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**FAST FLUX TEST FACILITY
BACKUP DESIGN PROJECT
FOURTH QUARTERLY REPORT
MARCH-MAY 1968**

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
FOURTH QUARTERLY REPORT
March 1, 1968—May 31, 1968

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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 March 1, 1968 through May 31, 1968.

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 firming up the design in the areas of driver fuel instrumentation, open and closed loops, top head design and penetrations and closed loop piping configurations.

The areas of work resulted in significant changes that enhanced the backup design. Nuclear calculations confirmed the advantages of utilizing a zoned core by showing a 24% increase in reflector control while reducing the number of driver fuel channels and maintaining neutron flux required at the 1.0×10^{16} level. The zoned core thus has significant reflector control margin with 59 vol % B_4C (~23\$ for natural and ~35\$ for fully enriched against the required 20\$), and a reasonable control margin should tantalum have to be used for shim control (24\$ with tantalum shim and $B_4^{10}C$ scram rods versus the required 20\$). The control system parameter study has resulted (pending further work in the area of

deceleration) in utilization of a 10-inch offset between the drive and the control rod. The greater offset allows for flexibility in handling the drives and allows the refueling plug outer diameter to be increased. The increased plug diameter provides for greater access for closed loop piping handling operations.

Extensive detailed drafting and analysis work was accomplished in the reactor head area. Several alternate means of accommodating the closed loop sodium pipes have been under study. Reactor head cooling means and structural design to accommodate the DBA have been analyzed. Detail design and analysis have been in process on the various head penetrations to ensure that on-line access to the head could be accomplished.

A core support plate configuration, compatible with the latest reference design vessel, has been evolved. A modification of the SEFOR clamping design is being incorporated in the backup design.

Open and closed loop designs have progressed to the point of establishing a reference design. The PNL Backup Design Task Force work in the area of the test loops has been of valuable assistance in evolving practical loop designs.

Details have been evolved for replaceable driver fuel instrumentation modules. Driver fuel instrumentation sensor and sensor lead package design has been started by the GE Research and Development Center Instrumentation Branch.

Design ground rules have been established for shielding in the vicinity of annular gaps around top head penetrations and around the various top head plugs.

Extensive parameter studies have been conducted for the purpose of establishing control rod drive, drive offset, and control rod design characteristics. Deceleration aspects of the control system design are still in process.

Closed loop design and handling configurations have been evolved for both a single channel reentrant configuration (19-pin test assembly) and a double channel configuration (37-pin test assembly). These two assemblies are interchangeable.

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⁾

PLANT PERFORMANCE TABLE

2.1 TEST LOOPS

Closed Loops	6
Location	1 central 5 midcore
Open Loops	12
Flux	
Peak Level	0.95×10^{16} without shuffling* 1.01×10^{16} with shuffling*
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	36 inches
Equivalent Diameter	40 inches
Average Fuel Power Density	8.24 kW/ft
Composition	
Fuel	PuO ₂ -UO ₂
Structure	stainless
Coolant	sodium

PLANT PERFORMANCE TABLE (Continued)

Core Channels

Driver Fuel

Enrichment Zone 1	63*
Enrichment Zone 2	42*
Closed Loops	6 (7 core positions)
Open Loops	12
Backup Control	3
Total	127*

Fuel Channel

Pins/Channel	127
--------------	-----

Dimension

Across Flats	3.36 inches*
--------------	--------------

Channel Material	Inconel 800
------------------	-------------

Fuel Pins

Diameter	0.210 in. o.d.
----------	----------------

Cladding Material	Type 316 stainless steel
-------------------	--------------------------

Cladding Thickness	0.015 inch
--------------------	------------

P/D	1.286 inches
-----	--------------

Fuel Cycle	60 full power days*
------------	---------------------

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
----------	---------------------

Single Elements	2
-----------------	---

Double Elements	18
-----------------	----

Worth

Single Element	\$0.5 to \$0.75*
----------------	------------------

Double Element	\$1 to \$1.5*
----------------	---------------

Total Worth in Reflector	\$20 to \$35*
--------------------------	---------------

Material	B ₄ C (natural or partially enriched)
----------	--

Drive	G.E. compact mechanical drive
-------	-------------------------------

Backup Control

Location	in-core
----------	---------

Number	3
--------	---

Worth	\$1—\$2.7 each
-------	----------------

	\$3—\$8 total
--	---------------

Material	B ₄ C
----------	------------------

Drive	compact drive
-------	---------------

*Indicates change from last report period.

PLANT PERFORMANCE TABLE (Continued)

2.2.4 Vessel

Diameter	16.0 ft
Thickness	0.75 inch
Material	Type 304 stainless steel

2.2.5 Coolant

Flow	15.1×10^6 lb/h
Inlet Temperature	700°F
Outlet Temperature	1000°F
Core ΔP (Nominal Plenum-to-Plenum)	100 psi*

2.2.6 Cover Gas

Type	argon
Pressure, Operating	± 0.5 in. water gage

2.2.7 Temperature

Peak Channel (With Core Orificing)	<u>100% Power</u>	<u>120% Power</u>
Fuel—Maximum	4600°F*	5200°F*
Fuel Surface—Maximum	1875°F*	1990°F*
Cladding—Maximum (i.d.) (Hot Spot)	1180°F*	
Sodium—Maximum (Hot Stream)	1060°F*	
	<u>Design</u>	<u>Operation</u>
Grid Plate	1000°F	700°F
Reactor Vessel	1200°F	1000°F
Vessel Head	1000°F	200°F

2.2.8 Power Distribution (Beginning of Equilibrium Burnup Cycle)

Radial Flux Peaking	1.30*	1.556*
Axial Flux Peaking	1.2*	
Overpower Allowance	1.2	
New to Average	1.06*	
Fuel Nonuniformity	1.04*	

2.2.9 Hot Channel Factor

Statistical and Uncertainties	1.1
-------------------------------	-----

2.3 SYSTEM

2.3.1 Primary Coolant

Number of Loops	3*
Rating	133 MWt/each*

*Indicates change from last report period.

PLANT PERFORMANCE TABLE (Continued)

Piping		
Material		Type 304 stainless steel
Size		
Reactor Outlet		20 inches*
Reactor Inlet		18 inches*
Heat Exchanger Rating		133 MWt/loop
Coolant Loop Unavailability		7.8%
Temperature		
Design		1200°F
Operation		1000°F
2.3.2	<u>Containment Vessel</u>	
	Type	cylindrical
	Diameter	135 feet
	Thickness	1.1 inches
2.3.3	<u>Emergency Coolant</u>	
	Type	NaK
	Heat Load	approximately 4 MWt
2.3.4	<u>Vessel Cavity</u>	
	Gas	nitrogen
	Pressure	± 10 in. water gage
	Liner	steel
2.3.5	<u>Equipment Cell</u>	
	Gas	nitrogen
	Pressure	± 10 in. water gage
2.3.6	<u>Loop Cell</u>	
	Gas	nitrogen
	Pressure	± 10 in. water gage
	Floor Liner	steel
2.4	HOT CELL	
2.4.1	<u>Size</u>	
	Length	84 feet
	Width	30 feet
	Height	78 feet

*Indicates change from last report period.

PLANT PERFORMANCE TABLE (Continued)2.4.2 Transfer Cell

Maximum Size Transfer

Diameter	4 feet
Length	43 feet

2.4.3 Fuel Handling Method

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

2.4.4 Fuel Storage Method

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

2.4.5 Refueling Cell Atmosphere

Atmosphere	argon
Design Temperature—Accident	250°F
Design Operating Temperature	150°F
Design Pressure—Estimated Maximum	20 psig
—Estimated Minimum	-6 in. water gage
Operating Pressure—High	+2 ± 1 in. water gage
—Low	-2 ± 1 in. water gage
Leakage (est. maximum)	1% cell vol/day at design pressure

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

2.4.7 Major Equipment

Bridge crane mounted telescoping tube hoists with fuel grapple mechanisms.	2
Bridge crane mounted general purpose manipulators.	2
Master-Slave Manipulators	3
Traveling Hoist	100 ton
Bridge Crane	5/20 ton
Man Access Equipment	
Decay and Storage Tanks	7
Spent Fuel Transfer Thimbles	50

2.5 SAFEGUARDS

PLANT PERFORMANCE TABLE (Continued)

2.5.1	<u>Containment</u>	double
2.5.2	<u>DBA Energy Release Estimated Maximum</u>	1500 MW-sec
2.5.3	<u>Credible Accidents</u>	
	Flow Coastdown	
	Loss of Single Coolant Loop	
	Piping Leakage (Including Guillotine Pipe Failure)	
	Single Fuel Bundle Meltdown	
	Control Rod Withdrawal	
	Refueling Bundle Drop-In (Subcritical core)	

2.6 NUCLEAR

2.6.1 Core Composition

Fuel Channel Volume Fraction

Fuel	0.326
Steel	0.154
Incoloy 800	0.114
Sodium	0.406

Experimental Loop Volume Fraction

Closed Loops

Steel	0.487
Sodium	0.464
Fuel	0.049

Open Loops

Fuel	0.244
Sodium	0.466
Steel	0.116
Incoloy 800	0.174

Backup Control Rod Volume

Steel	0.333
Sodium	0.667

2.6.2 Midcycle Fuel Composition (atom %)*

	<u>Zone 1</u>	<u>Zone 2</u>
Uranium	74.57%	65.97%
Plutonium	22.38%	30.99%
Fission Products	3.05%	3.04%

Plutonium Isotopic Concentration*

Pu-239	84.00%	84.50%
Pu-240	14.74%	14.82%
Pu-241	1.22%	1.14%
Pu-242	0.04%	0.04%

PLANT PERFORMANCE TABLE (Continued)

Uranium Isotopic Concentration		
U-235	0.2%	0.2%
U-238	99.8%	99.8%
2.6.3 Reactivity		
Doppler		
$T \frac{dk}{dT}$ (sodium-in)	0.0052*	
$T \frac{dk}{dT}$ (sodium-out)	0.0032*	
Sodium Void		
Total Core	\$ -2.2*	
2.6.5 MeV/Fission		
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}	

*Indicates change from last report period.

FIGURE 2-1. CONCEPTUAL REACTOR SYSTEMS ARRANGEMENT

SECTION III

PLANT SYSTEMS

3.1 REACTOR VESSEL HEAD

Consistent with program emphasis on mechanical design areas agreed to be of prime importance, work was initiated to review the functional specifications and to provide a more detailed conceptual design of the FFTF Backup Design vessel head. The components included in this work are the vessel head, the refueling plug, other head and plug penetrations (such as access ports for control rod drives, in-vessel handling, lighting, viewing, and auxiliary experiments), vessel interface support, and DBA holddown facilities.

The vessel head functional requirements include:

1. Contain primary sodium and vapor (except during DBA).
2. Provide adequate nuclear shielding for on-line access to the head.
3. Provide rapid access to driver fuel and in-core experiments.
4. Provide access to reactor components including: control, reflector, core clamping, and shield rods; closed loop branch piping; fuel leak location detection tubes, and in-vessel irradiation facilities and to the activated parts storage area (outer vessel periphery).
5. Provide heat removal capability from head, vessel, and penetrations.
6. Provide adjustment to line up center-line of head and core after plant operation.

7. Adequately support the control rod drives, access, support, and viewing port gear, and the refueling plug. The refueling plug supports the driver fuel instrumentation and rack, the backup fuel holddown plate, closed loop reactor tube protrusion and cover, and the backup scram control drives.
8. Minimize DBA effects by providing: energy absorption below the head and in stretch head-holddown bolts; equipment designed to minimize sodium (water) hammer on the head and to maintain head integrity under all conditions.

Prior to this quarter's activities it was apparent that there were two independent primary concepts that affected the head design. One is the choice of whether the head structure should be located above or below the vessel flange (and refueling cell floor) level and the other is whether the branch piping from the closed loops penetrates the vessel or the head. These four possibilities were reconsidered this quarter and tentatively it was concluded that:

1. The choice of the location of the structure was confirmed to be independent of the location of the branch piping penetrations.
2. The future work on the head structure should be directed toward the "below" flange concept since it provides a less deep (~24 inches) head (thus providing shorter passages to thread closed and open loop instrumentation lead stalks). It is recognized that this concept is more difficult to design structurally in the vessel to holddown bolt region

because of the additional stiffness requirements, but the core access advantages appear to offset this disadvantage.

3. The near-future work on the head should include the closed loop branch piping penetrations through the head (near the periphery). This choice was difficult to make at this time because a number of design decisions external to the vessel have not become firm. Furthermore, it appeared that the penetration and attendant piping structure support, thermal insulation, and biological shielding were equally difficult to design for vessel or head penetration locations. The choice to continue to design the through-head facilities was based on the relative independence of external (to the vessel) design decisions and simplification of the head design if the choice were made to penetrate the vessel rather than the head.

The following paragraphs, numbered and related to the respective items in the functional requirements, indicate the work accomplished, the observations and conclusions generated and the work to be done to complete this conceptual design.

1. Containment for the primary sodium and vapor is provided by the vapor trap method and backed by an extended length flow region that is maintained below the melting point of sodium plus deformable low-temperature ($<200^{\circ}\text{F}$) gasket seals. Even though the head cooling system is in an embryonic state of development, it is apparent that liquid (NaK) or gas (argon) cooling systems located on the lower structural member will be adequate to provide these temperature conditions. The gasket type seals are provided to minimize "solid" sodium vapor and potential gaseous fission product transfer into the refueling cell.
2. Biological shielding materials and geometric interferences are provided to reduce the gamma dose from the primary sodium ($2.5 \cdot 10^8 \text{ mR/h}$) to a tolerable limit ($\sim 25 \text{ mR/h}$) above the head. Leakage neutrons from the core and associated structures are adequately attenuated by the gamma shield. As reported in the physics section of this report, the shielding analyses are preliminary in nature but have indicated that at least one step is required for any practical size plug gap. The most difficult gap to shield is the annulus between the vessel and head. As described in paragraph 6 below, a vessel-head radial gap of 1 inch was considered necessary for postoperational realignment of the core and the refueling plug or control drives. This is the largest gap in the head and thus the more difficult to shield. Structure and shielding external to the vessel have not been fully designed so it may be practical to apply removable shielding (steel, lead, or concrete) over and around the periphery of the head structure to shield this region as well as any inadequacy of shielding built into the head-vessel step structure. Attempts to internally shield this gap are shown on the accompanying drawings. Iteration of shielding and mechanical design requirements will continue to produce a mutually satisfactory design.
3. As reported previously, rapid access to the driver fuel and in-core experimental facilities is provided by a small (4-foot) diameter refueling plug. Considerable attention has been and will continue to be devoted to accommodating the various plug penetrations for experimental facilities (open and closed loops), control safety rod drives,

for sodium fuel leak detection location lines and driver fuel instrumentation. Additional description of these penetrations is included in a following section of this report.

