Loops One Channel Each, One at Center	Twelve Closed Loops One Channel Each, One at Center	0.99×10^{16}
		Six Closed Loops, Center Loop Size Equal Seven Channels	0.91×10^{16}

Parameters Held Constant:

Power = 400 MW

Pressure Drop = 100 psi

Peak kW/ft $\approx$ 18

Overpower Factor = 1.2

Local Peaking Factor = 1.1

$k_{\text{eff}} = 1.01$, Nickel Reflected

*Base Case was Calculated for Midburnup (35,000 MWd/T); Hence, its $\phi_{\max}$ is not Directly Comparable to the Initial Loading $\phi_{\max}$.

TABLE 7-4

FFTF NUCLEAR PARAMETER STUDY

Case No.	1	2	3	4
Core Height (in.)	36	36	36	33
Fuel Pin Outside Diameter (in.)	0.210	0.210	0.250	0.210
Fuel Cladding Thickness (in.)	0.010	0.015	0.015	0.015
P/d Ratio	1.291	1.286	1.194	1.260
Bundle Area (cm ²)	62.57	62.08	73.73	58.62
Closed Loop Volume (liters)	34.33	34.06	40.46	29.49
Total Core Volume (liters)	744.89	789.10	937.14	746.91
H/D Ratio	0.898	0.872	0.800	0.787
Driver Bundle				
Fuel	0.3713	0.3358	0.4224	0.3557
Na	0.3931	0.3903	0.3273	0.3646
Steel	0.1207	0.1586	0.1438	0.1612
Incoloy 800	0.1149	0.1153	0.1065	0.1185
Core Assemblies				
No. Driver Bundles	121	130	130	143
No. Closed Loops	6	6	6	6
No. Control Rods	3	3	3	3
Fissile Pu Enrichment	17.60%	18.82%	14.90%	18.18%
Mass Fissile Pu	384.2 kg	396.0 kg	470.0 kg	383.9 kg
Peak kW/ft	14.37	13.47	14.17	13.48
Peak Flux (n/cm ² - sec)	0.993×10^{16}	0.985×10^{16}	0.844×10^{16}	1.011×10^{16}

All calculations assumed a central closed test loop with the rest of the core homogenized into one zone. The calculations were converged on $k_{\text{eff}} = 1.01$ and the calculated peak kW/ft = allowable peak kW/ft. The results and a description of the converged calculations are listed in Table 7-4.

7.2 COMPARISON OF PNL AND G.E. NEUTRONICS CALCULATIONS

A comparison of nuclear parameters was made by PNL and G.E. for both the

FFTF backup design and ZPR-III Assembly 48. These comparisons are described below.

7.2.1 Areas of Agreement

Close agreement was obtained for the two most important parameters in the comparison of the reference and backup core—reflector control worth and peak neutron flux, close agreement in the U-238 Doppler effect was also obtained in the analysis of ZPR-III Assembly 48, i.e., $\Delta k = -0.0104$ versus $\Delta k = -0.0097$.

7.2.2 Discrepancies

The criticality factor of G.E.'s backup design calculation was 1.5% lower than PNL's. Had we used BAS02* instead of BAS01, the difference would have been larger. For example, using BAS02, Hutchins shows a 2.5% difference between PNL and G.E. for Assembly 48. Of this difference, 0.5% to 1% is attributed to the use of diffusion theory by G.E. as opposed to transport theory by PNL.

The comparison of calculations on the G.E. backup reactor is shown in Table 7-5. (11) A comparison of calculations on ZPR-III Assembly 48 are shown in Table 7-6. (12)

7.3 BACKUP DESIGN CRITICAL EXPERIMENT

A critical experiment for the backup FFTF design has been proposed. The most important parameters to be measured are:

- a. Critical mass
- b. Reflector control worth
- c. Sodium loss effect
- d. Doppler effect

It was recommended that the Phase B critical program be extended to include a mockup of the backup design (to be referred to here as Assembly VI) to follow the presently planned PNL sequence of assemblies. Approximately 300 kg of B_4C will have to be procured for this work.

Since most of the reactivity control for the backup design will be supplied by the

*Current GE Cross Section File, based on ENDF/B

reflector control system, any critical experimental program for this design should examine carefully the reflector worth. The backup design contains about 280 kgs of B_4C in the reflector control rod followers. This is about an order of magnitude greater than what is available at ZPR-III.

Performing reflector measurements with the available B_4C would require a large extrapolation to the full reflector system control worth. Therefore, it is necessary to procure about 300 kg of B_4C for the Assembly VI measurements. The lead time on this procurement necessitates placing this critical experiment late in the Phase B program.

Consideration was given to performance of sodium loss and Doppler experiments for the backup design using the fuel drawers loaded for the reference design assemblies. It was concluded that dilution of the fuel drawer with sodium-steel drawers would deviate from the backup design too much to provide meaningful information for the sodium loss and Doppler effects. Therefore, it is recommended that Assembly VI contain new fuel drawers, loaded especially for the backup design. It may turn out that a relatively minor modification of the reference drawers will be all that is required to provide a good backup critical.

7.3.1 Details of Proposed Assembly VI

The proposed modification of the reference core fuel drawers must be determined from later calculations. A decision will be made later concerning the detail to which test loops and control rod positions (as are mocked up in Assembly IIb) should be mocked up in Assembly VI.

TABLE 7-5

G.E. BACKUP REACTOR

<u>Parameter</u>	<u>PNL⁽²⁾</u>	<u>2D Diffusion</u>	<u>G.E.⁽³⁾</u>	<u>Footnote</u>
k_{eff} (No rods)	1.0411		1.0259	4, 5
δk ($B_4^N C$)	-0.0520	-0.0537	-0.0465	6
δk ($B_4^{10} C$)	-0.0798		-0.0858	7
ϕ (400 MW, No rods)	0.998×10^{16}		1.00×10^{16}	8, 9
% $\phi > 0.1$ MeV	67.4		66	9

1. Reactor composition and geometry taken from letter to D.R. Riley from A.B. Reynolds, September 14, 1967.
2. 6-group, S_4 model (2DF).
3. Values taken from above reference.
4. PNL assumed $T_o = 1400^\circ K$.
5. A 2-D diffusion calculation (using PNL's 2DB code) gave $k_{\text{eff}} = 1.0355$.
6. 1-D S_4 calculations (DTF-IV) gave $\delta k = -0.0501$ for 6 groups, and $\delta k = -0.0505$ for 12 groups.
7. 1-D S_4 calculations (DTF-IV) gave $\delta k = -0.0772$ for 6 groups, and $\delta k = -0.0770$ for 12 groups.
8. Assuming 215 MeV/fission.
9. At core center.
10. Fundamental mode calculation using core composition.

TABLE 7-6

RESULTS OF COMPARATIVE SPHERICAL CALCULATIONS

	<u>G.E.</u>	<u>PNL</u>
1. k_{eff}	0.9704	0.9968
2. $\Delta k_{\text{Doppler}}$, U-238 in Core Only, $300 \rightarrow 1400^\circ K$		
Direct	-0.0104 ⁽¹⁾	-0.0097 ⁽²⁾
Perturbation Theory	-0.0115 ⁽¹⁾	
3. Pu-239 Central, Worth,		
Per cm^3 of Pu-239 in Core	$+3.64 \times 10^{-6}$	$+3.96 \times 10^{-6}$
$\Delta k/\text{kg}$	$+5.59 \times 10^{-3}$	$+6.0 \times 10^{-3}$

1. Includes 10% increase due to denominator effect in self-shielding calculations.
2. On same basis as G.E. calculation, i.e., no debye temperature correction, includes 10% increase due to denominator effect in self-shielding calculations.

The following experiments will be performed in Assembly VI:

- a. Obtain radial fission traverses.
- b. Measure sodium loss reactivity effect by simulating sodium voiding near the core center and progressively increasing the voided volume until the total reactivity effect is negative.
- c. Measure Doppler effect near center of the core, with sodium removed around the Doppler sample.
- d. Reflector Control: Start replacing radial reflector drawers with B_4C . The simulated test loops and control rod location will be replaced with driver fuel to maintain k_{eff} . When about 70 kgs of B_4C have been loaded in the radial reflector, radial fission traverses will be obtained. These fission traverses, when compared with those obtained in experiment (a), will indicate the effect on the radial power and flux distribution through the use of reflector control. Continue replacing the radial reflector with B_4C until the full control system is simulated (~280 kg of natural B_4C), and again obtain radial fission traverses.

Since the B_4C reflector rod followers are large (6.5 cm in diameter) considerable self-shielding may exist. It is important that the effect of self-shielding be measured adequately since self-shielding may be difficult to determine analytically because of the large flux gradients at the core boundary.

7.4 SUBCRITICALITY MEASUREMENT DURING REFUELING

7.4.1 Introduction

The maximum worth of an FFTF backup fuel assembly, loaded at the center of the core, is 2.2\$. Since it is possible to inadvertently drop an assembly into the core during refueling, it is necessary to assure that the core is sufficiently subcritical during refueling that this accident would not make the core critical.

A discussion of subcriticality measurement is provided here. It is intended that this discussion will contribute to the FFTF backup safeguards position on the refueling accident.

Shutdown reactivity will be measured during FFTF backup refueling by the method of subcritical multiplication. In addition, assurance that sufficient margin to criticality is available can be obtained by periodically withdrawing a specified number of control rods. Both methods are described in this memo.

7.4.2 Subcritical Multiplication

7.4.2.1 Description of the Method

The subcritical reactivity ($1-k$) is related to the neutron count rate (n) recorded by the source range detectors by the proportionality,

$$N \propto \frac{S}{1-k}$$

where S is the neutron source strength.