Structurally the refueling plug consists of an upper and lower 3-1/2 inch plate spaced 20 inches apart, and spaced by a cylindrical wall and the numerous penetration sleeves. These are integral with the refueling plug and provide structural support and guidance and seal surfaces for the insert plugs, as well as a barrier between the sodium vapor and the interstices of the refueling plug. Cooling coils are located on or near the lower structural plate.

Immediately below the lower plate is a 27-inch-deep multiple plate-and-honeycomb assembly that provides the dual function of energy absorption during a DBA and thermal insulation. Tentatively, the crush strength of the absorber has been set at 500 psi. Estimates of the thermal conductivity are in the process of being generated. This does not appear to be a severe limitation on the design. Design work on these dual functions and requirements will continue. The depth of the sandwich assembly was determined by the vessel length (wave length) to reduce the temperature from the outlet temperature of the sodium (1200°F design) to 200°F. This type construction persists radially outward to the vessel.

The refueling plug is heavy enough (6 to 7 tons) to remain in place during normal operation. However, during an accident, including the DBA, some type of holddown is required. Two alternate designs have been considered. The first, a segmented shear ring integral with the refueling plug structure and the second, an above-head restraint structure. Either can

be satisfactorily designed. The present choice is to use the segmented shear ring since it utilizes the minimum amount of refueling plug and control drive area. Because of the need for accessibility to the many plug penetrations and leads, any structural interference handicaps the amount of facilities that can be provided.

4. To provide additional access space to service and maintain the core, reflector and other regions within the vessel, an annular concentric plug has been shown between the refueling plug and the head. It is approximately 7 feet in diameter with about a 4-foot inside diameter to accommodate the refueling plug. Major components penetrating this annular "reflector" plug are the reflector control drive mechanisms. Also under consideration are penetrations for access to some potential reflector region test facilities, the core clamping actuators, and sodium sample piping from the failed fuel location detection system. These facilities are presently being considered and do not appear on the attached illustrations.

The reflector plug provides for more complete accessibility to the reactor (the outside diameter being larger than the core clamping device surrounding the radial reflector region). Removal of this annular plug will take longer than the refueling plug since the latter probably would be removed first; also the control drive mechanism will have to be disconnected from the reflector control rods. Other disconnect operations, such as separation of the fuel leak location detector sodium lines and the core clamping device operators, would also require additional time. Initial estimates indicate that the frequency of the annular plug removal

would be two to three times in 5 years of operation, primarily for reflector and radial shield maintenance and inspection of the reactor.

The structure of the reflector plug is similar to the refueling plug except that radial stiffeners are installed to minimize rolling of the annulus during the DBA. Similar to the refueling plug, the reflector plug is prevented from blowing out of the head during a DBA by segmented shear rings. These are about 2 inches square, compared with the 1-1/2-inch square rings in the refueling plug. Cooling to the lower plate will be similar to and in parallel with the refueling plug system.

Complete vertical access to all parts within the reactor vessel can be obtained by removing the large vessel head. Due to the anticipated lengthy time to disconnect and replace all refueling and reflector plug connections, plus the DBA holddown bolts (48, 3-1/2-inch diameter), the head-vessel gasket bolts (if any), disconnect the branch piping to all the closed loops and tubing to the failed fuel location detector, and remove any biological shielding or other gear on the head; the large head will not be removed very frequently. As noted previously, the refueling and reflector plugs and related in-vessel handling equipment are designed to provide operationally necessary access to the vessel.

5. Initial cooling analyses for the head and plugs are reported in Section IV. Considerably more detailed analyses will be generated as soon as the thermal properties of the energy absorber-thermal insulation and design details of the various head and plug penetrations are known. Because of the large number

of penetrations, the thermal loading in the refueling plug is estimated to be over 1 kilowatt per square foot, while in the reflector plug and head the loading is respectively lower. Liquid cooling (NaK) has been assumed in the plugs in order to minimize the distance from the cooling coil to the heat source (penetration). If the heat sources continue to be of low magnitude, it may be practical to return to gas (argon) cooling. The largest heat load, but the lowest heat loading, occurs in the head (~80% of total area). Apparently, either liquid or gaseous coolant can be utilized.

It has not been determined whether all the head and plug penetrations can be adequately cooled by the respective cooling systems. If excessive temperatures are generated in some of the larger penetration plugs, individual cooling systems may be required. Because of space limitations, liquid cooling would be most desirable for these applications.

6. Due to so many unknowns at this stage of design and fabrication of the vessel and its contents, a postoperational core-head radial lineup allowance of 1 inch was assumed. Thus, the center of the refueling or reflector plug could be moved horizontally 1 inch in any direction to improve the lineup with the core. This allowance should be reconsidered after the design and vessel fabrication become more detailed.

Conditions that potentially would displace the core once it is installed include residual stresses in the vessel and foundation, creep or yielding of the core support with time, irradiation, hydrostatic forces, temperature, primary coolant system piping reactions,

and temperature stripping of the vessel or internals caused by inadvertent sodium circulation.

Internal biological shielding difficulties occur with the 1-inch gap or annulus with a 192-inch diameter (2-inch potential gap on one side of the vessel when the plug is displaced at the maximum position). The present approach is to utilize as much of the "unnecessary" vessel flange volume as possible to install, integral with the head, a steel shielding plug. About 13 inches of thickness is available at present. Shielding analysis of the effectiveness of the associated step(s) and gap (1—2 inches) will be used to optimize the shielding design. Offset or step construction of the 3/4-inch vessel wall was rejected in favor of adding additional gamma shielding above and around the head, if necessary.

7. Mechanical support of the two concentric plugs and all the equipment, that is mounted on these plugs and the head, requires a small fraction of the head strength required for integrity during the DBA. See Section IV of this report for the structural analyses. Equivalent steady-state DBA loads are 10 to 20 times the magnitude of gravity loadings.

Even though the DBA loading is in the reverse direction (up), the structure appears to be more than adequate for normal support functions. The plug interface shielding steps supply ample bearing surface for the weight of the plugs.

Vertical height or deflection of the head under operating conditions is expected to be very stable due to mounting the head on the reactor support and maintaining the head structure at nearly

ambient temperature conditions. Anticipated head deflections are very small (tenths of mils) compared to the several inches of difference between the head and the core support plate from thermal expansion of the vessel and reactor internals.

As noted in Item 6, head adjustments in the horizontal plane can be made for postoperational realignment of the core and head. Radial snubbers for horizontal seismic loadings have not been shown. It is anticipated that this feature will be provided by the forthcoming head-vessel-skirt-foundation bolting system.

8. As specified in the functional requirements, the head design incorporates many features to alleviate the effects of a DBA and to maintain cover integrity. Preliminary analyses of the effectiveness of these mechanical features are presented in Section IV.

Equipment absorbing energy or spreading the impact load over a longer time interval includes the laminar plate and honeycomb structure below the head structure, the head (and plugs) holddown stretch bolts (and related plug segmented shear rings) and the sodium impact (water hammer) absorbing structure below the head. Energy absorption within the head structure has not been considered recently because of its estimated low magnitude. The water hammer attenuator is located very near the operating sodium level to minimize momentum of the sodium. The inertia of the attenuator plate transmits its energy to the head through the plate and honeycomb structure. Energy producing excessive pressure on the plate (greater than

500 psi equivalent) would be attenuated by the crush structure. Water hammer attenuator structures are not shown on the refueling and reflector plugs since they occupy only 20% of the vessel (head) area and are partially protected by the structures below the energy absorption plates such as the closed loops, open loop stalks, driver fuel instrumentation, sodium sample lines for fuel leak location detection, and the extension rods of the control rod drive system.

3.1.1 Head Study Drawings

Attached are copies of a few selected drawings from the 20 studies that were made to arrive at the present state of head design. Since the significant items are described in the preceding text, the following tabulation lists the figure number and denotes the significant items shown.

Figure 3-1: Head Structure Above-Vessel-Flange

- Note • Above-vessel-flange head structure.
- Steel and shot gamma shielding.
 - Branch piping from the closed loops penetrating the head.
 - Additional shielding over branch piping.
 - Segmented shear rings for refueling and reflector plug.
 - Typical head and plug structure with energy absorbing and water hammer structure below structure.
 - Cooling coils mounted below structure.

Figure 3-2: Plan View of Structure Above-Vessel-Flange Head

- Note • Relative size of central refueling plug and the annular reflector plug.

- Branch piping shielding covers.
- Holddown bolt location (more than the required 48 are shown).
- Bolts are located under the lead shielding but not under the branch piping.

Figure 3-3: Head Structure At-Vessel-Flange Level

- Note • Reduced thickness of head and simplified structure.
- Gussets for stiffness in vessel-to-bolt region.
 - Location of cooling coils.
 - Branch piping above gussets and holddown bolts.

Figure 3-4: Plan View of Structure At-Vessel-Flange Head

- Note • Revised control drive pattern.
- Gussets on head.
 - Branch piping-shielding cover is removed.

Figure 3-5: Plan View of Head Showing Internal Structure

- Note • Radial ribs in reflector plug and head.
- Rectangular penetration for branch piping.
 - Width of vessel flange (broad cross-hatched quarter-circle).
 - Spacing of holddown bolts (48).

Figure 3-6: Minimum Wall for Reflector-Refueling Plug Interface

- Note • Structural requirements of 3-1/8-inch radial width for segmented shear ring

FIGURE 3-1. HEAD STRUCTURE ABOVE VESSEL FLANGE

FIGURE 3-2. PLAN VIEW OF STRUCTURE-ABOVE-VESSEL-FLANGE HEAD

FIGURE 3-3. HEAD STRUCTURE AT VESSEL FLANGE LEVEL

FIGURE 3-4. PLAN VIEW OF STRUCTURE-AT-VESSEL FLANGE HEAD

FIGURE 3-5. PLAN VIEW OF HEAD SHOWING INTERNAL STRUCTURE

STUDY NO 15
MINIMUM WALL FOR REFLECTOR-REFUELING PLUG INTERFACE

FIGURE 3-6. MINIMUM WALL FOR REFLECTOR-REFUELING PLUG INTERFACE

plus back-off space and minimum structural wall thickness (3/8 inch).

- Two shielding steps in addition to shear ring.
- Increased wall thickness to augment shielding.
- Shielding step width limitation on plug clearance.
- Thin walls below lower plate to reduce axial conduction of heat from the sodium.

3.2 CORE SUPPORT AND INLET PLENUM

The design of the inlet plenum and core support plate, which provides support for the reactor internals, has been modified to define the way the core support for the backup design will interface with the design of reference pressure vessel. As shown in Figure 3-7, the inlet plenum consists of two perforated plates which would be bolted to the reference design core structure, replacing the two perforated plates that form the inlet plenum for the reference design. Various schemes were investigated for the plenum structure design. Integrally fabricated double core support plate designs were discarded at the recommendation of Manufacturing, because of the inability to match-drill the plates across the plenum accurately.

Core bypass leakage that might occur at the junction of the perforated plates and the reference structure will be controlled by metal O-ring seals at each of the junctions. The O-rings are compressed by the plate holddown bolts. The upper plate is bolted directly to the reference structure while the lower plate is bolted to the upper plate by a ring of special fasteners located just inside the ring of bolts that fasten the upper plate to the reference structure. The upper plate has an outside diameter of 68-1/2 inches,

a thickness of 6 inches, and is perforated with 127 1/2-inch diameter holes on a 3.39-inch equilateral triangular spacing. The lower plate has an outside diameter of 65 inches, a thickness of 4-1/2 inches, and is also perforated with holes on the same pitch and pattern as the upper plate. The alignment of the hole patterns between the two plates is very important since the nose piece of the fuel assemblies must penetrate both plates to be properly installed. The hole pattern alignment will be controlled by machining both plates at the same time while clamped together.

The upper plate provides support for the driver fuel assemblies, open and closed loops, and the control rods when they are disconnected from the drives. The lower plate provides a seal around the lower end of the fuel assemblies so that a hydraulic holddown can be obtained on the fuel assemblies. The nominal pressure of the coolant in the inlet plenum will be as high as 150 psi above the pressure in the surrounding coolant. The 150 psi pressure difference, acting on the grid plates, creates a load in excess of that created by the weight of the components supported by the upper plate and, hence, is an important factor in determination of the thickness of the two plates.

3.3 CORE CLAMPING

A new clamping for the FFTF core is being considered. The new concept utilizes a torsion rod type of clamping mechanism similar to the type installed in the SEFOR reactor. A drawing of a clamping device which is similar to the mechanism proposed for use in the FFTF core is shown in Figure 3-8.

In the system proposed, a clamping device would be installed within each of a set of reflector assemblies which form a complete ring around the fuel assemblies. The ring of

FIGURE 3-7. INLET PLENUM

FIGURE 3-8 CLAMPING DEVICE

clamping mechanisms would be located radially beyond the outside diameter of the reflector plug. The clamping mechanisms are actuated by a special tool which is inserted through access holes located in the reactor outer head. The clamping mechanisms are connected by roller chains, as shown in Figure 3-8, in groups of three, so that 12 access holes are necessary to actuate the 36 clamping mechanisms required to form the complete ring of clamping force around the reactor core. The actuating tool rotates the torsion rods of the clamping mechanisms to some determined torque value. The torque is then locked in the torsion rods by means of a splined connection.

The reaction to the torque stored within the torsion rods is used to force the hexagonal channel, in which the clamping mechanism is installed, radially inward. The tightening process, if repeated at 12 locations around the periphery of the core, creates a complete ring of radially inward-directed forces which hold the fuel assemblies in a compact rigid array. Differential thermal expansion between the core and surrounding reflector region is accommodated by more or less rotation of the torsion rod on the clamp which functions as a torsional spring.