The neutron source for the FFTF comes from Pu-240 spontaneous fission and from α , n reactions in the oxygen in the fuel. The source is proportional to the number of fuel assemblies in the core and to the plutonium isotopic composition. Since there are more than 100 assemblies in FFTF, each fuel assembly loaded during refueling changes the source less than 1%.

The proportionality factor relating the detector response, N, to the subcritical reactivity is measured during wet critical testing. This calibration can be made after the control rods are calibrated. Starting from critical, a control rod of known worth is inserted and the count rate is measured. This procedure is continued, one rod at a time, until all rods are inserted, at which time k will be ~ 0.92 . Some recalibration will be required periodically as plutonium isotopic composition varies.

7.4.2.2 Criticality Factor During FFTF Refueling

The total reactivity of the FFTF primary and backup control systems is 8% Δk . The 6% Δk of primary control is supplied by 27 reflector control rods; the 2% backup control is supplied by three in-core rods.

During refueling, all control rods are inserted. The reflector rods are operable; the in-core rods are disconnected. When refueling is complete, the maximum criticality factor k is ~ 0.97 .

This value of k can be understood from the following list of FFTF control requirements.

Control System	Control Function	Reactivity
Primary	Fuel Cycle	2%
	Temperature-Refueling	
	Temperature to Full Power	2%

Experiments	1%
Shutdown	1%
Backup Additional Shutdown	2%

At the start of the fuel cycle (i. e., after refueling), the 2% Δk for fuel cycle is not available for shutdown. If a minimum of experiments are loaded in the core, the 1% Δk for experiments is also unavailable for shutdown. This leaves only the 3% Δk for shutdown specifically listed for this function in the table. Thus, the criticality factor for the cold, freshly fueled core with a minimum of experiments and with all primary and backup control rods inserted is $k = 0.97$.

At the end of the fuel cycle, the core is 2% more subcritical with all rods inserted, or $k = 0.95$. An additional 1% subcritical is possible if 1% Δk worth of experiments are in the core.

During wet critical testing at the beginning of FFTF startup, a minimum critical mass will be loaded at refueling temperature. The criticality factor for this core with all control rods inserted is $k = 0.92$.

7.4.2.3 Allowable Uncertainty; Sensitivity of the Method

Since k is so far from unity when refueling is completed and since the count rate varies so strongly with k near critical, a large margin for uncertainty in the correlation between N and $1-k$ and in the count rate is allowable. For example, if a criticality factor of 0.97 were measured and a propagated error as large as a factor would still be no greater than 0.985, which is still 5% subcritical. Some uncertainty will exist in the correlation, particularly since the reflector control rod configuration during refueling will be somewhat different

from that during calibration and also there will be some uncertainty in the count rate measurement. The influence of various reflector control configurations on flux at the detectors will be calculated during design and should be measured after startup. However, it is considered unlikely that the uncertainty will be excessive.

It was pointed out earlier that the maximum rod worth was 2.2\$ (0.8% Δk) and that the $k_{\max}$ during refueling was 0.97. Hence, loading the last bundle in the maximum worth location would change k from 0.962 to 0.97, and increase the count rate N by 27%.

Subcriticalities significantly more than 5% Δk may become difficult to measure accurately because of the effect of the higher order harmonics in the neutron distribution. This problem arises at low subcriticality values with large cores with low neutron leakage. The leakage from the FFTF (>30% of the neutrons) is sufficiently large that the higher order harmonics are expected to pose no problem over the subcriticality range of interest. Further work is necessary to determine just how subcritical one can go before the higher harmonics pose a serious limitation to FFTF subcriticality measurement.

7.4.2.4 Kinetic Response of Count Rate to a Change in k

The response of the detector to the change in k upon loading a fuel bundle is essentially instantaneous. The delayed neutrons contribute so little to k (specifically 0.003) relative to the large margin of subcriticality present during refueling that their influence on the time dependence of the detector response can be ignored during refueling. The influence of delayed neutron kinetics becomes important only as subcriticality approaches 1\$.

These results concerning time dependence of the detector response can be seen by writing the relation relating neutron flux (which is proportional to count rate N) to criticality factor more exactly to include the effect of delayed neutrons. The derivation is given in the subsection 7.4.2.5 below.

$$\phi(t) = \frac{\frac{S}{\Sigma_a V} + \frac{1}{\Sigma_a} \sum_i \lambda_i C_i(t)}{1 - k(1 - \beta)} \quad (1)$$

The first term, $S/\Sigma_a V$ is constant. The second term represents the transient response of the delayed neutrons.

At steady-state, the ratio of the delayed neutron term to the source term is

$$\frac{\frac{1}{\Sigma_a} \sum_i \lambda_i C_i}{\frac{S}{\Sigma_a V}} = \frac{k\beta}{(1 - k)} \approx \frac{0.003}{(1 - k)} \quad (2)$$

Hence, for $k \lesssim 0.97$, which is the range of k during refueling, the delayed neutron term is much smaller (factor of 1/10 or less) than the source term. As k approaches unity so that $(1 - k)$ is of the order of β , the delayed neutron term becomes important.

7.4.2.5 Derivation of Subcritical Multiplication Equation

The space-independent one-velocity diffusion equation is:

$$\nu \Sigma_f (1 - \beta) \phi(t) + \sum_i \lambda_i C_i(t) + \frac{S}{V} - \Sigma_a \phi(t) = \frac{dn}{dt} \quad (3)$$

The nomenclature is conventional, except for S/V which is the neutron source per unit core volume in neutrons/cm³ sec. We relate n and ϕ and define lifetime ℓ and criticality factor k as follows:

$$nv = \phi \quad (4)$$

$$\ell = \frac{1}{\Sigma_a V} \quad (5)$$

$$k = \nu \Sigma_f / \Sigma_a \quad (6)$$

Dividing Equation (3) by Σ_a and using relations (4), (5), and (6) gives:

$$k(1 - \beta) \phi(t) + \frac{1}{\Sigma_a} \sum_i \lambda_i C_i(t) + \frac{S}{\Sigma_a V} - \phi(t) = \ell \frac{d\phi}{dt} \quad (7)$$

Neglecting the $\ell d\phi/dt$ term, which means that we assume the prompt neutrons respond instantaneously to a change in k —a valid assumption until prompt criticality is approached.

$$\phi(t) = \frac{\frac{S}{\Sigma_a V} + \frac{1}{\Sigma_a} \sum_i \lambda_i C_i(t)}{1 - k(1 - \beta)} \quad (8)$$

The delayed neutron precursor equation is

$$\frac{dC_i}{dt} = \nu \Sigma_f \beta_i \phi(t) - \lambda_i C_i(t) \quad (9)$$

at steady-state

$$\sum_i \lambda_i C_i = \nu \Sigma_f \beta \phi = \Sigma_a k \beta \phi \quad (10)$$

Substituting Equation (10) into (3) and solving for S/V as a function of steady-state flux and k gives

$$\frac{S}{V} = \Sigma_a (1 - k) \phi \quad (11)$$

Finally, we obtain the ratio of the steady-state delayed neutron contribution to the flux in Equation (8) to the source contribution by dividing Equation (10) by (11), or:

$$\frac{\frac{1}{\Sigma_a} \sum_i \lambda_i C_i}{\frac{S}{\Sigma_a V}} = \frac{k\beta}{1 - k} \quad (12)$$

7.4.3 Criticality Checks

Periodically during refueling, it may be desirable to make criticality checks by withdrawing a specified number of control rods. This would provide an assurance that the controlled core was sufficiently subcritical and, by comparing count rate N with changes in k due to control rod withdrawal, would provide a periodic check on the interpretation of the subcritical multiplication results.

The frequency with which criticality checks are needed and the number of rods which should be withdrawn at each check has not yet been specified. There are 127 fuel assemblies in the backup design. About a fourth of these will be replaced at each refueling. Since replacing (or replacing plus reshuffling) one assembly contributes well below 1\$ to the net reactivity of the core, there does not appear to be any clear reason for making frequent criticality checks. For boiling water reactors, criticality checks are made after about four fuel assembly replacements; but rod worths

and bundle worths are higher and vary more dramatically with geometry for light-water reactors than is the case for the FFTF backup fast reactor.

During FFTF refueling, 2% Δk (6\$—the fuel cycle Δk) will be added by replacing ~ 30 fuel assemblies. If it is decided, for example, that three criticality checks should be made

during refueling, a check would be made before loading each group of 10 assemblies, or before adding ~ 2\$ net reactivity. Each reflector control rod is worth ~ 1\$. Hence, withdrawal of about four control rods to check criticality, and then reinserting them before loading the next 10 assemblies, would provide added assurance that the next 10 assemblies loaded would not lead to criticality.

SECTION VIII

SHIELDING8.1 FFTF BACKUP DESIGN RADIAL
SHIELD OPTIMIZATION STUDY

An analysis was carried out to optimize the radial neutron shield design. Specific goals of this analysis were to:

- a. Determine the neutron shield composition which minimizes fast neutron fluence ($E_n > 1$ MeV) on the reactor vessel.
- b. Determine the neutron shield composition which minimizes the gamma heating in the concrete biological shield and the total radiation dose rate at the outer surface of this shield.
- c. Ascertain the "optimum" neutron shield composition after appropriately weighting the above results. Include recommendations from the reactor vessel and internals design group. Materials costs, availability, compatibility, and ease of manufacture must also be given appropriate consideration.