The above method of clamping the FFTF core has the major advantage of not interfering with refueling or experiment handling since the clamping mechanisms are located outside the core. Also, the core could be tightened and verified before startup since no core temperature changes or hydraulic pressure is required for clamp actuation.

3.4 CONTROL ROD

Design analysis of the control rod element continued throughout the quarter. Detailed calculations (see subsection 4.1) of the

10-inch offset between the control rod drive axis and the control rod element axis are in process.

Two design approaches were considered: a relatively rigid structure where only the end roller guides contacted the channel walls, and a limber structure where many wear surfaces slide along the channel walls. Because the latter design requires extensive development work to determine compatible material systems, it was decided to design a rigid structure with rolling guides at the ends of the carriages.

The present design has a poison region of B_4C enclosed in rods which is contained in a hexagonal carriage. Attached to this carriage is another carriage containing the nickel (reflector) follower material. Pertinent current design parameters are:

Number of reflector control elements	38 to 40
Number of pins	37
Cladding outside diameter	0.43 inch
Cladding wall thickness	0.045 inch
B_4C —Enrichment	natural
B_4C —Density, % TD	80
B_4C —Column length	34 to 44 inches
B_4C —Plenum length	15 inches
B_4C —Burnup, %	20
Design gas release, %	25
Reflector control worth	32\$
In-core control worth	8\$

To accommodate the full length of the nickel follower and a hydraulic snubber at the bottom of the control channel, as presently envisioned, will require either lengthening the reactor vessel or moving the core region up within the present fuel channel.

3.5 CONTROL ROD DRIVE MECHANISM

3.5.1 Drive Description

Two drive systems are planned for the FFTF backup design. The primary drive is a reflector control drive system and the secondary drive is an in-core safety drive system. The primary reflector control drive mechanisms are mounted around the periphery and outboard of the central refueling shield plug. An important advantage of this mounting arrangement is the ability to remove the refueling plug without uncoupling and removal of the primary drive mechanisms. This also leaves the reflector control drives operable during refueling which reduces the safeguard hazard of uncoupled control rods, and provides a method if needed for determining the degree of subcriticality. To achieve this mounting arrangement of the drives outboard of the inner shield plug requires that the centerlines of the drive and control rod be offset. The coupling of the drive to the control rod is made through an offset connection.

The secondary drive system consists of three in-core safety drives that are planned to be mounted on the inner shield plug. These drives will be uncoupled and removed prior to the removal of the inner shield plug.

A. Design Objectives

Following is a preliminary list of the design objectives which are being incorporated into the control rod drive design.

1. System Safety Criteria

- a. Control rods to scram downward on signal from any position in the stroke under all operating conditions.
- b. No single failure will prevent system scrambling.

- c. No energy shall be required to be applied to the system to obtain a scram.
- d. Only one drive shall be actuated in the shim mode at a time. All units will scram together upon the scram signal.
- e. No signal failure can cause a reactivity insertion greater than twice the normal insertion rate.
- f. Any failure will cause the system to respond into a safe mode.
- g. Reactor shutdown from the primary control system shall be accomplished with one drive stuck in the most reactive position.
- h. The in-core safety control system can shut down the reactor to hot standby condition when one safety rod has failed to operate.

2. Qualitative Characteristics

- a. Primary reflector control drives to be combination shim and scram drives.
- b. Backup safety drives to be two-position scram drives, full-in or full-out.
- c. A coupling shall be provided for drive assembly disconnect at the control rod.
- d. A disconnect joint for maintenance disassembly shall be provided between the extension shaft and drive mechanism.
- e. Reactor blanket cover gas shall be sealed from leaking into the cell through the drive penetration.
- f. Position indication shall be provided during scram.

- g. Capabilities for remote installation and removal of the drives in the refueling cell are to be provided.

B. Primary Reflector Control

Figure 3-9 shows the basic concept proposed for the primary reflector control drives. An analysis of the design details for this drive presently in process is described in subsection 3.5.2.

The main element of the primary reflector control drive is a vertically mounted double shaft pneumatic cylinder. Connected to the lower shaft of the pneumatic cylinder is an extension shaft which extends down into the reactor and carries the load of the reflector control rod. The upper shaft of the pneumatic cylinder extends up into a cylindrical housing mounted in line with the pneumatic cylinder. An attachment to the upper shaft engages slots in the cylindrical housing to achieve an antirotation feature that is required to prevent disengagement of the rotary breech-block coupling between the extension shaft and the reflector control rod. The coupling is engaged or disengaged by 45-degree rotation of the drive and extension shaft, thus to prevent uncoupling the antirotation feature of the drive shaft is required.

The control is also prevented from rotation in its guide channel by being captured within the limits of the hexagonal channel. Mounted at the top of the cylindrical housing is a gear motor which drives a lead screw that positions a stop nut at any desired position along the full stroke length of the drive. This nut provides a stop for the cylinder. Gas pressure is applied to the lower end of the pneumatic cylinder which carries the load of the control rod and holds the control rod and piston assembly firmly in contact with the back stop nut engaged on the lead screw. Position of the control rod is then determined by positioning the stop nut with the lead screw.

A control rod scram is produced by pressurizing the top side of the pneumatic cylinder from a remote accumulator. The support pressure on the lower side of the piston is simultaneously vented.

Figure 3-10 shows the plan view arrangement of the reflector control drives around the periphery of the opening for the refueling shield plug. The arrangement pattern is based on the two-zone core (see subsection 5.1) and a 10-inch offset in the drive coupling to the control rods. Analysis of the offset features of the design is reported in subsection 4.1 of this report.

C. In-Core Safety Control

Development of a concept for the in-core safety control drive mechanism is currently in progress. The safety control drive concept is a two-position scram only drive utilizing the cocked rod principle. The control rod is withdrawn from the core into the cocked position by coupling the bridge crane to the drive mechanism and using the bridge crane to withdraw the rod. Two methods are used to provide the scram force which is developed as the rod is cocked. The first is the compression force of a long helical spring, and the second is the withdrawal of a piston from a pneumatic cylinder in which the cylinder is maintained at vacuum. The rod is maintained in the cocked position by an electric clutch latch similar in principle to the EBR-II drive latch. By this means, the in-core safety control operates on a different principle from the primary reflector control with the intention of satisfying the suggested AEC design criteria of two drive systems of different operating principles.

3.5.2 Analysis of Key Design Parameters

The major effort during this quarter has been in the analysis of several of the key design parameters of the primary reflector control drive. The most significant area has

FIGURE 3-9. DRIVE MECHANISM

FIGURE 3-10. DRIVE MOUNTING-PLAN VIEW

been the evaluation of the offset at the coupling of the drive shaft to the control rod. Many variables enter into this problem such as the elastic stability of the drive line, the B_4C cladding and plenum stress, the bypass flow around the control rod, the scram resistance, the stress and deflection of the control element, the scram response, and the reactivity worth versus clearance.

Separate investigations have been made of the above variables over the possible ranges of the variables involved. The results of these analyses are reported in subsection 4.1.

At the present time, further work is being accomplished on the scram cushion deceleration function.

3.6 CLOSED LOOP DESIGN AND HANDLING

The closed loops which are designed to provide a test region within the reactor core, complete with an isolated independent coolant system, consist of two different interchangeable types. There will be a total of six closed loops installed within the FFTF core. Five of the six loops presently shown are of reentrant or counterflow type, as pictured on Figure 3-11, and are located at the same radial position within the core approximately 13-1/2 inches from the core center at a flux of approximately 0.8×10^{16} n/cm²-sec. The test sections of the closed loops are suspended within the core by means of a hanger rod or handle which extends to the top of the closed loop above the refueling plug. The test sections may be removed from the closed loops by means of the handle which also serves to seal the closed loop inlet flow from the outlet flow, thus providing a coolant flow circuit through the closed loop. In the reentrant type loop, the inlet coolant flows downward through an outer annular path and then flows upward through the test region to the outlet pipe.

The sixth closed loop, to be installed at center of the core, differs from the other five closed loops in that it occupies two hexagonal channel positions in the core. One of the hexagonal channels is used for the inlet flow and the other is used to house the test region, thus allowing for a larger diameter test assembly and more coolant flow than can be accommodated in the reentrant type closed loop. The upper end of both types of closed loops is essentially identical above the elevation of the point of attachment of the closed loops to the inlet and outlet piping crossing over the core.

Shielding will be provided as part of the test section handle over the length of closed loop that passes through the refueling plug. The lower end of the closed loops will be similar to the nose piece of the driver fuel elements and will extend through the support plates which form the inlet plenum, taking advantage of the hydraulic holddown effect. Differential expansion of the closed loops with respect to the refueling plug will be accommodated by having the closed loops connected to the refueling plug by means of a bellows in the nonsodium head area which will allow the required axial motion of the closed loop. A spring will be used to hold the closed loops in the proper position. Designing the closed loops with a lower end similar to that of a driver fuel element will allow a driver fuel element to be installed in a test loop position if it is desired to operate the reactor with less than six closed loops installed.

All of the closed loops are connected to the coolant inlet and outlet lines by means of remotely operable couplings. The couplings are located at an elevation just below the refueling plug and at a radial position above the reflector-control channels which surround the fuel assemblies. The closed loop couplings are accessible when the refueling plug has been removed since they

are located within the refueling plug opening (see Figure 3-11). At the present time, the couplings are operated by rotating two screws that hold the two halves of each coupling together. Rotation of the two screws also causes the joint to separate in a horizontal direction through a cam and follower to free the closed loop from the piping to allow removal. Contacts with a vendor have produced information on a new type of coupling which is remotely operable by special tools and which requires no threaded fasteners and has a smaller envelope dimension. This new type, which is presently being tested by the vendors, appears to have some desirable features for use on FFTF closed loops.

3.7 OPEN LOOP

The current design concept provides for 12 open loop assemblies located around the periphery of the core region in flux of $0.65\text{--}0.75\text{ n/cm}^2\text{-sec}$. The instrumentation leads are routed out of the vessel through a vertical pipe stalk that penetrates the central refueling plug. Figure 3-12 shows the concept proposed for the open loop penetration. The penetration design allows the refueling plug to be lifted vertically with the stationary open loop stalks slipping through the penetration. A bellows seal in the nonsodium area of the plug incorporated in the penetration provides for thermal expansion of the open loop stalk with respect to the refueling plug. A cable connection is provided at the top of the stalk. The cable connection and the loosening of the penetration sealing collar will be performed by remote means, and the open loop installed and removed remotely by attaching handling equipment at the top of the stalk. A design feature planned to be incorporated into the design is the ability to install an open loop into a closed loop position.

The preliminary allocation of instrumentation leads contains a total array of 19 paired leads, each pair in a 1/8-inch diameter sheath.

Assembly outlet temperature, flow, and inlet temperature are the basic parameters to be measured. The number of paired leads allocated for each of these is listed below:

Outlet Temperature	5 (thermocouples)
Flowmeter	1
Inlet Temperature	3 (thermocouples)

This leaves a remainder of 10 paired leads to be utilized for additional instrumentation requirements as may be established in the future.

3.8 DRIVER FUEL-ASSEMBLY DESIGN

The current effort in the driver fuel-assembly design is the optimization of the axial reflector and shield regions. Shortening these regions will allow: (1) relocation of the core upward in relation to the core support plate to accommodate the addition of a hydraulic snubber at the bottom of the reflector control channels; (2) removal of interfering media between the core region and the top of the fuel channel where acoustic and temperature sensors are proposed to be located; and (3) possible shortening of the overall length of the fuel assembly.

3.8.1 Pertinent FFTF Backup Design Driver Fuel Assembly Data

Number of Driver Fuel Assemblies	105
Plenum-to-Plenum Pressure Drop, psi	105
Inlet Temperature, °F	700
Outlet Temperature, °F	1000
Fuel Assembly Pitch, Center-to-Center at 70°F	3.390
Fuel Pin:	
Number of Pins per Assembly	127
Center-to-Center Pitch, inch	0.270
Diameter, inch	0.210
Nominal Pin Separation, inch	0.060
Wall Thickness, inch	0.015
Active Fuel Length, inches	36
Pin Length, inches	80
Separation Method	Wire Wrap
Wire Wrap, o.d., inch	0.060
Wire Wrap Pitch, inches	12

GENERAL ELECTRIC	
FILE	DATE
FIRST MADE FOR

FIGURE 3-11. REENTRANT-CLOSED LOOPS

GENERAL ELECTRIC		174F137
TITLE: CONCEPTUAL		REV. NO.
OPEN LOOP		(2 1/2" PENETRATION)
FIRST MADE FOR		FFTF
F.C.F. 296x146

FIGURE 3-12. OPEN LOOP PENETRATION

Fuel Pellet:

Fuel Composition	Mixed Oxide 20 to 30 atom % Fissile Plutonium (239 and 241).
Fuel Form	Solid, Right Circular, Cylindrical Pellets
Pellet Fabrication	Coprecipitation or Mechanically Mixed, Cold Pressed, and Sintered

3.9 DRIVER FUEL INSTRUMENTATION

Operation of the FFTF will be dependent on the reliability of the driver fuel instrumentation. To maintain a high plant availability, it is necessary to have a relatively easy means of replacing the instrumentation if it becomes defective. The driver fuel instrumentation proposed for the backup FFTF design is included with the refueling plug. Instrumentation is built in modules that are assembled to the refueling plug. Replacement of any defective instrumentation will require that an entire module be replaced. Maintenance on the removed modules, if feasible, can be carried out at a remote location. The modules are intended to be fabricated and tested to ensure they are working properly before being brought into the refueling cell and assembled to the refueling plug. The structure of the modules provides protection to the sensors and leads during its assembly to the refueling plug.

The current design includes 14 instrument modules as determined by the core arrangement, the refueling plug penetration arrangement, and by the number of channels each unit provides instrumentation to. Two types of instrument modules will be required to provide instrumentation to all the fuel elements. The instrumentation to the core positions under the closed loop piping will require a slightly different module than those supplying instrumentation to the other core positions. Instrumentation beneath the closed loop piping must be offset from their position under the piping before the refueling plug can be raised from the vessel.

3.9.1 Instrument Modules

The instrument modules supplying instrumentation to the core positions are firmly attached to the refueling plug, as shown in Figure 3-13. The modules consist of a central pipe extending through the refueling plug down to an elevation slightly above the fuel elements. At the bottom of the central pipe a series of tubes fan out to the core channel positions. These tubes contain the sensor elements and route the sensor leads to the central pipe. The central pipe carries instrument leads through the refueling plug and ends at a multicon-tact electrical connector accessible from the refueling cell. The central pipe has a reduction in diameter in the region that penetrates the refueling plug. There is a corresponding reduction in the diameter of the head penetration. This step serves the dual purpose of providing a shielding step and sealing surface. The portion of this instrumentation pipe penetrating the head is filled with steel shot for shielding purposes.

The procedure for installing the instrument modules to the refueling plug will progress with the refueling plug in its refueling cell storage position. An instrument module is moved beneath the refueling plug and aligned with a head penetration. A grapple lowered through the penetration is attached to the module and is used to pull the module into position in the refueling plug. Alignment pins ensure correct positioning of the instrument packages. The module is firmly connected to the refueling plug at both ends to keep it in its correct position and help prevent vibration problems. The tightening force on the seal is applied by a tightening nut on the top of the center pipe. Means for connecting the lower end of the pipe to the refueling plug structure are being studied. Removing an instrument module is done in the reverse order of that described above.

FIGURE 3-13. DRIVER FUEL INSTRUMENTATION

3.9.2 Instrumentation Under Closed Loop Piping

The right side of Figure 3-13 shows the design for the modules supplying instrumentation to the core positions beneath the closed loop piping. A 2-foot vertical lift will cause the lower end of the module to tilt from beneath the closed loop pipes. This allows the refueling plug to be raised vertically without interference between the instrument packages and the pipes. The motion of the module is guided by a cam slot and follower, as shown in Figure 3-14.

The head penetration is modified from that previously described to allow the module to be raised several feet. The steps in the head penetration are arranged such that the larger diameter is at the top of the plug. The shielding step on the module is attached after the module has been inserted through the refueling plug penetration. This step rests on the corresponding step in the head penetration to support the module and to provide a shielding step.

The axial arrangement of the diameter reductions is such that a 2-foot vertical lift will result in a 1/2-inch radial clearance between the module and the head penetration. This clearance allows the entire module to tilt, which results in a 3-inch movement at the instrument package. This is a sufficient distance to move the instrument packages from beneath the closed loop pipes.

A method also being investigated locates the instrumentation beneath the closed loop pipes on the closed loops. The instrument leads are carried along with those in the closed loop. The details of the head penetration and connector have to be designed to meet the new requirements. Refueling of the affected fuel elements would occur when the loop is removed, a factor which has no appreciable effect on the fuel cycle.

3.9.3 Driver Fuel Instrumentation

Contact has been made with the G. E. Research and Development Center for assistance in the preliminary design of a driver fuel instrumentation package. A proposal from R & DC has been received and the necessary technical requirements are being assembled. Instrumentation sensors to be included in each channel are:

- a. Temperature
- b. Flow
- c. Acoustic

3.9.3.1 Temperature Measurement

It is anticipated that the temperature sensors will be commercially available thermocouples. The system response is expected to have a system response time ≤ 0.7 sec. Attachment methods will be investigated to achieve the desired accuracy and response time.

3.9.3.2 Flow Measurement

There will be one fluid flow sensor per channel. The flow sensor will be a go-no-go gage sensing a loss of flow $< 70\%$ — 80% of rated channel flow. Simplicity and reliability of operation will be stressed in the design. The capabilities of the following type sensors will be evaluated with respect to the above requirements.

- a. Pressure transducer
- b. Rotometer type flow sensor
- c. Flexible flap flow sensor
- d. Turbine flow meter
- e. Magnetic flow meter
- f. Thomes type (heater and thermocouples)

The most promising sensing system will be selected and recommendations will be made for an experimental sensor evaluation program.

INSTRUMENTATION UNIT FOR
POSITIONS UNDER CLOSED LOOP PIPING

F.F.T.F

W. Butler Feb 7, 1968

FIGURE 3-14. INSTRUMENTATION UNDER CLOSED LOOP PIPING

3.9.3.3 Acoustic Sensor

The primary purpose of the sensor is to detect liquid boiling in the channel and cracking of the fuel cladding. Estimates will be made of the required acoustic sensitivity, frequency range, and directionality. The characteristics of the Backup Design Fast Flux Test Reactor as a source of flow noise and other background noise will be reviewed. Environmental effects of the liquid sodium, high temperature, and the neutron density will be considered and factored into the selection of candidate sensor. Since no development work is included in the current contract it is expected that this portion of the effort will result in a preliminary sur-

vey which will indicate the expected feasibility of using acoustic sensing for this application.

3.10 IN-REACTOR HANDLING

Studies were continued on the problems of driver fuel accessibility and other handling operations that must be performed in the reactor vessel. Relocation of the five midcore closed loops to the same radial position has improved the driver fuel accessibility situation. Further work will be conducted on determining the optimum location of the open loops and obtaining an optimum size for the instrumentation tube which extends from each open loop. The size of the refueling grapple is also being reviewed to accommodate all driver fuel element locations.

SECTION IV

DESIGN ANALYSIS

4.1 CONTROL SYSTEM

4.1.1 Introduction

The magnitude of the offset between the control rod drive line and the axis of the poison section is a parameter which has significant influence on the space available in the refueling plug for locating the many penetrations desired. The following analyses were performed on the offset drive design to determine the limitations on offset and the attainable control strength. There are several interactions which make optimization a complicated process; however, with the exception of an evaluation of the deceleration phase of the scram motion which is under review currently, the analyses are sufficient to indicate adequate worth (about 20%) and scram rate (about 1g) can be attained with a 10-inch offset. The deceleration load-

ing is the limiting item and detailed evaluation may require further modification to the configuration which meets the other design conditions and requirements.

Some of the interactions in the drive design are:

- a. Clearance between drive element and channel;
 1. A large clearance (>0.1 inch) reduces the scram resistance but also reduces the volume percent poison in the element;
 2. A large clearance increases the flow requirements to the control channel, most of which is bypass flow;
 3. A small clearance limits the amount of flexural deflection and thermal distortion which can be tolerated.

- b. Poison region wrapper thickness (hexagonal cylinder)—A thick-walled wrapper decreases flexural deflection but also decreases volume percent poison.
- c. Cladding thickness and plenum length—For a given design life and helium release rate, the cladding thickness decreases proportional to plenum length provided. Increasing plenum length results in more flexural deflection and higher annular clearances around the carriage.

4.1.2 Elastic Stability of Drive Line

The drive line is loaded as a column during scram. The column is comprised of two sections AB and BD of different cross sections (see Figure 4-1), AB being a 2- to 3-inch pipe, and BD being a 1-1/4- to 1-3/4-inch solid shaft. The upper section (BD) is restrained at the gas seal (C) against lateral movement by the cylinder (EC) which is cantilevered down from the top head.

If EC is assumed to be sufficiently rigid, the column can be considered as a pin-ended strut at A and C. To maximize the critical buckling load, the small diameter section BC should be as short as possible; i. e., the stroke length of the drive plus clearance. For calculational purposes, this is taken as 50 inches; it may be as short as

40 inches in the actual design. The seal, C, must be above the normal sodium level which is about 70 inches below the top of the head. EC has been selected as 50 inches.

The critical buckling load was computed using Rayleigh's method. (Reference, "Advanced Strength of Materials," Dan Hartog, Chapter 8). A deflection shape was assumed and the critical load found when the change in external work done by the load for a small increment in deflection exceeded the change in internal strain energy for the deflection increment.

By assuming a sinusoidal deflection shape, i. e., $y = \bar{y} \sin \frac{\pi x}{L}$

where

$\bar{y}$ = maximum deflection

x = distance from one end

L = length of the strut = $L_1 + L_2$

the following expression for critical load results.

$$P_{cr} = \frac{\pi^2 EI_1}{L^2} \frac{1}{\frac{L_1}{L} + \frac{L_2 I_1}{L I_2} + \frac{1}{2\pi} \frac{I_1 - I_2}{I_2} \sin \frac{2\pi L_1}{L}} \quad (1)$$

Refer to Figure 4-1 for nomenclature.

The following tabulation shows the critical load for various size rods for AB and BC with $L_1 = 150$ inches, and $L_2 = 50$ inches. E is assumed constant at 20×10^6 psi.

Section 1	Section 2	I_1	I_2	P_{cr}
2.25 in. o.d.—1.75 in. i.d.	1.25 in. diam.	0.8	0.12	2140
	1.5 in.	0.8	0.25	3740
	1.75 in.	0.8	0.46	6900
3.0 in. o.d.—2.5 in. i.d.	1.25 in.	2.05	0.12	2400
	1.5 in.	2.05	0.25	4600
	1.75 in.	2.05	0.46	7580
3.0 in. o.d.—2.0 in. i.d.	1.25 in.	3.2	0.12	3100
	1.5 in.	3.2	0.25	6100
	1.75 in.	3.2	0.46	10,300

FIGURE 4-1. CROSS SECTIONS OF DRIVE LINE

The above procedure was repeated for $y = \bar{y} \sin \frac{\pi x}{2L_1}$ for $0 < x < L_1$ and $y = \bar{y} \cos \frac{\pi(x-L_1)}{L_2}$ for $x > L_1$ with results within 10% of those tabulated. Since Rayleigh's method, which is approximate, under-predicts buckling loads, the above values are conservative.

As discussed later, the design scram load will be in the 300- to 700-lb range. A 3-inch o.d.—2.5-inch i.d. pipe for Section AB and 1.5-inch diameter shaft for BC will result in a margin of safety in the 7 to 10 range. This has been tentatively selected as a balance between drive line weight and drive line stiffness.

4.1.3 Rigidity Requirement on Seal Support Tube EC

Give the point C an arbitrary lateral deflection δ . Let the force at C on the cantilever EC be H_1 to give a deflection δ . Let the force at C on the strut be H . From equilibrium of the strut $H = \frac{P\delta}{L_1}$. For adequate restraint at C, H_1 must be $> H$ or deflection of C relative to E caused by H_1 must be less than $\frac{HL_1}{P}$.

For the cantilever EC,

$$\delta_c = \frac{H_1 L_2^3}{3EI} < \frac{HL_1}{P}$$

or

$$I > \frac{P L_2^3}{3EL_1}$$

For $P \sim 5000$ lb (critical load for strut)

$$L_1 \sim 200 \text{ inches}$$

$$L_2 \sim 50 \text{ inches}$$

$$E \sim 25 \times 10^6 \text{ psi}$$

$$I_{\text{required}} = \frac{5000 \times 50^3}{3 \times 25 \times 10^6 \times 200} = 0.04 \text{ in.}^4$$

Current reference for EC is a cylinder 3-inch o.d., 2.5-inch i.d., $I = 2.06$. For initially straight rod, I is ample.

4.1.4 B₄C Cladding and Gas Plenum

The estimated helium generation rate for 35 volume percent natural B₄C is 5×10^{13} atoms/gm/sec.

For a 36-inch active length poison section this results in a gas pressure in the collection plenum

$$p = \frac{480r}{\ell_p} \times T_d \text{ at } 1000^\circ \text{F}$$

where

r = percent gas released to plenum

ℓ_p = length of plenum

T_d = days exposure at full power.

For a 37-pin hexagonal array

$$d_o = \frac{3.36 - 2h}{6.19} \text{ inches}$$

d_o = o.d. of pin

3.36 = channel width across flats,
inches

h = dimension outer surface of
channel to inner surface of
B₄C wrapper

The average hoop stress in a cylinder
is $S_t = p \frac{(d_o - t_c)}{2t_c}$, t_c = cladding thickness.

Let S_t = yield strength of Type 304
stainless steel at 1100°F

$$= 14,000 \text{ psi}$$

$$S_t = \frac{480 r T_d}{\ell_p} \frac{(d_o - t_c)}{2t_c}$$

$$V^o = \frac{37}{4} \frac{(d_o - 2t_c)^2}{A_{ch}}$$

V^o = volume percent poison

A_{ch} = channel area—10 in.²

From these equations, Figure 4-2 was
generated.

Limited data on powdered B₄C pellets
80% smear density indicate gas release rates
may be less than 10% and cladding deforma-
tion may be negligible for up to 20% burnup
of the B¹⁰ in the B₄C.

Twenty percent peak burnup corresponds
to approximately 1 full-power-year exposure
and is selected as a reference for sizing gas
plenum and cladding thickness along with an
assumed 25% gas release to plenum. This
should provide ample flexibility in selecting a
design exposure for the B₄C after test data
provide more precise information on swelling
and gas release.

4.1.5 Nickel Reflector Segment

4.1.5.1 Thermal Stress Limitations

The nickel segment when adjacent to
the core has an internal heat generation rate
of 4 watts/gm (or 3.44×10^6 Btu/h-ft³).

Since flow around the periphery may be
choked off to minimize bypass flow through
the control drive channels, and since the flow
to the B₄C region must be directed into the
center of the element, it is assumed that
cooling is provided by flow in internal pas-
sages in the nickel segment and (conserva-
tively) cooling on the outer surface is neglected.

To minimize the volume of nickel to be
removed, a symmetrical array of seven cool-
ing holes was selected.

To keep the pressure drop in the 2 to 3
psi range, the cooling passage holes were
sized at 3/8-inch diameter.

The region around each hole was con-
sidered as an equivalent thick-walled hollow
cylinder of 0.65-inch o.d. and 0.375-inch i.d.
(see Figure 4-2a).

Maximum hoop stress in a cylinder is
found on the inside diameter with the relation-
ship:

$$\sigma_{\theta a} = K \frac{E\alpha}{2(1-\nu)} \left[\frac{Q}{2k\rho} \frac{a^2}{2} \left(\frac{2b^2}{a^2} \ln \frac{b}{a} - \frac{b^2}{a^2} + 1 \right) \right]$$

$$= \frac{KE\alpha}{2(1-\nu)} (T_b - T_a)$$

$\sigma_{\theta a}$ = Hoop stress at inside diameter

K = Constant

E = Modulus of elasticity = 27×10^6 psi

α = Coefficient of linear expansion
= 8×10^{-6} per °F

ν = Poisson's Ratio = 0.3

FIGURE 4-2. VOLUME PERCENT B_4C POISON VERSUS WRAPPER INSIDE DIAMETER

- Q = Internal heating rate = 3.44×10^6
 Btu/h-ft³
 k = Thermal conductivity = 29 Btu/h-ft-°F
 a = Inner radius = 0.188
 b = Outer radius = 0.325
 ρ = Density = 0.3 lb/in.³
 T = Temperature

To find K we will compare the above formula to Figure 11-9 of Glasstone.⁽²⁾ Since accuracy is lost at $b/a < 4$ and K is independent of b/a , use $b/a = 10$.

$$\text{From Figure 11-9 } \sigma_{\theta} = \frac{1000 E \alpha Q a^2}{16 k \rho (1 - \nu)}$$

for $b/a = 10$.