Assumptions of the study are:

- a. Thickness of the radial neutron shield is 45.7 cm (or 18 inches). It is located in sodium just external to the core radial reflector and core support structure.
- b. The shield composition is 30 volume percent sodium. The remaining 70% is composed of Type 304 stainless steel and B_4C . No minimum was set for the B_4C , but B_4C containment requires a minimum 10 volume percent stainless content.

Figure 8-1 illustrates the effect of reducing the B_4C content in the neutron shield on the fast neutron fluence, nvt ($E_n > 1$ MeV) at the reactor vessel. The fast neutron fluence is minimized by maximizing the stainless content of the neutron shield.* These results are summarized in Table 8-1, along with the fast neutron fluence for neutron energies in excess of 0.1 MeV. While the fast neutron limiting fluence has not been definitely established for FFTF at this time, the values indicated in Table 8-1 are well under a tentative limit of 1×10^{20} nvt ($E_n > 0.1$ MeV) recommended by PNL.⁽¹³⁾

In reference to the fluences having neutron energies in excess of 0.1 MeV, it is significant to note that maximizing the B_4C content in the neutron shield minimizes the neutron fluence; whereas, the opposite result occurs when considering only those neutrons having energies in excess of 1 MeV.

The final determination of an optimum composition must await recommendations

*Figure 8-1 indicates that the neutron flux in the region internal to the neutron shield varies as a result of varying the composition of the neutron shield. Also, in the case of the 60 volume percent B_4C neutron shield, this flux actually increases before entering the neutron shield. Both of these phenomena are believed in error and are probably caused by a deficiency in the calculational technique of the MAC removal-diffusion theory computer program. It is noted that all of these cases used a different number of mesh points in the neutron shield. Two check calculations were made for the 60 volume percent case whereby the number of mesh points was reduced from 1300 to 600 for the first case and to 30 for the second case. While the slopes in the region internal to the neutron shield did change to resemble those of Figure 8-1 the neutron fluxes greater than 1 MeV at outer edge of the neutron shield were essentially in agreement with that calculated for the 1300-point case. (However, negative thermal neutron flux values were noted at several points in the neutron shield for the 30-point case.)

FIGURE 8-1. FAST NEUTRON FLUENCE, $nvt(E_n > 1 \text{ MeV})$ VERSUS DISTANCE FROM THE CORE-REFLECTOR INTERFACE

TABLE 8-1

FFTF BACKUP DESIGN—FAST NEUTRON
FLUENCE, nvt, AT THE REACTOR VESSEL
VERSUS
NEUTRON SHIELD COMPOSITION

Assuming: Full Power 400 MWt
 Plant Lifetime 20 years
 Capacity Factor 0.75

Neutron Shield Composition			nvt	
<u>B₄C</u>	<u>Type 304 Stainless Steel</u>	<u>Na</u>	<u>E_n>1 MeV</u>	<u>E_n>0.1 MeV</u>
60	10	30	7.0×10^{17}	0.9×10^{19}
20	50	30	3.2×10^{17}	1.3×10^{19}
10	60	30	2.6×10^{19}	1.7×10^{19}
0	70	30	2.2×10^{17}	2.5×10^{19}

from the reactor vessel and internals design group, based on their analysis of cooling requirements. The following paragraphs include results of alpha, gamma, and neutron heating calculations in materials external to the FFTF backup design core in the radial direction.

8.2 FFTF BACKUP DESIGN RADIATION ABSORPTION HEATING VERSUS RADIAL DISTANCE FROM THE EDGE OF THE CORE

Heat generation data due to radiation absorption were calculated for the following uses:

- a. To determine coolant requirements inside the reactor vessel.
- b. To determine the nuclear heat generation rate in the radial shield material external to the reactor vessel.

c. To assist in the optimization of the neutron shield composition.

d. To evaluate the necessity for cooling a concrete biological shield.

These studies indicate that radiation heating is minimized by including 20 to 40 volume percent B₄C in the proposed 18-inch thickness neutron shield. They further indicate that, to match the maximum nuclear heat generation rate of 0.035 watts/cm² calculated by Battelle-Northwest in the radial shield material external to the reactor vessel for a typical conceptual arrangement, a minimum of 10 volume percent B₄C is required.

8.2.1 Methods and Discussion

The MAC shielding code was used to perform this analysis. The following types of energy deposition were included:

- a. Absorption of core fission and fission product decay gammas.

- b. Absorption of neutron capture gammas.
- c. Transfer of fast neutron kinetic energy due to elastic scattering events.
- d. Absorption of alpha particle energy in boron carbide.

It was necessary to supplement the MAC calculations with synthesized two-dimensional 16-neutron group diffusion theory calculations to calculate the latter two types of energy deposition.

Figure 8-2 illustrates total (alpha, gamma, and neutron) radiation heating rate in boron carbide as a function of radial distance from the core-reflector interface and neutron shield composition. This figure indicates that a 10 volume percent boron carbide neutron shield has lower radiation heating than either a 0 or 60 volume percent boron carbide neutron shield.

Figure 8-3 illustrates the same information for iron. However, there is no significant alpha particle absorption in iron, and as shown by Figure 8-8, neutron elastic scattering does not contribute significantly. Both figures assume a core power level of 400 MW thermal. Figure 8-3 indicates that radiation heating per gram of iron is minimized by 20 to 40 volume percent B_4C in the neutron shield. Higher percentages of B_4C in the neutron shield result in less attenuation of capture gamma sources from the reflector and support structure. However, no B_4C results in a high capture gamma source in the neutron shield itself because of relatively high thermal and epithermal neutron fluxes in the neutron shield.

All of these calculations assume homogeneous neutron shields. Some improvement in results could be expected by spatial rearrangements of shield compositions. Table 8-2 illustrates the shield compositions assumed for this analysis.

TABLE 8-2

ASSUMED RADIAL SHIELD COMPOSITIONS

Region	Thickness, cm	Material Composition, g/cm ³					
		B	C	Cr	Fe	Ni	Na
Reflector	18.72			0.292	0.762	5.746	0.185
Structure	10.00			0.753	2.775	0.436	0.425
Neutron Shield:							
No B_4C				1.055	3.886	0.611	0.255
10%	45.72	0.133	0.037	0.904	3.331	5.234	0.255
20%		0.266	0.074	0.753	2.776	0.436	0.255
40%		0.532	0.148	0.452	1.665	0.262	0.255
60%		0.798	0.222	0.151	0.555	0.087	0.255
Sodium Annulus	121.92						0.8512
Reactor Vessel	1.90			1.507	5.551	0.872	
Blast Shield	10.16				7.7		
Thermal Insulation	15.24			0.151	0.555	0.087	
Concrete Retainer	1.90				7.7

FIGURE 8-2. ALPHA, GAMMA, AND NEUTRON HEAT GENERATION RATE IN B₄C VERSUS
(1) RADIAL DISTANCE FROM CORE-REFLECTOR INTERFACE, AND (2)
NEUTRON SHIELD COMPOSITION

FIGURE 8-3. GAMMA HEATING IN Fe VERSUS (1) RADIAL DISTANCE FROM THE CORE-REFLECTOR INTERFACE, AND (2) NEUTRON SHIELD COMPOSITION

Figure 8-4 illustrates the manner in which alpha particle heating in boron carbide is expected to vary as a function of radial distance from the core-reflector interface. Figure 8-5 illustrates similar data as a function of axial height above the core mid-plane. Figure 8-6 illustrates gamma heating rates in boron carbide as a function of

- a. radial distance from the core-reflector interface, and
- b. B_4C content in the neutron shield.

Figure 8-7 illustrates the effect of varying the B_4C content in the neutron shield on gamma heating in the proposed concrete biological shield. More than a factor of 10 reduction occurs for the 60 volume percent B_4C shield as compared to no B_4C in the neutron shield. This data will be evaluated further to assess the necessity for cooling the concrete biological shield.

Figure 8-8 illustrates energy deposition in boron carbide due to the transfer of fast neutron kinetic energy by elastic scattering events. (The assumption is

made that neutron inelastic scattering energy is dissipated by the generation and absorption of gamma rays. It is not included since it is not expected to make a significant contribution to this data.)

It has been assumed in the calculation of the results shown on Figure 8-8 that elastic scattering heating external to the core is proportional to the neutron flux having energies in excess of 1 MeV. Figure 8-8 indicates that neutron elastic scattering heating in the vicinity of the proposed reflector control rods has a value in the range of 2 to 7 watts/gram of B_4C , as compared to 26 to 28 watts/gram due to alpha particle absorption, and 4 to 5 watts/gram due to gamma absorption.

Finally, as noted at the beginning of this section for the backup core design to replace the reference design, it would be desirable for the heat generation rate at the external vessel surface to be about the same for both designs. Table 8-3 illustrates the calculated heat generation rate for the different neutron shield compositions evaluated.