Using the formula at $b/a = 10$

$$\sigma_{\theta} = K \frac{E \alpha Q a^2}{8 k \rho (1 - \nu)} \quad (361.4)$$

$$K = \frac{1000}{16} \times \frac{8}{361.4} = 1.38$$

For nickel segment

$$\frac{Q}{\rho k} = 828^{\circ}\text{F} \quad b/a = 1.73$$

$$\begin{aligned} \sigma_{\theta a} &= \frac{27 \times 10^6 \times 8 \times 10^{-6} \times 1.38 \times 0.188^2}{2 \times 0.7 \times 4} \\ &\quad (2 \times 1.73^2 \ln 1.73 + 1 - 1.73^2) \\ &= 2000 \text{ psi} \end{aligned}$$

The allowable number of fatigue cycles is infinite for such a low stress.

FIGURE 4-2a

The thermal stress calculation was also made for a single central hole and it was determined the hole would have to be in the range of 1.5 inch diameter to keep the thermal stresses below the range where fatigue would be a potential failure mode.

4.1.5.2 Bypass Flow Around B₄C Control Rod

An energy balance calculation for three clearance gaps between channel-carriage hexagonals established the required coolant flow. The three cases considered were 0.1-inch gap with no flow in the gap, and 0.1- and 0.2-inch gaps with flow in the gap.

The corresponding necessary coolant flows through the B_4C were 9200, 9200 and 8200 lb_m/h . The flow rates integrate the energy transfer from the heat developed in the B_4C and steel components in the channel.

The geometries analyzed were:

Channel hexagonal across flat	3.36 in.
Channel hexagonal wall thickness	0.1 in.
Wrapper tube wall thickness	0.050 in.
Number of B_4C pins	37
Cladding thickness	0.050 for no gap 0.050 for 0.1 gap 0.045 for 0.2 gap
Internal heating rates	
B_4C	40 watts/gm
Stainless steel	4.72 watts/gm
Component lengths	
B_4C	3 ft
Gas plenum	2.5 ft
Nickel reflector	3 ft
Extension rod (effective length used for hydraulic analysis)	2.5 ft

The calculated bypass flow was as follows:

No Gap	0.10 Gap	0.2 Gap
0	24,000 lb_m/h	145,500 lb_m/h

The no gap case is analogous to the 0.10-gap case with a mechanical design restricting the bypass flow. For flow in the gap the flow rates were computed by equating the pressure drop along the parallel flow paths of annulus and pin bundle.

The design philosophy required restricting the bypass flow to a minimum whenever the control rod is in the full up

or down position. For any intermediate position, the flow is not restricted and full bypass flow is permitted to minimize viscous resistance to scram motion. The required coolant flow in maintaining control rod and reflector segment cooling is $\sim 3.5\%$ of total coolant flow across the reactor vessel. The bypass flow percentage with number of pairs of control rods in the intermediate position are shown in the following tabulation:

	0.1 gap	0.2 gap
One pair	4.7%	6.2%
Two pair	5%	8%
Three pair	5.3%	9.7%

The bypass flow percentage can be controlled by programming the control rod pattern.

For no restriction to flow in the annulus in the 19 pairs of rods in the system, the bypass flow would be 10.4% for 0.1-inch annulus and 38% for a 0.2-inch annulus.

4.1.5.3 Temperature Distribution in B_4C Control Rod

Thermal calculations were performed in support of the design of the B_4C control rod. Figure 4-3 is the calculated B_4C center-line temperature (at the axial midplane) for three heating rates with a cladding thickness of 30 mils. For temperatures greater than 1500°F, constant conductivity is assumed.

Figure 4-4 adjusts Figure 4-3 curves for selection of cladding thicknesses other than 30 mils. Corrections are for 0.4-, 0.5-, and 0.6-inch B_4C outside diameter geometries.

Figure 4-5 illustrates the maximum cladding temperature for 0.4-, 0.5-, and 0.6-inch B_4C diameter rods with several cladding thicknesses. The cladding temperature was calculated at the proper axial location where the maximum temperatures occur and the local conditions have been integrated into the calculations.

FIGURE 4-3. FFTF B₄C CONTROL ROD TEMPERATURES

FIGURE 4-4. FFTF B₄C CONTROL RODS —CLADDING ΔT

FIGURE 4-5. FFTF B_4C CONTROL RODS — CLADDING PEAK TEMPERATURE

The following assumptions were used in the calculations:

Sodium temperature at inlet	700 °F
Sodium temperature at axial midplane	850 °F
Sodium temperature at control rod outlet	1000 °F
Film coefficient—sodium to cladding	20,000 Btu/h-ft ² -°F
Film coefficient—cladding to B ₄ C	1200 Btu/h-ft ² -°F
Thermal conductivity of stainless steel	12.4 Btu/h-ft ² -°F
Density of B ₄ C	1.9 gm/cc
Density of stainless steel	7.91 gm/cc
Heat generation of stainless steel caused by (σ, n)	7.5 watts/gm

4.1.5.4 Scram Resistance

The control rod is subjected to resistance forces during scram because of the viscous forces imparted by the sodium. The normal required cooling flow plus the bypass flow is the total flow through the control rod during operation. During scram, the flow increases by the amount of coolant displaced by the control rod hardware being inserted during shutdown, the resistance force being a function of the rod insertion rate.

Two cases of control rod scram were analyzed. The two cases were:

- (1) Control rod channel connected to the high pressure plenum (100 psi);
- (2) Control rod channel connected to an intermediate pressure plenum (< 100 psi).

A. Case 1—High Pressure Plenum—The resistance force is computed using a pseudo-steady-state approach in which it is assumed that the control rod has

reached a terminal velocity. The pressure increase resisting the free fall of the control rod over the effective cross-sectional area is the resisting force for a specified terminal velocity.

The increased pressure drop across the control rod during scram resulting from the displaced liquid impedes the inlet coolant flow. The reduction of inlet coolant flow is factored into the analysis.

Figure 4-6 is the resultant curve relating control rod terminal velocity with the resisting force impeding rod insertion during scram for a 0.1-inch annular gap configuration.

B. Case 2—Intermediate Pressure Plenum—

The complexity of the problem of flow distribution and pressure distribution increases in this case. Figure 4-7 is a sketch of the system considered. The symbols in Figure 4-7 are:

$\dot{m}_s$ = intermediate plenum supply flow

$\dot{m}_r$ = coolant flow in all of the remaining rows of nickel reflector region.

$\dot{m}_{In}$ = control rod coolant flow

P_{IP} = pressure of the intermediate plenum

P_{hex} = pressure in the control rod hexagonal

The equivalent size of C_r , the nickel reflector flow, is determined by the heating rate of the nickel segments. The equivalent size of C_p , the high to intermediate pressure plenum flow, is determined from the total control rod flow plus the nickel reflector flow. The pressure, P_{IP} , is determined by the operating pressure drop through the control rod channel.

Because of the complexity of the problem, the case where the control rod is scrambled at a rate such that no coolant flow enters

FIGURE 4-6. CONTROL ROD VELOCITY VERSUS FRICTIONAL FORCES

FIGURE 4-7. INTERMEDIATE PLENUM DESIGN CONCEPT

the control rod ($\dot{m}_{In} = 0$) was analyzed. It was found that the intermediate plenum pressure would rapidly approach pressures in the order of pressures calculated in Case 1 across the control rod. It was concluded that no significant advantage in scram rate is gained in the intermediate plenum design concept.

4.1.6 Stress and Deflection of Control

Element

The loading on the drive must be considered during the following situations:

- Scram—acceleration phase
- Scram—deceleration phase
- Shim out

There are two elements per drive mechanism.

Weight of each element = 275 lb

Weight of push rod and piston = 150 lb

Total: $2 \times 275 + 150 = 700$ lb

The objective is to select support point locations and geometry of beam sections so that deflections and clearances are compatible. The reactions at slider bearing points should be as small as possible to minimize wear and galling.

Trial calculations of U_i and U_e (see Figure 4-8) were made for various plenum lengths, wrapper thicknesses, support point location F, and degree of taper of FA.

During acceleration a 500-lb scram force was selected as a tentative reference. Maximum down force $P_s = 500 + 150 = 650$ lb. This is divided between two elements

$$P'_s = \frac{P_s}{2} = 325 \text{ lb.}$$

During shim out P'_{Shim} = weight of element
 = 275 lb, and
 during scram = P'_d .

Means of maintaining this at about 300 lb are currently being pursued.

The following parameters have been selected as a tentative reference:

Length GF = 70

Length FA = 62 inches

Poison length GJ = 40 inches

Plenum length JK = 15 inches

Carriage dimensions G to K = 55 inches

Material - Type 304 stainless steel

Hexagonal Wrapper—

Dimension across flats 2.86 inches

Wall thickness 0.100 inch

$I = 1.03 \text{ in.}^4$

Section KF—Solid Hexagonal—

Type 304 stainless steel

Dimension across flats 2.76 inches

$I = 3.5 \text{ in.}^4$

Section FA—Solid circular cylinder

Straight taper from 2.76- to 2.0-inch diameter

$I = 2.86 \text{ to } 0.785 \text{ in.}^4$

$U_i = 8.2 \times 10^{-6} \times P'e$

$U_e = 125 \times 10^{-6} \times P'e$

For $P' = 325$ lb, $e = 10$ inches

$u_i = 0.039$ -inch clearance
 = 0.150—tolerance and bow
 = 0.150—0.1 = 0.050

$u_e = 0.443$ -inch clearance
 = 0.61—tolerance ~ 0.5

4.1.6.1 Thermal Bow

Because of the variation in heat generation across the B_4C pin bundle, there will

FIGURE 4-8. CONTROL ELEMENT BENDING

be a temperature gradient across the hexagonal wrapper tube.

The variation across the bundle is from 35 watts/gm in B_4C on the outside face of the bundle to 45 watts/gm at the inside face of the bundle.

If ΔT_c is the average coolant temperature rise of sodium passing through the bundle, the temperature gradient across the bundle at the upper end of the B_4C will be

$$\Delta T_r = \frac{45-35}{40} \Delta T_c = \frac{\Delta T_c}{4}$$

When heat transfer to the annulus is neglected, the difference in expansion between the inside wall and outside wall of the wrapper will be

$$\alpha \left(\frac{1}{2} AB + BC \right) \Delta T_c / 4 \sim 8.75 \Delta T_c \alpha$$

R = Radius of curvature of deflection shape

H = Dimension across wrapper = 2.7 in.

$$\frac{AD + \Delta(AD)}{R + H} = \frac{AD}{R}$$

$$R \Delta(AD) = H(AD)$$

$$R = \frac{2.7 \times 70}{8.75 \Delta T_c \alpha} = \frac{21.6}{\Delta T_c \alpha}$$

$$R^2 = (R - \delta)^2 + (AD)^2 / 4$$

Neglecting δ^2

$$\delta = \frac{(AD)^2}{8R} = 0.284 \Delta T_c \times 10^{-3} \text{ in.}$$

For $\Delta T_c = 300^\circ \text{F}$ (same as core) $\delta = 0.085 \text{ in.}$

$$\Delta T_c = 150^\circ \text{F} \quad \delta = 0.042 \text{ in.}$$

4.1.7 Scram Response

A 10\$ scram at 1g with 0.150 second delay has been selected as a referenced based on safety considerations. The scram force and deceleration forces to be applied to the control rod are under review. The scram response has been computed for a constant downward force applied to the push rod over the first 2 feet of the 3-foot stroke. An evaluation of the rate of insertion has been performed assuming a 0.10-inch annulus around the wrapper tube and a 0.2-inch annulus around the nickel reflector and the control element shaft. Recent calculations on flexure of the drive indicate the clearance around the wrapper tube will be greater than 0.10 inch, hence the rate of insertion will be bigger than presently calculated. Since the drive has to be designed to withstand the weight of the two control elements hanging on the offset, a scram force of magnitude equal to the weight of the control elements ($\sim 600 \text{ lb}$) will not impose higher loadings than during extraction. As shown in Figure 4-9 a scram force of about 500 lb on the double element assembly results in a scram rate slightly greater than a 1g free fall with no viscous resistance. This is tentatively selected as the design scram force. A design evaluation of the scram cylinder pressure and venting requirements to meet this has not been completed to date.

FIGURE 4-9. CONTROL SYSTEM SCRAM, TRAVEL VERSUS TIME

The relation used as a basis for Figure 4-9 was evolved as follows.

The equation of motion of the control element and push rod is

$$\ddot{X} = \left(\frac{F_S + W - F_R}{W} \right) g \quad (2)$$

F_S = Scram force

F_R = Viscous resistance

W = Weight of moving assembly.

The viscous resistance can be conservatively represented by the following simplified equation (see Figure 4-6):

$$F_R = F_A + f\dot{X} \quad (3)$$

F_A = Pressure force across the control elements at zero velocity

f = Slope of AB (Figure 4-6)

Substitute Equation (3) in (1) and integrate

$$X = \frac{F + W - F_A}{f} \left\{ t + \frac{W}{fg} [\exp(-fgt/W) - 1] \right\}$$

(basis Figure 4-9)

t = Time from start of motion

4.1.8 Reflector Worth

Figure 4-10 shows the reflector worth of the two-zone core as a function of volume percent poison in the control element for natural B_4C , enriched B_4C , and tantalum. The characteristics of Figure 4-10 have been combined with those of Figure 4-2 to generate Figure 4-11 which shows the control worth versus dimension between inside of the carriage to outside of channel, i. e., carriage wall thickness plus gap dimension plus channel wall thickness. Let this be denoted by h . The required gap width is set by adequate clearance for tolerances, thermal bow, and flexural deflection.

Straightness tolerance is 0.010 inch per foot $\times 4.5$ ft = 0.045

Tolerance on bearing pads = 0.015

Thermal bow = 0.045 inch

For 650-lb force in push rod at the offset a number of parameters were evaluated.

Offset	5 inches	10 inches	5 inches	10 inches	
$t_c =$	0.05	0.05	0.10	0.10	(carriage wall thickness)
$\delta_f =$	0.030	0.060	0.015	0.030	(flexural deflection)
$t_{ch} =$	0.1	0.1	0.1	0.1	(channel wall thickness)
Tol. =	0.06	0.06	0.06	0.06	(mechanical tolerances)
$\delta_T =$	0.045	0.045	0.045	0.045	(thermal deflection)
Min. Gap	0.135	0.165	0.120	0.135	

As a reference $t_{ch} = 0.1$

$t_c = 0.1$

Gap = 0.15

$h = 0.35$ (Inner surface of carriage to outer surface of channel)

Worth of all natural B_4C control system assumed to be \$19.8 for the analysis resulting in Figure 4-11.

FIGURE 4-10. TWO-ZONE CORE CONTROL

**FIGURE 4-11. REFLECTOR WORTH VERSUS RADIAL CLEARANCE
POISON TO CHANNEL NOMINAL WORTH ESTIMATES**

4.2 CORE

4.2.1 Fuel Pin Temperature Distribution

The temperature distribution in the cladding of the corner and side fuel rods is currently being investigated. A computer program has been written to evaluate the velocity distribution for input information in the temperature distribution calculation. This program is currently being debugged.

The major assumptions in the velocity distribution analysis are:

- Single-phase, steady-state, fully developed turbulent flow.
- Coolant is incompressible.
- Decoupled energy and momentum equations (properties in momentum equations are evaluated at coolant bulk temperature).
- No momentum flux across the boundaries of the channel associated with each rod.

The circumferential variation of the velocity is being determined using a modification of the procedure of Nijsing, Gargantini, and Eifler.⁽³⁾ This procedure was developed for the case of a rod in an infinite array, but by modifying the equations to include the channel wall, it can be used for corner and side rods. This procedure will yield the local mean velocity as a function of circumferential position.

The radial velocity distribution will be based upon an annular model proposed by Levy.⁽⁴⁾ This analysis uses the local mean velocity at each circumferential position as the average velocity in an annulus.

The circumferential distribution of a local mean velocity will be determined for a

number of circumferential segments. Each of these segments will then be treated as if it were an annulus to determine the radial velocity distribution corresponding to that circumferential position.

In addition, the eddy diffusivities for momentum in the radial and circumferential directions will be determined in this analysis. These values will then be used to obtain eddy diffusivities for heat. This will allow the calculation of effective conductivity by addition of the thermal conductivity and turbulent conductivity K_t :

where

$$K_t = \rho c_p \epsilon_h$$

ρ = density, c_p = specific heat,

ϵ_h = eddy diffusivity.

The temperature distribution will then be determined by use of a general heat transfer code. This will be a steady-state model with no axial conduction. This model will include the velocity and effective conductivity distributions.

4.2.2 Cladding Temperature Calculation

An evaluation of peak cladding temperature has been made. The analysis predicts a peak local hot spot at cladding inside diameter of 1180°F. Our established design basis is 1200°F.

In the calculations, orificing was assumed to be proportional to the radial power profile with no fuel shuffling and a 5% increase in the power generation of new fuel relative to fuel of average burnup in the region being considered. This is believed conservative since with a fuel management program which used shuffling the new to average peaking factor would be less than with no shuffling.

The tolerances and peaking factors used are all subject to refinement as the design goes into the more detailed phase, and as physical property information is updated; however, the assumptions are believed to be realistic unless a dramatic change in fuel conductivity or gap conductance is required. The consequence of using an unorificed core with and without two-zone enrichment has been briefly reviewed.

For an unorificed core without zone enrichment (radial power flattening), the bulk average outlet temperature would be lowered from 1000°F to about 900°F. For an unorificed core with zone enrichment (assuming radial power peaking factor of 1.2), the corresponding bulk average outlet temperature would be 950°F to meet the cladding temperature limit of 1200°F.

A. Unzoned Core, No Fuel Shuffling

1. Coolant Temperature Distribution in Core

a. Nominal

Inlet to Core 700°F

Outlet to core 1000°F—Bulk average

Assume core operated at constant power since closed loops are at mid radius; this is essentially constant flux at most closed loops throughout the operating cycle.

B.O.C.; M.O.C.; E.O.C.; =
Beginning, middle, end of cycle.

Assume orificing based upon B.O.C.

At end of cycle center channels slightly overcooled. At end of cycle edge channels slightly undercooled.

b. Orificing Scheme

Assume several radial regions.

Center region is nominally perfect.

Edge region—Maximum deviation from perfect orificing is nominally 10% (30 degrees).

Nominal Channel ΔT —Assume 1.05 peaking factor in new fuel relative to fuel of average burnup.

Center Channel—

New Fuel—B.O.C. 315°F
Average Fuel—B.O.C. 300°F

Edge Channel—

New Fuel—B.O.C. 315°F +
30 = 345
Average Fuel—B.O.C. 300°F +
30 = 330

New Fuel—E.O.C. 320°F +
30 = 350

Average Fuel—E.O.C. 305°F +
30 = 335

c. Coolant Temperature—Flow Tolerances

Orifice Tolerances—Allow 10% →
 $0.1 \times 300 = 30$

Local Internal Stream

Rod Diameter 102%
Rod Pattern 102%
Total = $1 + 0.2^2 + 0.2^2 + 0.2^2 =$
1.104 use 1.10
 $1.1 \times 300 = 330^\circ\text{F}$,
30-degree Tolerance

Center Channel ΔT New Fuel—
B.O.C. 315 + 30 = 345

Edge Channel ΔT New Fuel—
E.O.C. 350 + 30 = 380°F

d. Radial Power Gradient

Zero—Center Channel

Edge—Channel Assuming No Mixing
 Inside of Edge Pin ~15% above
 center
 ~450° F
 temperature

Edge Channel $380 + 45 = 425^\circ \text{F}$

e. Tolerance on Inlet Temperature 5°F

2. Fuel Rod Power Distribution

Local power at center of core with over-
 power and local tolerance on f kdt, density
 and enrichment = 17.8 kW/ft (incipient
 melting) new fuel B.O.C.

Peaking Factors

Overpower	1.2
Radial	1.4
Axial	1.2
New to Average	1.05
Tolerance	1.035
TOTAL	2.19, 1.82 without O.P.
Average Power = $17.8/2.19 =$	8.12 kW/ft.

Axial Power Distribution $P/A = 1.2$

$$P_{\text{end}} = 0.654 P_{\text{center}}$$

Radial Power Distribution $P/A = 1.4$

$$P_{\text{edge}} = 0.54 P_{\text{center}} \text{ (For pins in outer row channel closest to } Q_c \text{ of core)}$$

Center Channel, 100% Power, New Fuel;
 B.O.C.

$$\text{Power} = 8.12 \times 1.82 = 14.8 \text{ at center.}$$

$$0.654 \times 14.8 = 9.62 \text{ at top}$$

$$\text{Edge Channel (center)} 14.8 \times 0.54 =$$

$$8.0 \text{ kW/ft (Inner side of channel) and } 0.654$$

$$\times 8 = 5.2 \text{ kW/ft}$$

3. ΔT_f —Temperature Difference Between Cladding Surface and Coolant

H.T. Coefficient $h = 30,000 \text{ Btu/h-ft}^2 - ^\circ \text{F}$

Heat Flux in 0.21-inch outside diameter
 Pin

$$1 \text{ kW/ft} = \frac{10^3 \times 12}{1 \times \pi \times 0.21} \frac{\text{watts}}{\text{ft}^2} \times$$

$$0.05692 \times 60 \frac{\text{Btu}}{\text{hour watt}}$$

$$= 0.062 \times 10^6 \text{ Btu/h-ft}^2$$

$$\Delta T_f = \frac{62}{30} = 2^\circ \text{F/kW/ft}$$

4. ΔT_c —Radial Temperature Rise Through Cladding Thickness

$$k = 12 \text{ Btu/h-ft-}^\circ \text{F}$$

$$Q = 62,000 \text{ Btu/h-ft}^2 \text{ per } 1 \text{ kW/ft}$$

$$\Delta T_c = \frac{Qt}{k} = \frac{62000 \times 0.015}{12} =$$

$$= 6.45^\circ \text{F/kW/ft}$$

Use 6.5°F/kW/ft

5. Circumferential Temperature Distribution in Cladding

Geometry and Wire Wrap

Use $2.5^\circ \text{F per kW/ft}$

6. Radial Temperature in Coolant Across Channel Caused by Power Profile Gradient

Center Channel—Negligible

Edge Channel—Second fuel pin row
 ~15% above center fuel pin

$$\text{Edge Channel—Axial } Q_c 1.15 \times 5.5 =$$

$$6.3 \text{ kW/ft}$$

$$\text{Top } 1.15 \times 3.6 =$$

$$4.2 \text{ kW/ft}$$

7. Cladding Temperaturea. Center Region—Outlet

Coolant Inlet Temperature	700° F
Tolerance on Inlet Temperature	5° F
Temperature Rise Incore—Nominal	300° F
New to Average B. U. Peaking	15° F (5%)
Statistical Tolerance on orifice and fuel bundle geometry	30° F (10%)
	<u>1060° F</u>

$$\text{Hot Channel Factor } \frac{350}{300} = \underline{\underline{1.17}}$$

Assuming fuel rod power at 100% reactor power with 1.05 peaking for new to average burnup and 1.035 peaking for variations in enrichment density and conductivity of fuel, the center region outlet is at 9.62 kW/ft.

The temperature differences cladding to sodium are:

ΔT —Cladding Outside Diameter to Coolant	19°
Radial ΔT in Cladding	63°
Circumferential and Wire Wrap Perturbation	24°
	<u>106° F</u>

$$\begin{aligned} \text{Cladding Hot Spot } 1050 + 106 &= 1156^{\circ} \text{ F} \\ \text{Say } &\underline{\underline{1150^{\circ} \text{ F}}} \end{aligned}$$

b. Edge Region—Outlet

Coolant Inlet	700° F
Tolerance on Inlet	5° F
Nominal Temperature Rise	300° F
Variation from Establishing One Orifice Over Finite Region	30° F (10%)
New to Average B. U. Peaking	15° F (5%)
Tolerance on Orifice and Fuel Bundle Geometry	30° F 10%
Radial Gradient	45° F 15%
	<u>1125° F</u>

Fuel pin power on radially inboard side of outer row fuel channels 5.2 kW/ft

ΔT Cladding Outside Diameter to Coolant	10° F
Radial ΔT in Cladding	33° F
Circumferential and Wire Wrap Perturbation	13° F
	<u>56° F</u>

$$\text{Cladding Hot Spot } 1125 + 56 = 1180^{\circ} \text{ F}$$

B. Unorificed Core, Unzoned, Shuffling Optional

The center channel is limiting because of radial power peaking with no orificing.

With no shuffling and 1.05 peak to average for new to used fuel the center channel outlet sodium is increased by (radial peaking factor - 1) $\times$ Core $\Delta T = 0.4 \times 300 = 120^{\circ} \text{ F}$

$$\begin{aligned} \text{Peak Cladding Temperature - Channel outlet } 1150^{\circ} + 120^{\circ} &= 1270^{\circ} \text{ F} \end{aligned}$$

To meet our established limit of 1200° F the core outlet temperature should be dropped ~ 100° F, 1000° to ~900° F.

With shuffling the 1.05 P/A for new fuel could possibly be reduced to say 1.02.

Results in $0.03 \times 350 = 10^{\circ} \text{ F}$ on hot channel coolant

$$\begin{aligned} 0.03 \times 106 &\sim 3^{\circ} \text{ F on} \\ \text{cladding to coolant } \Delta T &\underline{\underline{13^{\circ} \text{ F Negligible}}} \end{aligned}$$

This does not appear significant from cladding temperature considerations.

C. Unorificed Core, Two Zones, Shuffling within the Zones Optional

Assuming center of core and inner edge of outer zone are same peak these will be critical channels.

1. Center Channel

Increase from Case 1 (P/A radial -1) $\times$ 300°F

Assume P/A radial = 1/2 (December 1966 Design which was 1.4)

Increase in coolant outlet = $0.2 \times 300 = 60^\circ\text{F}$

Peak Cladding Temperature = $1150 + 60 = 1210^\circ\text{F}$

This is marginal and a reduction to 950°F outlet provides some margin to keep below 1200°F.

2. Inner Edge—Outer Zone

Cladding temperature should be ~ same as center of core.

The radial gradient may be a 10° to 15°F effect.

950°F outlet is adequate.

D. Orificed Zone Core

Should be about the same as (a)—Orificed Unzoned Core.

4.2.3 Pressure Drop in Fuel Bundle

The pressure drop across the central fuel bundle has been calculated. Results of the calculation as well as the configuration are shown on Figure 4-12.

The geometry is based upon G.E. Drawing 277E190, for a P/D equal to 1.286- and 0.21-inch diameter fuel pins. The total core flow used was 14.27×10^8 lb/h and the flow through the central fuel bundle was 1.57×10^7 lb/h.

The pressure drops were calculated based upon sudden contractions and sudden expansions. Rounding of the sharp corners will reduce these values significantly.

4.3. HEAD AND VESSEL

4.3.1 Thermal Analysis of the Reactor Head Design

Thermal analysis of the reactor head heat load on the present design was performed to assist in the designing of the head cooling. Table 4-1 is a summary of the heat loads for the components requiring cooling. The bulk heat transfer from radiation, conduction, and convection from the sodium pool to the head structure is incomplete at the present time. Analysis of the problem is in progress.

4.3.2. Vessel Stress Analysis

Stress analyses on two geometries in the flange area of the reactor vessel were performed to determine the allowable axial temperature gradient.

A linear gradient was assumed along the vessel wall from the sodium surface level to the underside of the flange. This length was varied from 12 to 30 inches and a temperature drop of 1100°F along this length taken as an upper bound on the gradient for a sodium design temperature of 1200°F. The stress distribution was similar for each geometry, and Figure 4-13 shows a geometry which permits blockage of the radiation path in the annulus between the head and the vessel.