TABLE 8-3

FFTF BACKUP DESIGN, 400 MW THERMAL

Nuclear Heat Generation Rate at the External Reactor Vessel Surface

versus

Neutron Shield Composition

(Fission and Neutron Capture Gammas Only)

Neutron Shield Composition, Vol %			Heat Generation Rate	
B_4C	Type 304 Stainless Steel	Na	MAC ∞ Slab	1/r Correction
0	70	30	1.143 watts/cm ²	0.252 watts/cm ²
10	60	30	0.142	0.031
20	50	30	0.078	0.017
40	30	30	0.046	0.010
60	10	30	0.071	0.016

FIGURE 8-4. ALPHA PARTICLE HEATING IN B_4C VERSUS RADIAL DISTANCE FROM CORE-REFLECTOR INTERFACE

FIGURE 8-5. ALPHA PARTICLE HEATING IN B_4C VERSUS HEIGHT ABOVE CORE MIDPLANE

FIGURE 8-6. GAMMA HEATING IN BORON VERSUS (1) RADIAL DISTANCE FROM CORE-REFLECTOR INTERFACE, AND (2) NEUTRON SHIELD COMPOSITION

FIGURE 8-7. GAMMA HEATING IN MAGNETITE CONCRETE BIOLOGICAL SHIELD VERSUS (1) DISTANCE INTO BIOLOGICAL SHIELD, AND (2) VOLUME PERCENT B_4C IN NEUTRON SHIELD

FIGURE 8-8. NEUTRON ELASTIC SCATTERING HEATING VERSUS RADIAL DISTANCE FROM CORE CENTER

The heat generation in Table 8-3 neglects gamma heating due to the decay of Na-24 in the primary coolant. When assuming an equilibrium Na-24 decay activity of 2.64×10^9 disintegrations/cm³-sec, an energy absorption rate of 0.026 watts/cm² results (neglecting absorption in the reactor vessel). Total energy emission from a cylindrical surface 16 feet in diameter and 30 feet in height would be 36 kW.

A comparison of the Na-24 energy rate to the results illustrated in Table 8-3 indicates that little would be gained by any further reduction in the heat generation rate due to fission and neutron capture gammas. The results for neutron shields having B₄C compositions of 20 volume percent or more are also satisfactory in matching the heat generation data calculated by the Pacific Northwest Laboratory (0.035 watts/cm²).

SECTION IX

THERMAL-HYDRAULICS ANALYSIS9.1 TRANSIENT ANALYSIS SIMULATION

A dynamic simulation of the FFTF Backup Design is being developed. The dynamic equations will be solved on the digital computer. An understanding of the dynamic response of the backup design is necessary to resolve questions in the area of safeguards, design, and control.

The initial version of the simulation includes the core and intermediate heat exchanger (IHX), but does not include the air dump heat exchanger. For the fast transients to be examined initially, it is sufficient to hold the secondary inlet to the IHX constant and neglect the effect of the air dump heat exchanger on the secondary sodium.

The equations for the initial model are described below. Checkout of the reactor kinetics equations and the fuel rod temperature equations has been completed. Checkout of the primary sodium system equations is now in progress.

9.1.1 Description of the Model

The FFTF system out through the IHX is shown in Figure 9-1. Only one external loop is shown although there will be three or four in the FFTF. Two loops will be simulated; this can mockup either a three or four-loop system. The option is available to assume all loops are acting the same or one is acting differently.

Point kinetics is assumed for the core, and fuel temperatures are calculated only at the average power position. This simple core model is expected to simulate

adequately the temperature and reactivity-feedback responses in the core for system-dynamics related questions; the FORE code is available for more detailed questions related to the core.

Bypass flow around the core is provided. Outlet mixing is simulated by a simple time constant. The accurate evaluation of this time constant or development of a more adequate model is expected to require considerable analysis and experimental work in the FFTF development program.

Counter-current flow has been assumed for the IHX, with the primary sodium tubeside. All time delays in the sodium system are allowed to vary with varying flow rate.

The nomenclature is defined in Table 9-1.

9.1.2 Reactor Kinetics

For large reactivities, the exact point kinetics equation will be used. For reactivities small relative to 1β , a simplified form will be used which ignores the prompt neutron time dependence. When the exact equation is used, integration will be carried out in a special fast integration routine separate from the remainder of the system due to the small time step required.

The time dependence of that portion of the power controlled by fission product decay and buildup, P_d , must be treated separately from the prompt power, P_p , particularly for the cases of scram, flow coastdown, and shutdown. For the first 30 seconds of these transients, the time

FIGURE 9-1. FFTF FLOW DIAGRAM

TABLE 9-1

NOMENCLATURE

The following letters are used for system temperatures:

F	Fuel
T	Cladding, coolant in core and vessel
P	Primary in IHX
X	IHX tubes
S	Secondary in IHX

Subscripts—refer as follows:

c	Coolant in core (including sodium and structure)
c_ℓ	Cladding
d	Decay heat
di	ith time delay
D	Doppler coefficient
e	Fuel expansion
f	Fuel
g	Gap
i	ith delayed neutron group
m	mth IHX node
n	(a) nth fuel node (b) Power "produced" (proportional to neutron population)
Na	Sodium
o	Initial value
p	(a) Prompt power absorption (b) Primary side of IHX, primary flow
s	(a) Secondary side of IHX, secondary flow (b) Core structure
x	IHX tubes

Parameters:

a, b, c, d	Coefficients for forcing function (\$/sec)
AMF	Maximum or minimum forcing function worth (\$)
$A_{p, m}$	Heat transfer surface in mth node of IHX on primary side (ft ²)
$A_{s, m}$	Heat transfer surface in mth node of IHX on secondary side (ft ²)
b	Fraction of flow bypassing the core
c_c	Specific heat of coolant in core (including structure) (Btu/lb-°F)
$c_{c\ell}$	Specific heat of cladding (Btu/lb-°F)
c_f	Specific heat of fuel (Btu/lb-°F)
c_{Na}	Specific heat of sodium (Btu/lb-°F)
c_s	Specific heat of core structure (Btu/lb-°F)
C_i	Quantity proportional to delayed neutron precursor population (MW)

TABLE 9-1 (Continued)

f_i	Fraction of delayed neutrons in i th delay group
f_p	Fraction of power absorbed promptly
$f_{c,d}$	Fraction of decay heat absorbed in coolant and structure
$f_{c,p}$	Fraction of prompt power absorbed in coolant and structure
$f_{cl,d}$	Fraction of decay heat absorbed in cladding
$f_{cl,p}$	Fraction of prompt power absorbed in cladding
$f_{f,d}$	Fraction of decay heat absorbed in fuel
$f_{f,p}$	Fraction of prompt power absorbed in fuel
F	Average fuel temperature ($^{\circ}\text{F}$)
F_n	Fuel temperature in n th fuel node ($^{\circ}\text{F}$)
g, h, i, j	Coefficients for scram function ($\$/\text{sec}$)
h_c	Fuel rod-to-coolant heat transfer coefficient ($\text{Btu}/\text{h}\cdot\text{ft}^2\cdot^{\circ}\text{F}$)
h_g	Gap coefficient ($\text{Btu}/\text{h}\cdot\text{ft}^2\cdot^{\circ}\text{F}$)
$h_{p,m}$	Heat transfer coefficient on primary side of IHX ($\text{Btu}/\text{h}\cdot\text{ft}^2\cdot^{\circ}\text{F}$)
$h_{s,x}$	Heat transfer coefficient on secondary side of IHX ($\text{Btu}/\text{h}\cdot\text{ft}^2\cdot^{\circ}\text{F}$)
k_{cl}	Cladding conductivity ($\text{Btu}/\text{h}\cdot\text{ft}\cdot^{\circ}\text{F}$)
k_x	IHX tube conductivity ($\text{Btu}/\text{h}\cdot\text{ft}\cdot^{\circ}\text{F}$)
k_f	Fuel conductivity ($\text{Btu}/\text{h}\cdot\text{ft}\cdot^{\circ}\text{F}$)
K_c	Coolant and core structure reactivity coefficient ($\$/^{\circ}\text{F}$)
K_e	Fuel expansion reactivity coefficient ($\$/^{\circ}\text{F}$)
ℓ	Prompt neutron lifetime (sec)
L	Total length of IHX (ft)
M	Number of nodes in the IHX
MSW	Maximum scram worth ($\$$)
n	n th node in the fuel rod
N	Number of nodes in the fuel rod
P	Power absorption rate at time t (MW)
P_c	Power absorption rate in coolant and structure (MW)
P_{cl}	Power absorption rate in cladding (MW)
P_d	Decay heat absorption rate (MW)
P_f	Power absorption rate in fuel (MW)
P_m	Primary temperature at inlet of m th node of IHX ($^{\circ}\text{F}$)
P_n	Power "production" rate at time t (MW)
P_{n0}	Power "production" rate at time t_0 (MW)
P_p	Prompt power absorption rate (MW)
R_{cl}	Outside radius of cladding (ft)
R_f	Fuel pellet radius (ft)
S_m	Secondary temperature at outlet of m th node in IHX ($^{\circ}\text{F}$)
t	Time (sec)
t_{di}	i th delay time (sec)
T_c	Average coolant and structure temperature in the core ($^{\circ}\text{F}$)
T_{cl}	Cladding temperature ($^{\circ}\text{F}$)
T_1-T_{11}	Sodium temperatures in the vessel (Figure 9-1) ($^{\circ}\text{F}$)
$(\frac{dk}{dT})_D$	Doppler coefficient (absolute units of reactivity and temperature)

TABLE 9-1 (Continued)

V_{core}	Core volume (ft ³)
$V_{p,m}$	Primary volume in one node of the IHX (ft ³)
$V_{s,m}$	Secondary volume in one node of the IHX (ft ³)
$V_{x,m}$	IHX tube volume in one node of the IHX (ft ³)
V_c	Volume fraction of coolant and structure
V_f	Volume fraction of fuel
V_{Na}	Volume fraction of sodium
V_s	Volume fraction of structure
y_i	Delayed neutron precursor parameter (MW sec)
β_{eff}	Effective delayed neutron fraction (value of the dollar)
δ	IHX tube wall thickness (ft)
δ_ℓ	Option to allow for different flow rates between loops
δ_{wp}	Option to allow for variable primary flow rate in IHX
Δk	Net reactivity (\$)
Δk_{TEST}	Test reactivity for decision about which power equation to be used (\$)
Δr	Width of fuel node (all nodes of constant width) (ft)
Δx	Cladding thickness (ft)
λ_i	Decay constant for ith delayed neutron group (sec ⁻¹)
ρ_c	Coolant and structure effective density (lb/ft ³)
ρ_{cl}	Cladding density (lb/ft ³)
ρ_f	Fuel density (lb/ft ³)
ρ_{Na}	Sodium density (lb/ft ³)
ρ_s	Structure density (lb/ft ³)
τ_d	Decay heat time constant (sec)
$\tau_{n-1,n}$	Fuel node time constant for heat transfer from the (n-1) to the nth node (sec)
$\tau_1-\tau_{19}$	Time constants (sec) used as follows:

Time Constant	For Heat Transfer Between a and b		For Use in Calculation of Temperature of:
	a	b	
τ_1	Last fuel node	Cladding	Last fuel node, F_N
τ_2	Last fuel node	Cladding	Cladding, T_{cl}
τ_3	Cladding	Coolant	Cladding, T_{cl}
τ_4	Cladding	Coolant	Coolant, T_c
τ_5	Coolant	Core outlet	Coolant, T_c
τ_6	Outlet mixing		Vessel outlet, T_4
τ_7	Inlet mixing		Core inlet, T_6
τ_8	Across IHX primary node		IHX primary, P_m
τ_9	IHX primary	IHX tubes	IHX primary, P_m
τ_{10}	IHX primary	IHX tubes	IHX tubes, X_m
τ_{11}	IHX tubes	IHX secondary	IHX tubes, X_m

TABLE 9-1 (Continued)

Time Constant	For Heat Transfer Between a and b		For Use in Calculation of Temperature of:
	a	b	
τ_{12}	IHX tubes	IHX secondary	IHX secondary, S_m
τ_{13}	Across IHX secondary node		IHX secondary, S_m
τ_{14}	T_1	T_7	Coolant, T_7
τ_{15}	T_7	T_8	Coolant, T_7
τ_{16}	T_7	T_8	Reflector tube, T_8
τ_{17}	T_9	T_{10}	Coolant, T_{10}
τ_{18}	T_{10}	T_{11}	Coolant, T_{10}
τ_{19}	T_{10}	T_{11}	Axial shield tube, T_{11}

dependence of the decay heat relative to the prompt power can be approximated closely by a simple 30 second time constant, i. e., the decay heat lags the prompt power by this time constant. To follow a flow coastdown beyond 30 seconds, tabulated values of decay heat versus time or a single $t^{-1.2}$ variation will be used.

We distinguish between power "produced" at time t , P_n , (i. e., power that ultimately will be produced due to events occurring at time t), and power absorbed at time t , P . The fraction of P_n which is absorbed promptly at time t is f_p ; hence $(1-f_p)P_n$ is "produced" at t but is absorbed later.

Based on the discussion and definitions in the above three paragraphs, we can write the kinetics equations as follows:

If $\Delta k \geq \Delta k_{TEST}$

$$\frac{dP_n}{dt} = \frac{\beta_{eff}}{\ell} (\Delta k - 1) P_n + \frac{\beta_{eff}}{\ell} \sum_i \lambda_i y_i \quad (13)$$

If $\Delta k < \Delta k_{TEST}$

$$P_n = \frac{\sum_i \lambda_i y_i}{1 - \Delta k} \quad (14)$$

$$\frac{dy_i}{dt} = f_i P_n - \lambda_i y_i \quad (15)$$

where

$$y_i = \frac{\ell}{\beta_{eff}} C_i \text{ (and } C_i \text{ is proportional to delayed neutron precursor population)}$$

$$P_p = f_p P_n \quad (16)$$

$$\frac{dP_d}{dt} = \frac{1}{\tau_d} [(1-f_p) P_n - P_d] \quad (17)$$

A tabular value for P_d versus time can be used after 30 seconds if desired.

$$P = P_p + P_d \quad (18)$$

Reactivity Equations:

$$\Delta k = \Delta k_{\text{input}} + \Delta k_{\text{control}} + \Delta k_{\text{feedback}} \quad (19)$$

$$\Delta k_{\text{feedback}} = \Delta k_{\text{Doppler}} + \Delta k_{\text{fuel expansion}} + \Delta k_{\text{coolant}} \quad (20)$$

$$\Delta k_{\text{Doppler}} = \left(T \frac{dk}{dT} \right)_D \ln \frac{F_f + 460}{F_{fo} + 460} \quad (21)$$

$$\Delta k_{\text{fuel expansion}} = K_e (F_f - F_{fo}) \quad (22)$$

$$\Delta k_{\text{coolant}} = K_c (T_c - T_{co}) \quad (23)$$

$$\Delta k_{\text{input}} = a + bt + ct^2 + dt^3 \text{ up to a maximum value specified by the user} \quad (24)$$

$$\Delta k_{\text{control}} = \alpha (t - t_o) + \beta (t - t_o)^2 + \gamma (t - t_o)^3 \text{ up to a maximum value specified by the user.} \quad (25)$$

Scram can start at a specified value of t_o or at a time when a specified overpower is reached.

9.1.3 Fuel, Cladding, and Coolant Temperatures

9.1.3.1 Fuel

The following equation is solved for fuel temperature, F , in a finite difference approximation.

$$\rho_f c_f \frac{\partial F}{\partial t} = k_f \nabla^2 F + \frac{P_f}{V_{\text{core}} V_f} \quad (26)$$

The fuel pellet is divided into $N+1$ nodes, each of the same width, equal to $R_f/(N+1)$, as shown in Figure 9-2. Four fuel nodes ($N=3$) have been selected for the initial simulation. Nodes are numbered 0 through N . The resulting transformation of the Equation (26) into finite difference form is given below for the fuel nodes followed by similar equations for the cladding and coolant.

The thermal conductivity, k_f , of the fuel is described by the equation

$$k_{f,n} = A + \frac{B}{\frac{F_n + F_{n-1}}{2} - 32} \quad (27)$$

The heat transfer coefficient between the cladding and coolant, h_c , is proportional to the 0.8 power of the sodium mass flow rate. It will be assumed that all other physical and heat transfer parameters in the fuel, gap, and cladding are constant. A choice will be available to the user whether to hold the cladding-to-coolant heat transfer coefficient, h_c , constant or to allow h_c to vary with coolant flow. Changes in structure temperature will be assumed to follow the sodium instantaneously, and the heat capacity of the structure will be added to that of the coolant.

The power absorbed in the fuel is given by

$$P_f = P_{pf,p} + P_{df,d} \quad (28)$$

For node n , $0 \leq n \leq N-1$, Equation (26) written in finite difference form is

$$\frac{dF_n}{dt} = \frac{1}{\tau_{n-1,n}} (F_{n-1} - F_n) - \frac{1}{\tau_{n,n+1}} \frac{(F_n - F_{n+1})}{(F_n - F_{n+1})} + \frac{P_f}{\rho_f c_f V_{\text{core}} V_f} \quad (29)$$

FIGURE 9-2. FUEL PELLETT MODEL

where

$$\tau_{n-1,n} = \frac{\rho_f c_f}{k_{f,n}} \frac{(2n+1)R_f^2}{2n(N+1)^2} \quad (30)$$

$$\tau_{n,n+1} = \frac{\rho_f c_f}{k_{f,n+1}} \frac{(2n+1)R_f^2}{2(n+1)(N+1)^2} \quad (31)$$

and the first term on the right does not exist for $n=0$.

For $n=N$,

$$\begin{aligned} \frac{dF_N}{dt} = \frac{1}{\tau_{N-1,N}} (F_{N-1} - F_N) - \frac{1}{\tau_1} (F_N - T_{cl}) \\ + \frac{P_f}{\rho_f c_f V_{core} V_f} \end{aligned} \quad (32)$$

where

$$\tau_{N-1,N} = \frac{\rho_f c_f}{k_f} \frac{(2N+1)R_f^2}{2N(N+1)^2} \quad (33)$$

$$\begin{aligned} \tau_1 = \rho_f c_f \frac{(2N+1)R_f^2}{(N+1)^2} \left[\frac{1}{k_f(4N+3)} + \frac{1}{2h_g R_f} \right. \\ \left. + \frac{\Delta x}{k_{cl}(4R_{cl} - 3\Delta x)} \right] \end{aligned} \quad (34)$$

Average fuel temperature, F_f

$$F_f = \sum_{n=0}^N F_n \frac{2n+1}{(N+1)^2} \quad (35)$$

Cladding: The power absorbed in the cladding is:

$$P_{cl} = P_{pf_{cl},p} + P_{df_{cl},d} \quad (36)$$

The cladding temperature follows the equation

$$\begin{aligned} \frac{dT_{cl}}{dt} = \frac{1}{\tau_2} (F_N - T_{cl}) - \frac{1}{\tau_3} (T_{cl} - T_c) \\ + \frac{P_{cl}}{\rho_{cl} c_{cl} V_{core} V_{cl}} \end{aligned} \quad (37)$$

where

$$\tau_2 = \rho_{cl} c_{cl} (2R_{cl} \Delta x - \Delta x^2)$$

$$\left[\frac{1}{k_f(4N+3)} + \frac{1}{2h_g R_f} + \frac{\Delta x}{k_{cl}(4R_{cl} - 3\Delta x)} \right] \quad (38)$$

$$\tau_3 = \rho_{cl} c_{cl} (2R_{cl} \Delta x - \Delta x^2)$$

$$\left[\frac{\Delta x}{k_{cl}(4R_{cl} - \Delta x)} + \frac{1}{2h_c R_{cl}} \right] \quad (39)$$

Coolant: The power absorbed in the coolant is

$$P_c = P_{pf_c,p} + P_{df_c,d} \quad (40)$$

The coolant temperature is described by the equation

$$\frac{dT_c}{dt} = \frac{1}{\tau_4} (T_{cl} - T_c) - \frac{1}{\tau_5} (T_c - T_6)$$

$$+ \frac{P_c}{\rho_c c_c V_{core} V_c} \quad (41)$$

where

$$\tau_4 = \rho_c c_c R_f^2 \frac{V_c}{V_f} \left[\frac{\Delta x}{k_{cl}(4R_{cl} - \Delta x)} + \frac{1}{2h_c R_{cl}} \right] \quad (42)$$

$$\tau_5 = \frac{\rho_c c_c V_{\text{core}} V_c}{2(1-b) w_c c_{\text{Na}}} \quad (43)$$

$$w_c = \sum_i w_{p,i} \quad (44)$$

where $w_{p,i}$ is the flow in each loop and the summation is over all loops.