The stress levels were also computed for the dead weight loads of the vessel internals. The weight used was 1×10^6 lb.

The reaction from the support skirt was considered as an axial line load (1500 lb/in.) and a moment (5655 in.lb/in.) at the center of gravity of the flange ring.

A computer code for stress analysis of axisymmetric structures was utilized.

- POSITION
- 1 — OUTSIDE ENTRANCE
 - 2 — AT CORE SUPPORT PLATE
 - 3 — AT TRANSITION PIECE
 - 4 — AT SHIELD SPACER
 - 5 — AFTER SHIELD SPACER
 - 6 — AT SECOND SHIELD SPACER
 - 7 — AFTER SECOND SHIELD SPACER
 - 8 — AFTER FUEL SPACER
 - 9 — AT END OF FUEL PINS
 - 10 — AFTER THIRD SHIELD SPACER
 - 11 — AT TRANSITION PIECE
 - 12 — AFTER TRANSITION PIECE
 - 13 — UPPER PLENUM

$$\Delta P_{1-2} = 12.00 \text{ psi}$$

$$\Delta P_{2-3} = 0.228 \text{ psi}$$

$$\Delta P_{3-4} = 2.860 \text{ psi}$$

$$\Delta P_{4-5} = 1.295 \text{ psi}$$

$$\Delta P_{5-6} = 6.880 \text{ psi}$$

$$\Delta P_{6-7} = 1.295 \text{ psi}$$

$$\Delta P_{7-8} = 1.935 \text{ psi}$$

$$\Delta P_{8-9} = 56.530 \text{ psi}$$

$$\Delta P_{9-10} = 3.930 \text{ psi}$$

$$\Delta P_{10-11} = 9.750 \text{ psi}$$

$$\Delta P_{11-12} = 3.285 \text{ psi}$$

$$\Delta P_{12-13} = 4.580 \text{ psi}$$

$$\Delta P_{\text{TOTAL}} = \Delta P_{1-13} = 104.508 \text{ psi}$$

FIGURE 4-12. PRESSURE DROP IN FUEL BUNDLE

TABLE 4-1

SUMMARY OF HEAT TRANSFER ANALYSIS FOR FFTF HEAD

Component	Penetration of Type	Area (in ²) each	Height (in.)	Number	Heat Load—Each		Heat Load—Total	
					Btu/h	kW	Btu/h	kW
Refueling Plug	Walls	56.5	30	1	-	-	1,724	0.505
	Closed Loop	0.0722		120				
		1.13	30	6		0.0158	324	0.095
		0.2675		6	54			
	Open Loop	0.0687		228				
		1.179	30	12	66.1	0.01935	794	0.2325
		0.919		12				
	In-Core CR	1.767	30	3	54.2	0.0159	162.8	0.0477
	Driver Inst.	4.35		6	133.2	0.0391		
	Standard and Special T _c	0.8 0.0036	30	6 630	24.5 0.1105	0.00718 3.24 × 10 ⁻⁵	1,011	0.2965
	Internal Heat	1810	-	1	-	-	650	0.1903
	Bulk Rad. + Conv. + Cond.			INCOMPLETE				
Reflector Plug	Walls	56.5 100.0	30	1 1	-	-	4,780	1.4
	Control Drive	1.767	30	20	54.2	0.0159	1,078	0.318
	Reflector Access	15	30	6	458	0.1345	2,750	0.816
	Internal Heat Gen.	3730	-	1	-	-	1,340	0.393
	Bulk Rad. + Cond. + Conv.			INCOMPLETE				
Head	Walls	302	-	1	-	-		
		100	-	1	-	-	12,290	3.6
	Internal Heat Gen.	19,900	-	1	-	-	7,150	2.09
	Bulk Rad. + Cond. + Conv.		INCOMPLETE					
Vessel	Wall	425	-	1	-	-	13,800	4.045

FIGURE 4-13. GEOMETRY PERMITTING BLOCKAGE OF THE RADIATION PATH IN THE ANNULUS BETWEEN THE HEAD AND THE VESSEL

The high stress locations are at the geometric discontinuity B, (Figure 4-13) and the temperature discontinuity A where the axial direct plus bending stresses peak.

For L = 24 inches

$$\sigma_{XA} = 47 \text{ ksi} \quad T_A = 1200^\circ \text{F}$$

$$\sigma_{XB} = 64 \text{ ksi} \quad T_B = \text{low.}$$

σ_{XA} controls

Stress Range = 47 ksi operating 1200° F
to 16 ksi refueling 400° F
31 ksi

$$S_{rev} = 1/2 S_r = 15.5 \text{ ksi}$$

Allowable number of fatigue cycles for this case is =5000 (Case 1331 ASME Code).

For L = 30 inches

$$\delta_{XA} = 33 \text{ ksi} \quad 1200^\circ \text{F}$$

$$= 13 \text{ ksi} \quad 400^\circ \text{F}$$

$$S_r = 20 \text{ ksi}$$

$$S_{rev} = 1/2 S_r = 10 \text{ ksi}$$

Allowable number of fatigue cycles for this case is =50,000.

Design of head and vessel cooling should spread temperature gradient over about 30 inches.

4.3.3 Structural Requirements on Top Head from DBA Loading

Figure 4-14 is a sketch of one of the current head configurations. A review was made of the following:

- Loadings
- Main holddown bolts
- Stiffeners and strength requirements

- Joints between plug, ring, and outer head.

4.3.3.1 Loadings

The dynamic loading from shock wave and water hammer is based upon the estimate that 3% of the charge energy is delivered to the head.

$$\text{Design Energy Release} = 1500 \text{ MW-sec}$$

Kinetic Energy Transmitted to Head (E_K)

$$45 \text{ MW-sec} = 3.98 \times 10^8 \text{ in./lb}$$

The following are the weight estimates for the head sections:

Inner Plug	11,000 lb
Ring	30,000 lb
Outer Head	190,000 lb
	230,000 lb = 115 tons.

The average specific impulse on the head is $\bar{I}_S$.

To deliver as kinetic energy E_K

$$E_K = \frac{1}{2} \frac{W}{g} V^2 = \frac{1}{2} \frac{W}{g} \frac{I_S^2 A g^2}{W}$$

$$A = \text{area at i. d. of vessel} = 28,900 \text{ in.}^2$$

$$I_S = \frac{2E_K W^{1/2}}{g A^2} = 23.8 \text{ lb sec/in.}^2$$

Since impulse loading will not be uniform, the shear connectors between plug and head are designed for $\sim 2 \times \bar{I}_S = 50 \text{ lb sec/in.}^2$ to provide a margin for error.

Initial velocity of head after impulse is given by

$$E_K = \frac{1}{2} \frac{W}{g} V_o^2$$

$$\text{gives } V_o = 1150 \text{ in./sec} \sim 100 \text{ ft/sec.}$$

FIGURE 4-14. SKETCH OF CURRENT HEAD CONFIGURATIONS

The inertia loading as the head is decelerated by the holddown bolts is determined by the force in the holddown bolts. If F_B is bolt load, $\ddot{X} = \frac{F_B g}{W}$.

The pressure in the expanding vapor bubble is limited by the pressure to cause gross yielding of the reactor vessel.

$$\sigma_{\text{Hoop}} = \frac{pr}{t}$$

$$p = \frac{t}{r} \sigma_{\text{Hoop}} = \frac{0.75}{100} \times 60,000 = 450 \text{ psi}$$

4.3.3.2 Main Holddown Bolts

Assume bolt material is Type 304 stainless steel at 200°F.

Design elongation 20%.

Strain energy at $e = 0.2$.

$$U_S = \frac{1}{2} (25 + 61) \times 1000 \times 0.2$$

$$= 8600 \text{ in. lb/in.}^3$$

Bolt volume required

$$= \frac{E_K}{U_S} = \frac{3.97 \times 10^8}{8600} = 46,500 \text{ in.}^3$$

48 Bolts—3.5 in. diameter—100 in. long

Maximum bolt load = $0.585 \times 10^6 \text{ lb/bolt}$
or $28 \times 10^6 \text{ lb total}$

4.3.3.3 Head Strength and Stiffness Requirements

At the end of bolt stretch $F_B = 28 \times 10^6 \text{ lb}$.

Inertia load of head from deceleration:

$$\ddot{X} = \frac{Fg}{W} = 122g$$

Equivalent uniform pressure = 850 psi inside vessel, 240 psi outside vessel.

Adding vapor bubble pressure these become 1300 psi and 240 psi.

To ensure gross plastic deformation does not occur so that rotation of the edge will not tend to twist the holddown bolts, designing the head not to exceed yield stress for a uniform 1000 psi over its entire area should provide ample rigidity.

Use $M = \frac{pA(3+\nu)}{16\pi}$ for bending moment at center of circular plate and $S = \frac{Mc}{I}$

for $S = 30,000 \text{ psi}$, $p = 1000 \text{ psi}$, $A = 40,000 \text{ in.}^2$.

$$t_f d = 87.5$$

satisfied by

$$t_f = 3 \text{ in. and } d = 30 \text{ in.}$$

4.3.3.4 Joint Between Plug and Ring

During the deceleration of the head at 122g the shear force in the ring must react the inertia force of the head

$$F_S = \frac{W \bar{X}}{g} = 11,000 \times 122 = 1.34 \times 10^6 \text{ lb}$$

For stainless steel ring - allowable shear stress (f_{sa}) = 12,500 psi.

$$\text{Ring area (circumferential) required} = F_S / f_{sa} = A_S$$

$$A_S = 107 \text{ in.}^2$$

At impact the ring must carry any loads caused by the impulse on the plug being higher than on the rest of the head. A preliminary review of this using as reference "Handbook of Engineering Mechanics," Flugge, p. 56.22 indicates that this may be a serious loading. The degree of seriousness depends upon the excess of the impulse on the plug over the average impulse on the rest of the head and the duration of the pulse. The crushing of the structure under the head may spread the pulse and lower its peak. This problem needs further evaluation. As an alternate, a series of six radial beams over the plug and head has been determined to be structurally feasible if the shear key configuration is determined inadequate.

4.4 SYSTEM DYNAMICS

In the previous Quarterly Progress Report,⁽¹⁾ the analysis of the sodium temperature response in the vessel to four mild transients was reported. A fifth mild transient was analyzed during the past quarter, namely loss of flow in one loop without scram. The sodium temperature response throughout the primary system to this accident is described in subsection 4.4.1.

In addition to the mild transients, three severe transients were analyzed. These included:

- a. Reactor scram with all pumps running.

- b. Reactivity ramp of 10¢/sec without scram.
- c. All pumps trip and coast down without scram.

The analyses of the short-term sodium temperature response in the vessel for these accidents are reported in subsection 4.4.2.

4.4.1 Loss of Flow in One Loop without Scram

A fifth mild accident was calculated in which it was assumed that flow stops in one loop while operation continues without scram and with the other two loops in service. The flow in the failed loop goes to zero linearly in 2 seconds, while the flow in the two operating loops increases linearly to 1.16 normal flow* in 2 seconds.

The response to this transient was calculated for 80 seconds with the entire FFTF Backup system mocked up. The IHX, heat sink, and external piping time delays were included. Hence, temperature responses beyond the transit times of the primary and secondary systems were tested accurately.

System responses, including sodium temperatures in the vessel, power, core temperatures, and reactivity feedbacks, are plotted in Figures 4-15 through 4-18.

In the early part of the transient (first 2.5 seconds), the negative feedback from the rising core sodium temperature dominates the core power response (Figure 4-16) and forces the core power down rapidly. Although the total core coolant flow has decreased, the fuel temperature (Figure 4-17)

*This increase in flow rate was estimated from typical pump characteristics of the type recommended for the FFTF Backup Design.

FIGURE 4-15. SODIUM TEMPERATURES ABOVE CORE—LOSS OF FLOW IN ONE LOOP WITHOUT SCRAM

FIGURE 4-16. FRACTIONAL POWER—LOSS OF FLOW IN ONE LOOP WITHOUT SCRAM

FIGURE 4-17. CORE TEMPERATURES—LOSS OF FLOW IN ONE LOOP WITHOUT SCRAM

FIGURE 4-18. FEEDBACK REACTIVITIES—LOSS OF FLOW IN ONE LOOP WITHOUT SCRAM

drops since the drop in power overcompensates for the loss of heat transfer to the coolant. The resulting positive reactivity feedback from the fuel (Figure 4-18) dominates the core power response from 2.5 to 4 seconds. After 4 seconds, the net effect of the positive fuel expansion and Doppler feedback and the negative coolant feedback is about constant at $\sim -3\%$.

The core inlet sodium temperature (Figure 4-15) shows the effect of time-delayed system responses outside of the vessel. The primary IHX outlet sodium temperature starts rising as the primary loop flow increases. This rise is first seen in the core inlet after 4 seconds, which is the transit time from IHX outlet to core inlet. The small increase in the rate of change of the core inlet temperature beyond 45 seconds results from the propagation of the effect of the change in vessel outlet temperature all the way around the secondary sodium system. The time after the start of the transient for this effect to appear at the core inlet includes the time for the vessel outlet to start changing temperature (~ 2 seconds), the time lag from the vessel outlet to the IHX primary inlet (~ 8 seconds), then the transit time through the entire secondary piping (~ 30 seconds), and finally the time lag from the IHX primary outlet back to the core (~ 4 seconds).

4.4.2 Response to Severe Transients

This subsection describes the short-term sodium temperature response of the FFTF system to various severe transients. These transients are associated with low probability accidents which the system must be designed to withstand should they occur sometime during the lifetime of the plant.

The temperature responses of the sodium at the top of the active core, at the channel outlet, and at the vessel outlet are

presented for the four accidents listed in subsection 4.4.1. In addition, the driving forces which cause the above sodium temperatures to change are shown to get a more complete picture of the system temperature response. These driving forces include reactivity inputs and feedbacks, power level, average fuel temperature, average cladding temperature, average core sodium temperature, and sodium flow rate.

4.4.2.1 Reactor Scram with All Pumps Running

The following events lead to the sodium temperature response at the core outlet, channel outlet, and vessel outlet (for 20% mixing) shown in Figure 4-19.