The heat capacity of the structure is added to the heat capacity of the coolant by means of Equations (45) and (46).

$$V_c = V_s + V_{\text{Na}} \quad (45)$$

$$\rho_c c_c V_c = \rho_s c_s V_s + \rho_{\text{Na}} c_{\text{Na}} V_{\text{Na}} \quad (46)$$

1-b = fraction of flow through core (b = bypass flow fraction)

The outlet coolant temperature (T_1) is calculated from the inlet (T_6) and the average (T_c):

$$T_1 = 2T_c - T_6 \quad (47)$$

9.1.4 Axial Reflector, Gas Plenum, and Axial Shield Region above the Core

It is assumed that the heat transfer first to the axial reflector and gas plenum and second to the neutron shield above the core can be described by simple time constant equations. We assume one temperature node in the reflector or shield rod and that it is located at 2/3 of the rod radius. This allows an approximation of the resistance to heat transfer from the rod to the coolant in a manner similar to the fuel. Energy absorption in the rods is neglected.

We write the equations for the upper reflector and gas plenum in terms of average sodium and rod temperatures (T_7 and T_8 respectively) as follows

$$\frac{dT_7}{dt} = \frac{1}{\tau_{14}} (T_1 - T_7) - \frac{1}{\tau_{15}} (T_7 - T_8) \quad (48)$$

$$\frac{dT_8}{dt} = \frac{1}{\tau_{16}} (T_7 - T_8) \quad (49)$$

where

$$\tau_{14} = \frac{\rho_{\text{Na}} c_{\text{Na}} V_{\text{plenum}} V_{\text{Na}}}{2(1-b) w_c c_{\text{Na}}} \quad (50)$$

$$\tau_{15} = \rho_{\text{Na}} c_{\text{Na}} R_{c\ell}^2 \frac{V_{\text{Na}}}{V_{\text{steel}}} \left(\frac{1}{5k_s} + \frac{1}{2h_c R_{c\ell}} \right) \quad (51)$$

$$\tau_{16} = \rho_s c_s R_{c\ell}^2 \left(\frac{1}{5k_s} + \frac{1}{2h_c R_{c\ell}} \right) \quad (52)$$

We have assumed that the radius of the reflector and plenum rods are equal to the fuel rod radius, that the reflector and plenum rods are steel, and that the sodium volume fraction is the same as in the core. The steel volume fraction accounts for the reflector rods, plenum cladding, and channel walls.

The outlet temperature from the reflector-gas plenum region is

$$T_9 = 2T_7 - T_1 \quad (53)$$

The equations for the shield region are written in terms of average sodium and shield

rod temperatures (T_{10} and T_{11} , respectively) as follows

$$\frac{dT_{10}}{dt} = \frac{1}{\tau_{17}} (T_9 - T_{10}) - \frac{1}{\tau_{18}} (T_{10} - T_{11}) \quad (54)$$

$$\frac{dT_{11}}{dt} = \frac{1}{\tau_{19}} (T_{10} - T_{11}) \quad (55)$$

where

$$\tau_{17} = \frac{\rho_{Na} c_{Na} V_{shield} V_{Na, shield}}{2(1-b) w_c c_{Na}} \quad (56)$$

$$\tau_{18} = \rho_{Na} c_{Na} R_{shield}^2 \frac{V_{Na, shield}}{V_{steel, shield}} \left(\frac{1}{5k_s} + \frac{1}{2h_c R_{shield}} \right) \quad (57)$$

$$\tau_{19} = \rho_s c_s R_{shield}^2 \left(\frac{1}{5k_s} + \frac{1}{2h_c R_{shield}} \right) \quad (58)$$

The outlet temperature from the shield region is

$$T_{12} = 2T_{10} - T_9 \quad (59)$$

9.1.5 Inlet Mixing, Outlet Mixing, and Bypass Flow

9.1.5.1 Outlet Mixing

Above the neutron shield the sodium from the core and from the bypass flow mixes with part of the reservoir of sodium in the upper part of the vessel. We assume a delay time t_{d1} from the shield outlet to the mixing reservoir equivalent to the time

for sodium to travel from the core to the mixing reservoir. The bypass flow is at the temperature of the core inlet at a delay time t_{d2} ago. Hence, the temperatures of the two sodium streams to be mixed with the sodium in the reservoir are

$$T_2 = T_{12} \text{ at } t - t_{d1} \text{ (sodium from shield and core)} \quad (60)$$

$$T_3 = T_6 \text{ at } t - t_{d2} \text{ (bypass sodium)} \quad (61)$$

The total pounds of sodium in the reservoir above the core level is M_a . It is assumed that a fraction f_m of this is mixed with the core and bypass sodium.

For the model described, the temperature of the sodium leaving the vessel, T_4 , is described by the equation

$$\frac{dT_4}{dt} = \frac{1}{\tau_6} \left[(1-b) T_2 + bT_3 - T_4 \right] \quad (62)$$

where

$$\tau_6 = f_m M_a / w_c \quad (63)$$

and w_c is defined by Equation (44).

9.1.5.2 Inlet Mixing

The vessel inlet temperature, T_5 , follows the outlet primary sodium temperature, P_{M+1} , of the IHX by a delay time t_{d4} . Hence,

$$T_5 = P_{M+1} \text{ at } t - t_{d4} \quad (64)$$

Mixing of all inlet streams occurs below the core in a mixing plenum which contains M_p

pounds of sodium. The core inlet, T_6 , is given by the following equation

$$\frac{dT_6}{dt} = \frac{1}{\tau_7} \left(\frac{1}{w_c} \sum_i T_{5,i} w_{p,i} - T_6 \right) \quad (65)$$

where

$$\tau_7 = M_b / w_c \quad (66)$$

9.1.6 Intermediate Heat Exchanger

The equations for a typical loop are given below. The equation for the primary temperatures, P , in a counter-current heat exchanger is

$$\rho_{Na} c_{Na} V_p \frac{\delta P(x,t)}{\delta t} = -w_p c_{Na} L \frac{\delta P(x,t)}{\delta x} - h_{p,x} A_p [P(x,t) - X(x,t)] \quad (67)$$

where

- V_p = total primary volume in IHX
- L = total length of IHX
- A_p = total heat transfer area on primary side of IHX
- h = heat transfer coefficient which is proportional to the 0.8 power of the sodium flow rate.

The spatial variation is approximated in difference equation form by dividing the IHX into M modes of equal volume, as shown in Figure 9-1.

Primary—The primary inlet temperature follows the vessel outlet by a delay time t_{d3} . Hence,

$$P_1 = T_4 \text{ at } t - t_{d3} \quad (68)$$

for $2 \leq m \leq M$

$$\frac{dP_m}{dt} = \frac{1}{2\tau_8} (P_{m-1} - P_{m+1}) - \frac{1}{\tau_9} (P_m - X_m) \quad (69)$$

$$\tau_8 = \frac{\rho_{Na} c_{Na} V_{p,m}}{w_p c_{Na}} \quad (70)$$

$$\tau_9 = \rho_{Na} c_{Na} V_{p,m} \left[\frac{1}{h_{p,x} A_{p,m}} + \frac{\delta/2}{h_x A_{p,m}} \right] \quad (71)$$

For the last node (primary IHX outlet), $m = M+1$

$$\frac{dP_{M+1}}{dt} = \frac{1}{2\tau_8} (-3P_{M+1} + 4P_M - P_{M-1}) - \frac{1}{\tau_9} (P_{M+1} - X_{M+1}) \quad (72)$$

IHX Tubes—For all nodes, $1 \leq m \leq M+1$

$$\frac{dX_m}{dt} = \frac{1}{\tau_{10}} (P_m - X_m) - \frac{1}{\tau_{11}} (X_m - S_m) \quad (73)$$

$$\tau_{10} = \rho_x c_x V_{x,m} \left[\frac{1}{h_{p,x} A_{p,m}} + \frac{\delta/2}{k_x A_{p,m}} \right] \quad (74)$$

$$\tau_{11} = \rho_x c_x V_{x,m} \left[\frac{1}{h_{s,x} A_{s,m}} + \frac{\delta/2}{k_x A_{s,m}} \right] \quad (75)$$

Secondary—For $2 \leq m \leq M$

$$\frac{dS_m}{dt} = \frac{1}{\tau_{12}} (X_m - S_m) - \frac{1}{2\tau_{13}} (S_{m-1} - S_{m+1}) \quad (76)$$

$$\tau_{12} = \rho_{Na} c_{Na} V_{s, m} \left[\frac{1}{h_{s, x} A_{s, m}} + \frac{\delta/2}{k_x A_{s, m}} \right] \quad (77)$$

$$\tau_{13} = \frac{\rho_{Na} c_{Na} V_{s, m}}{w_s c_{Na}} \quad (78)$$

For the secondary outlet ($m = 1$),

$$\frac{dS_1}{dt} = \frac{1}{\tau_{12}} (X_1 - S_1) + \frac{1}{2\tau_{13}} (-3S_1 + 4S_2 - S_3) \quad (79)$$

The secondary inlet ($m = M+1$) follows the outlet temperature of the air dump heat exchanger by a delay time. However, for the present model which does not include the air dump heat exchanger, the secondary inlet will be held constant.

9.1.7 Time Delays and Flow Coastdown Characteristics

For constant flow rates, time delays are constant. For problems involving flow coastdown or transition to natural convection, variation in flow rates results in variable time delays.

For flow rate $w_1(u)$ between times t_A and t_B , and flow rate $w_2(u)$ between times t_b and t , and for a pipe containing M pounds of sodium, the time delay at t is obtained by solving the following equation for t_A

$$M = \int_{t_A}^{t_b} w_1(u) du + \int_{t_b}^t w_2(u) du \quad (80)$$

There are three possible forms of Equation (80) which will satisfy M

$$M = \int_{t_A}^t w_1(u) du \quad (81)$$

where

$$t_A < t < t_B;$$

$$M = \int_{t_A}^{t_B} w_1(u) du + \int_{t_B}^t w_2(u) du \quad (82)$$

where

$$t > t_B \text{ and}$$

$$\int_{t_B}^t w_2(u) du < M;$$

$$M = \int_{t_A}^t w_2(u) du \quad (83)$$

where

$$t > t_B \text{ and}$$

$$\int_{t_B}^t w_2(u) du \geq M.$$

For constant flow rate w_0 , the time delay follows from Equation (81);

$$t_d = \frac{M}{w_0} \quad (84)$$

During the early part of a flow coastdown which begins at t_B , Equation (82) must be used, where $w_1(u) = w_0$ and $w_2(u)$ is the flow coastdown flow rate. When t becomes sufficiently large, so that $t_d = t - t_A \geq t - t_B$, Equation (83) will be used.

9.1.8 Time Delay for Specific Pump Coastdown Characteristics

It was found that the flow coastdown characteristics of the EBR-II and Fermi

pumps could be closely described by the relation

$$\frac{w}{w_o} = 1 + \frac{1}{at + bt^2} \quad (85)$$

where w_o is the flow rate before coastdown.

This relationship for w/w_o will be used in the FFTF backup simulation.

Consider the case for which Equation (83) can be used to calculate t_d , so that $t_d = t - t_A$. Substituting Equation (85) into Equation (83) and solving for t_A gives

$$t_A = \frac{2bt\sqrt{4b-a^2} + a\sqrt{4b-a^2} - (4b-a^2) \tan\left(\frac{LA\rho\sqrt{4b-a^2}}{2w_o}\right)}{4b^2 \tan\left(\frac{LA\rho\sqrt{4b-a^2}}{2w_o}\right) + 2ab \tan\left(\frac{LA\rho\sqrt{4b-a^2}}{2w_o}\right) + 2b\sqrt{4b-a^2}} - \frac{a}{2b} \quad (86)$$

where

L = length of pipe, ft

A = cross-sectional area of pipe, ft^2

ρ = sodium density, lb/ft^3

NOTE: Equation (86) is valid for $4b-a^2 > 0$.

For $4b-a^2 < 0$ one must use a somewhat different form involving $\sqrt{a^2-4b}$.

9.2 PARAMETER STUDY

To permit an evaluation of the effect on nuclear characteristics of variations in core geometry parameters, a study was performed in which the pressure drop versus pitch-to-diameter ratio was determined for two pin sizes, two core heights and two cladding thicknesses. A 100 psi plenum-to-plenum pressure drop was selected as a reference and the resulting geometries defined the input to the physics studies (see Section VII).

9.2.1 Analysis

The variations in core pressure drop (plenum-to-plenum) and core linear power

for FFTF were investigated as functions of core height, fuel pin diameter, fuel cladding thickness, and pitch/diameter ratio.

The plenum-to-plenum pressure drop is calculated for the "hot" fuel bundle shown in Figure 9-3. Two driver fuel pin lengths of 36 inches and 33 inches with gas plenum lengths equal to fuel pin lengths are investigated. The wire spacers have a pitch of 1 wrap per foot. The sodium coolant velocity through the core is shown in Figure 9-4.

The reactor power distribution has 95% of the rated power (400 MWt) generated by the core. The linear power results for the following conditions are shown in Figure 9-5. Only the results for the 36-inch fuel pins are shown as fuel linear power did not change noticeably for the 33-inch pins. The fuel pellet density is 95% of theoretical and the pellet-to-cladding radial gap is 0.002 inch. The melt temperature for new fuel is taken as 5200° F. The curve of the $\int Kd\theta$ is shown in Figure 9-6. The axial and radial peak-to-average power factors are 1.20 and 1.40, respectively, and the core overpower factor is 1.20. The peaking factor due to all

FIGURE 9-3. DRIVER FUEL ASSEMBLY

FIGURE 9-4. VELOCITY OF SODIUM COOLANT THROUGH THE FUEL BUNDLE

FIGURE 9-5. VARIATION OF (a) PEAK LINEAR POWER AND (b) AVERAGE LINEAR POWER WITH PITCH/DIAMETER RATIO

FIGURE 9-6. INTEGRAL OF $Kd\theta$

secondary effects is 1.09. The overall peak-to-average ($1.2 \times 1.4 \times 1.2 \times 1.09$) is 2.19.

The plenum-to-plenum pressure drop is a composite of fuel, reflector, and entrance-exit region pressure drop. The pressure loss inventory may be summarized as follows:

- a. Expansion loss from circular hydraulic holddown area to the hexagonal fuel bundle area.
- b. Entrance and exit losses in the axial blankets.
- c. Frictional losses in the axial blanket.
- d. Entrance and exit losses to fuel and gas plenum section.
- e. Frictional losses in fuel and gas plenum section (including wire wrap losses).

Figure 9-7 illustrates the complete inventory items (a) through (e). Figure 9-8 presents the frictional, entrance, and exit losses for the fuel and gas plenum sections only [items (d) and (e)].

9.3 DECAY HEAT REMOVAL— NATURAL CIRCULATION

9.3.1 Introduction

An analysis has been performed to establish the sodium temperatures in the system which result from carrying the decay heat of the fuel away by natural circulation to the main intermediate heat exchangers, with a 25-foot head between centers of core and IHX. Since the decay heat load drops very rapidly during the first few minutes after scram, this initial time interval is where assurance of adequate core cooling is critical.

The maximum cladding temperature during emergency cooling conditions following scram should not be greater than full power conditions.

Peak cladding temperature during full power operation has been estimated at 1200° F for 1000° F core bulk outlet temperature. Under emergency cooling conditions where the ΔT through the cladding and local hot spot will be small, the limit of 1200° F on the cladding will require the channel outlet temperature to be less than 1100° F. This permits about 50° F for tolerance and variations associated with orificing and 50° F for the effect of the radial power gradient.

The present analysis is quasistatic in nature and does not investigate the transient behavior of the fuel and coolant during the pump coastdown period following the scram. A detailed analysis using realistic flow coastdown characteristics is required to establish the core outlet temperature during the first minute. Figure 9-9 shows that with no forced convection and steady conditions that the 1100° F limit suggested above is attained about 45 sec after scram, and 10 minutes after scram the decay heat can be removed with a 200° F core ΔT .

9.3.2 Method of Analysis

Let

Q = Heat generated in core

ρ = Coolant density

C_p = Coolant specific heat

V = Coolant velocity

$T_2 - T_1$ = Temperature rise through the core

Suffix '0' = 100% power condition

ΔP = System pressure drop

$\Delta P_c = \Delta P_{\text{core}}$, ΔP_p = pressure drop in pipes and IHX

$T_3 - T_1$ = Temperature drop through IHX

FIGURE 9-7. TOTAL PRESSURE LOSS FROM PLENUM TO PLENUM

PINS PER BUNDLE = 127
CLADDING THICKNESS = 0.015 inch

FUEL LENGTH (a) 36 inch TOTAL PIN LENGTH
(b) 33 inch (a) 84 inch
(b) 78 inch

○ PIN o.d. = 0.21 inch
△ PIN o.d. = 0.25 inch

FIGURE 9-8. PRESSURE LOSS FROM FRICTIONAL, ENTRANCE, AND EXIT EFFECTS FOR FUEL AND GAS PLENUM SECTION ONLY

FIGURE 9-9. FUEL SECTION EXIT COOLANT AVERAGE TEMPERATURE AFTER REACTOR SHUTDOWN

At steady state 100% power

assuming $\rho_o/\rho \approx 1.0$

$$T_1 = 700^\circ \text{F}$$

$$T_2 = T_3 = 1000^\circ \text{F}$$

Heat removal in core is given by

$$Q = \rho A V C_p (T_2 - T_1)$$

or

$$V = \frac{Q}{\rho A C_p (T_2 - T_1)}$$

Ratio at N.C. to 100% power

$$\frac{V_c}{V_{o_c}} = \frac{Q}{Q_o} \frac{\rho_o}{\rho} \frac{(T_2 - T_1)_o}{(T_2 - T_1)}$$

$$\frac{V_p}{V_{o_p}} = \frac{Q}{Q_o} \frac{\rho_o}{\rho} \frac{(T_3 - T_1)_o}{(T_3 - T_1)}$$

$$\Delta P = \Delta P_{o_c} \left[\frac{Q}{Q_o} \frac{(T_2 - T_1)_o}{(T_2 - T_1)} \right]^{1.8}$$

$$+ \Delta P_{p_o} \left[\frac{Q}{Q_o} \frac{(T_3 - T_1)_o}{(T_3 - T_1)} \right]^{1.8}$$

$$(T_2 - T_1)_o = (T_3 - T_1)_o = 300$$

Assume T_1 is constant at 700, $\Delta P_o = 110$,
 $\Delta P_{p_o} = 43$

$$\Delta P = \left(\frac{Q}{Q_o} \right)^{1.8} \left\{ \left[\left(\frac{300}{T_2 - 700} \right)^{1.8} \Delta P_{o_c} \right. \right.$$

$$\text{assuming } \frac{\Delta P_c}{\Delta P_{o_c}} = \left(\frac{V_c}{V_{c_o}} \right)^{1.8} \text{ and } \frac{\Delta P_p}{\Delta P_{p_o}} = \left(\frac{V_p}{V_{p_o}} \right)^{1.8} \left. + \left(\frac{300}{T_3 - 700} \right)^{1.8} \Delta P_{p_o} \right\}$$

FIGURE 9-10. NIKURADSE'S SAND-ROUGHENED-PIPE TESTS

The driving head available for natural circulation is

$$H = (\rho_1 - \rho_2)h_1 + (\rho_1 - \rho_3)h_2$$

$$= -\frac{d\rho}{dT} (T_2 - T_1)h_1 + (T_3 - T_1)h_2$$

$$\frac{d\rho}{dT} = 0.0083 \text{ lb/ft}^3 \cdot ^\circ\text{F} \text{ for sodium.}$$

Equating H and ΔP permits T_2 to be calculated after T_3 is inserted.

In the time interval of interest immediately after scram T_3 is assumed = $T_{30} = 1000^\circ\text{F}$ and held constant since the large plenum above the core damps any tendency for this to rise.

In the following assumptions, if T_3 would be less than T_2 , it is conservatively taken as equal to T_2 .

9.3.3 Discussion of Assumptions

a. $\Delta P \propto V^{1.8}$

This presumes that most of the pressure drop is due to friction and the friction factor $f \propto \frac{1}{(\text{Re})^{0.2}}$

$\propto \frac{1}{V^{0.2}}$. The pressure drops which are not due to frictional drag are proportional to V^2 and are small, hence the validity of the assumption hinges on the relationship between friction factor and Reynolds Number versus velocity. After reviewing the sodium temperature calculations for this particular problem it was determined that turbulent flow is maintained until the heat generation in the core is down to the level that the heat can be removed with 100°F temperature difference between the hot and cold legs which gives ample margin to allow for the increase in friction factor in the laminar regime. Figure 9-10 shows

the standard f versus R_e curves from which the smooth surface line A-C was used.

b. Inlet temperature is constant at 700° F:

For the time interval of major concern immediately after shutdown, the time constant of flow around the system (97 sec from IHX to core inlet at full flow) is sufficiently long that reduction of inlet temperature during the first few minutes is not attainable by regulation in the secondary system.

c. Decay heat energy release:

The upper curve of Figure 9-11 was used in the calculations. This is based upon a delayed neutron contribution of one-half that of

thermal reactors which is considered conservative. Comparison with the delay groups used on SEFOR showed a reduction (see Figure 9-11) which would result in the T_2 at 15 sec on Figure 9-9 being depressed from 1235° to 1200° F, which translates into a corresponding additional margin of safety on cladding temperature of 25° to 35° F.

d. Pressure Drop:

Initial pressure drops of 110 psi through the core and 43 psi through the piping system are considered reasonable design levels. The 43 psi includes the losses that would occur in a nonrotating pump so that ratioing down to the natural circulation conditions is valid.

FIGURE 9-11. ENERGY RELEASE RATE FROM FISSION-PRODUCT DECAY

FIGURE 9-12. REYNOLDS NUMBER VERSUS TIME

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SECTION X

SAFEGUARDS

During the quarter, a review was made of the safeguards' criteria prepared by PNL for several subsystems of the reference design. Comments on these have been or are in the process of being transmitted to PNL.

The specific documents reviewed were:

"Design Safety Criteria for Reactor Systems," (Systems 31, 32, and 33), by D. Simpson, J. Hagan, August 30, 1967

(Refer to "Comments on Design Safety Criteria," D.R. Riley to W.B. McDonald, October 6, 1967)

"Design Safety Criteria for the Emergency Cooling System" by R.D. Peak, August 14, 1967

"Reactivity Worth Criteria for FFTF Control and Safety Elements," D. Simpson, August 21, 1967

A request was made following the first of the above reviews and a meeting was held between G.E. and PNL personnel to review reactivity insertion accidents during refueling. The major concern is, of course, the consequence to the design of requiring that it be able to contain the potential nuclear excursion associated with dropping a fuel bundle into a just subcritical core. Although every effort will be made to evolve a highly reliable fuel handling system, past experience in the industry is such that the probability of dropping a fuel bundle cannot be made insignificant. If sufficient subcriticality can be ensured during refueling, the potential nuclear excursion can be precluded. With two independent redundant control systems normally in the

scram position, each with the capability of maintaining the core subcritical, multiple errors are required to bring the core to a just subcritical condition. Possible interlocks will be evaluated to minimize the possibility of operator error. Since the above procedural controls cannot be made absolutely foolproof, the prime reliance will be placed in requiring that the level of subcriticality of the core be monitored by direct measurement. The measurement of subcriticality is discussed separately in subsection 7.4.

10.1 EMERGENCY COOLING

Review of the "General Design Criteria for Nuclear Power Plant Construction Permits" (No. 44) led to a recommendation for the minimum number of main heat transport loops in operation. Since elevated cooling loops have been recommended so that emergency cooling can be obtained by natural circulation in the main loops, the number of these loops enters into the redundancy requirements of the emergency cooling criteria. It was decided to recommend that a minimum of three main loops be capable of removing decay heat whenever the reactor is in a power operating mode. This would then provide a minimum of two loops capable of removing decay heat following loss of heat removal capability in a primary loop by any cause. This was considered to meet the redundancy requirement of Criterion No. 44 and provide adequate reliability of emergency cooling without introducing the complexity and associated safeguards' problems of a separate emergency cooling system.

Preliminary calculations were made to determine the temperature response of the

core following a scram utilizing natural circulation to remove the decay heat. Section IX describes the preliminary analysis utilized to determine the temperature response of the core following a scram with pump coastdown. The results of the analysis are being evaluated to determine the impact on design for decay heat removal.

10.2 SAFETY VESSEL

A review of the conceptual design of the safety tank resulted in the following conclusions.

1. Tank should be backed up by concrete since it may have to resist high transient pressure during DBA—(SEFOR 200 psi).
2. Should have clearance behind it to permit vapor driven off from concrete to be vented.
3. Requirements (1) and (2) will require a slotted surface and/or a small gap.
4. Tank should be made in factory and given ASME Code Class B requirements on fabrication and inspection. Testing to nominal pressure and hydrostatic leak test prior to installation is recommended.
5. Above (1)—(4) result in a free-standing tank with small clearance between tank and core wall (say 1 inch nominal). Flanges will be required for testing and connection to equipment cell liners.
6. Leaks—Accidental leaks cannot be positively precluded from causing some sodium to get into cavity. A pure cavity atmosphere (probably

argon) is recommended so that corrosive products are not formed in cavity due to a leak with a diaphragm seal at penetrations from cavity to N_2 cells. This diaphragm also minimizes probability of sodium leaks in equipment cells getting into cavity and requiring cleanup.

7. Auxiliary NaK cooling coils, vertical coils on inside of tank protected from crushing due to excessive expansion of blast shield.

8. To ensure that the reactor vessel fails before the head, further layout work must be done in locating the blast shield and sodium deflector.

10.3 SAFEGUARDS TRANSIENT ANALYSIS

The following problems are in process of evaluation using the FORE II Computer Code:

1. Scram with flow coastdown all loops.
2. Scram with flow coastdown two out of three loops.
3. Scram with flow coastdown following reactivity insertion due to control rod withdrawal by shim rods.
4. Scram with flow coastdown following reactivity insertion due to single bundle meltdown.
5. Scram and flow coastdown following reactivity insertion due to fuel bundle drop.

Table 10-1 shows the initial cases to be run. Further cases will be established after initial results are obtained.

TABLE 10-1

TRANSIENT ANALYSIS CASES

Case	Steady-State Power/Flow %/%	Transient Flow Parameters			Transient Power Parameters		
		Initial Flow Rate %	Delay Time	Coastdown Characteristic	Positive Reactivity I Insertion	Scram Delay Time	Leg Reactivity Insertion
1.	100/100	100	0	FERMI		0.150	\$-6.00 in 0.5 sec
2.	100/100	0.67	0	FERMI		0.150	\$-6.00 in 0.5 sec
3. a	100/100	100	Signal at 110% overpower	--	5¢/sec	Signal at 110% power 0.150 sec delay	\$ 6.00 in 0.5 sec
3. b	100/100	100	Signal at 110% overpower		10¢/sec	Signal at 110% power 0.150 sec delay	\$ 6.00 in 0.5 sec
4.	100/100	100	Signal at 110% power		50¢ - gravity fall through 11 inches	Signal at 110% power 0.150 sec delay	\$ 6.00 in 0.5 sec
5.	0/0	0	--	--	\$2.2 - gravity fall from 10 ft	Signal at \$1.00 with 0.150 sec delay	\$ 6.00 in 0.5 sec

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