Scram rods start to drop at time, $t=0$ (10\$ scram worth inserted). Sodium coolant remains at full flow.

The negative temperature gradients are large, and equal to the values for the delayed coastdown following scram. However, the present accident is more serious since the total drop in coolant temperature above the core and at the vessel outlet are so large. Figure 4-19 shows a drop of $\sim 250^\circ\text{F}$ in 6 seconds for the core and channel outlets and a drop of $\sim 200^\circ\text{F}$ over a 10-second period for the vessel outlet.

Figures 4-20 through 4-22 show the core temperatures, fractional power and reactivity effects associated with this transient.

The inlet temperature was held constant throughout the calculation. For this transient, the assumption of constant inlet is exact for 17 seconds at full flow because 17 seconds is the transit time of the primary sodium around the system external to the vessel.

FIGURE 4-19. SODIUM TEMPERATURES ABOVE CORE—REACTOR SCRAM WITH ALL PUMPS RUNNING

FIGURE 4-20. CORE TEMPERATURES—REACTOR SCRAM WITH ALL PUMPS RUNNING

FIGURE 4-21. FRACTIONAL POWER—REACTOR SCRAM WITH ALL PUMPS RUNNING

FIGURE 4-22. REACTIVITY—REACTOR SCRAM WITH ALL PUMPS RUNNING

4.4.2.2 Reactivity Ramp of +0.10\$/second without Scram

The second accident analyzed was a 10¢/second ramp reactivity insertion without scram starting at full power and full flow. The ramp is assumed to continue throughout the duration of the transient while the coolant flow is assumed to remain constant. The results are shown in Figures 4-23 through 4-26.

The sodium temperature gradients at the core and channel outlets eventually reach a magnitude of 17°F/sec, and extrapolation of Figure 4-23 indicates that sodium boiling at the core outlet would commence at 50 seconds. The sodium temperature gradient at the vessel outlet (with 20% mixing and with 10% bypass flow) reaches 13°F/sec.

The average fuel temperature (Figure 4-24) increases significantly as it follows the rising power level (Figure 4-25). The peak fuel would have melted during the early part of this transient. (The details of the peak fuel behavior can be analyzed by the FORE computer code.) The reactivity ramp increase is shown to be strongly dampened by the compensating negative reactivity feedbacks (Figure 4-26). The net reactivity is quite small, therefore, and the power level rise is relatively slow.

4.4.2.3 All Pumps Trip and Coast Down without Scram

The accident in which all pumps coast down but scram fails to occur was examined for two pump coastdown characteristics. The two coastdown curves used are characteristic of the FERMI and EBR-II pumps. The FERMI characteristics have been recommended for the FFTF Backup Design despite some advantage to the slower EBR-II coastdown in the accident considered in this section.

The results for coastdown curve 1 (FERMI) are shown in Figures 4-27 through 4-30. The results of curve 2 (EBR-II) are shown in Figures 4-31 through 4-34. For both cases, the core outlet and channel outlet sodium temperatures rise rapidly. For curve 1 sodium boiling (boiling point $\approx 1600^\circ\text{F}$) starts at 8 seconds; for curve 2 boiling is delayed until 30 seconds. The effects of sodium boiling on reactivity and on heat transfer for curve 1 coastdown transient were not taken into account. The vessel outlet temperature (for 20% mixing) does not rise rapidly since the mixing time constant is increasing because of the flow coastdown.

FIGURE 4-23. SODIUM TEMPERATURE ABOVE CORE— + \$0.10/sec REACTIVITY RAMP NO SCRAM—FULL FLOW

FIGURE 4-24. CORE TEMPERATURES— + \$0.10/sec REACTIVITY RAMP NO SCRAM—FULL FLOW

**FIGURE 4-25. FRACTIONAL POWER— + $0.10/\text{sec}$ REACTIVITY RAMP NO SCRAM
—FULL FLOW**

FIGURE 4-26. REACTIVITY— + $0.10/\text{sec}$ REACTIVITY RAMP NO SCRAM—FULL FLOW

FIGURE 4-27. SODIUM TEMPERATURE ABOVE CORE—ALL PUMPS TRIP WITHOUT SCRAM—COASTDOWN CURVE No. 1

**FIGURE 4-28. CORE TEMPERATURES—ALL PUMPS TRIP WITHOUT SCRAM—
COASTDOWN CURVE No. 1**

FIGURE 4-29. FRACTIONAL POWER—ALL PUMPS TRIP WITHOUT SCRAM—COAST-DOWN CURVE No. 1

FIGURE 4-30. REACTIVITY — ALL PUMPS TRIP WITHOUT SCRAM — COAST-DOWN CURVE No. 1

**FIGURE 4-31. SODIUM TEMPERATURES ABOVE CORE—ALL PUMPS TRIP
WITHOUT SCRAM—COASTDOWN CURVE No. 2**

**FIGURE 4-32. CORE TEMPERATURES—ALL PUMPS TRIP WITHOUT SCRAM—
COASTDOWN CURVE No. 2**

FIGURE 4-33. FRACTIONAL POWER—ALL PUMPS TRIP WITHOUT SCRAM—COAST-DOWN CURVE No. 2

FIGURE 4-34. REACTIVITY—ALL PUMPS TRIP WITHOUT SCRAM—COASTDOWN CURVE No. 2

SECTION V

NUCLEAR CHARACTERISTICS

The nuclear analysis and design efforts were directed toward an improved concept based on two core zones with different fissile enrichments. The zoned core has significant advantages in increased reflector control strength and operational flexibility, while maintaining a high neutron flux level (in the $0.95 - 1.0 \times 10^6$ n/cm²-sec range).

5.1 CORE DESIGN

The zoned FFTF Backup Design consists of 127 in-core hexagonal channels of which 105 are driver fuel locations. Twelve of the positions are open loops containing 75% of the fuel content of a driver bundle. The core arrangement is described in Table 5-1 and is shown in Figure 5-1. The calculational model used in this study is shown in Figure 5-2.

An equilibrium fuel cycle was assumed for the study with an average bundle discharge of ~57,000 MWd/Te resulting in a peak burn-up of 80,000 MWd/Te. The refueling interval is 60 full power days with a reactivity swing of 2.3% $\Delta k/k$.

The region dependent batch loading (i.e., 1/the fraction of fuel replaced at each reload), and isotopic atom fractions for the two enrichment zones are given in Table 5-2.

The peak linear power density allowable is 18.0 kW/ft. If an overpower allowance of 1.2 and a local peaking factor of 1.1 is assumed, the peak linear power density for normal operation becomes 13.6 kW/ft. Since the peak power density in both zones 1 and 2 is at its highest at the beginning of an equilibrium burnup cycle, when the greatest amount of peripheral control is inserted, the zoned core must be designed so that the beginning of cycle condition satisfies the limit of 13.6 kW/ft peak linear power density.

TABLE 5-1

CORE ARRANGEMENT

Core Thermal Power	400 MW
Core Height	36 inches
Core Composition	
Secondary Control	3 channels
Closed Loops	7 channels
Open Loops	12 channels
Driver Fuel	
Enrichment Zone 1	63 channels
Enrichment Zone 2	42 channels
Total	127 channels
Driver Fuel	
No. Fuel Pins/Bundle	127
Fuel Pin o.d.	0.21 inch
P/D	1.286
Bundle Size (flat-to-flat)	3.356 inches
Cell Size (includes 0.025-inch sodium gap between channels)	3.381 inches
Driver Fuel Cell Composition	
Volume Fraction, Fuel	0.326
Volume Fraction, Sodium	0.406
Volume Fraction, Steel	0.154
Volume Fraction, Incoloy 800	0.114
Open Loops	
Volume Fraction, Fuel	0.244
Volume Fraction, Sodium	0.466
Volume Fraction, Steel	0.116
Volume Fraction, Incoloy 800	0.174
Closed Loops	
Volume Fraction, Fuel	0.049
Volume Fraction, Sodium	0.464
Volume Fraction, Steel	0.487
Secondary Control Locations	
Volume Fraction, Sodium	0.667
Volume Fraction, Steel	0.333

IN-CORE CONTROL

CLOSED LOOPS

1 ZONE 1 DRIVER FUEL

1 ZONE 1 DRIVER FUEL
(BUT COULD BE ZONE 2)

2 ZONE 2 DRIVER FUEL

2 ZONE 2 DRIVER FUEL
(BUT COULD BE ZONE 1)

(2) OPEN LOOPS (TENTATIVE)

FIGURE 5-1. CORE ARRANGEMENT

(DIMENSIONS ARE FOR HOT
OPERATING CONDITIONS)

REGION →	NUMBER OF HEXAGONAL ASSEMBLIES				
	0	1	2	3	4
ZONE 1, DRIVER	0	35	16	12	0
ZONE 2, DRIVER	0	0	0	18	24
CLOSED LOOPS	2	0	5	0	0
OPEN LOOPS	0	0	0	0	12
CONTROL	0	0	3	0	0
TOTAL	2	35	24	30	36
					→ 127

FIGURE 5-2. CALCULATIONAL MODEL

TABLE 5-2
REGION DEPENDENT BATCH LOADINGS AND
FUEL ISOTOPIC ATOM FRACTIONS
 (Refer to Figure 5-2 for location of regions.)

Batch-Loadings

<u>Region</u>	<u>Zone 1</u>	<u>Zone 2</u>
1	4.00	~
2	4.74	~
3	5.54	4.04
4*	~	5.27

* The 12 open loops are included in the refueling scheme for region 4.

Atom Fractions

<u>Initial Clean Bundle</u>	<u>Zone 1</u>	<u>Zone 2</u>
U-238	0.7646	0.6718
Pu-239	0.2048	0.2855
Pu-240	0.0282	0.0394
Pu-241	0.0024	0.0033
	1.0000	1.0000

<u>Discharged Bundle</u> (Average for Zone)	<u>Zone 1</u>	<u>Zone 2</u>
U-238	0.7264	0.6468
Pu-239	0.1722	0.2393
Pu-240	0.0372	0.0489
Pu-241	0.0031	0.0040
Pu-242	0.0002	0.0002
F. P. P.	0.0609	0.0608
	1.0000	1.0000

<u>Beginning of Equilibrium</u> <u>Burnup Cycle</u> (Average for Zone)	<u>Zone 1</u>	<u>Zone 2</u>
U-238	0.7501	0.6623
Pu-239	0.1917	0.2667
Pu-240	0.0319	0.0434
Pu-241	0.0026	0.0035
Pu-242	0.0001	0.0001
F. P. P.	0.0236	0.0240
	1.0000	1.0000

<u>End of Equilibrium</u> <u>Burnup Cycle</u> (Average for Zone)	<u>Zone 1</u>	<u>Zone 2</u>
U-238	0.7414	0.6570
Pu-239	0.1843	0.2570
Pu-240	0.0340	0.0454
Pu-241	0.0028	0.0037
Pu-242	0.0001	0.0001
F. P. P.	0.0374	0.0368
	1.0000	1.0000

This limit has been met in the present zoned core design while assuming that no fuel bundle is moved from its initial loaded location. The shuffling of partially burned fuel to the core center would, of course, reduce the peak linear power density.

Sixteen energy group cross sections were generated using previous FFTF 60-group spectra for the core and reflector to condense the latest GE 60-energy-group cross sections on file BAS03. The most important differences between BAS02, used in earlier FFTF nuclear analyses, and BAS03 are the inclusion of higher Pu-239 alpha value and lower U-238 capture data.

5.2 DESIGN PARAMETERS

The radial power distributions at the beginning and end of an equilibrium burnup cycle are shown in Figure 5-3. The calculated average peak linear power density in both zones must be such that the peak power generated in a fresh, newly-loaded bundle at the beginning

of the cycle will not exceed 13.6 kW/ft. The peak total flux possible at the beginning of the cycle with this design and with no fuel shuffling is 0.95×10^{16} n/cm²-sec. If fuel shuffling were used in zone 1, the peak flux could be increased to 1.01×10^{16} n/cm²-sec.

Table 5-3 lists the basic nuclear parameters calculated or estimated in this preliminary study.

5.3 OPERATIONAL FLEXIBILITY

A significant advantage of the zoned core is that it offers much more operational flexibility than the unzoned core. Some of the options available to the reactor operator are the following.

5.3.1 Fuel Shuffling to Increase the Peak Neutron Flux

By not loading new fuel bundles into the first two rows of zone 1 driver fuel (~17 bundles), it is possible to increase the peak neutron

TABLE 5-3
NUCLEAR PARAMETERS FOR THE
ZONED FFTF BACKUP DESIGN CORE
(No fuel shuffling assumed)

	<u>Beginning of Cycle</u>	<u>End of Cycle</u>
k_{eff} , uncontrolled	1.025	1.002
$\hat{P}$, kW/ft (at $k = 1.0$)		
Zone 1	13.6	~12.7
Zone 2	13.45	~13.1
$\hat{\phi}$, n/cm ² -sec (at $k = 1.0$)	0.95×10^{16}	$\sim 0.91 \times 10^{16}$
Reflector Control Worth (at 59 vol % B ₄ ¹⁰ C)		~\$35
Doppler Coefficient, $T \frac{dk}{dT}$ (at $k = 1.0$)		
Sodium-In	-0.0050	-0.0054
Sodium-Out	~-0.0030	-0.0034
Total Core Sodium Void Effect		~- \$2.2

FIGURE 5-3. FUEL POWER DENSITY—BEGINNING OF BURNUP CYCLE AND END OF BURNUP CYCLE

flux by $\sim 6\%$. Fuel shuffling requires that partially burned fuel from other zone 1 locations be loaded into the first two rows instead of fresh bundles. This means that the enrichment at the peak power density location is lower and, hence, a higher flux is required to reach the peak linear power density limit. Fuel shuffling in zone 2 is also a possibility.

5.3.2 Compensation of Experimental Reactivity by Fuel Loading

It is possible to compensate for changes in the experimental reactivity by changing the number of zone 2 bundles. For example, the addition of 12 zone 2 bundles (and removal of 12 zone 1 bundles) increases the core reactivity by $\sim 1\% \Delta k/k$. Conversely, the removal of 12 zone 2 bundles (and addition of 12 zone 1 bundles) decreases the core reactivity by $\sim 1\% \Delta k/k$. Examination of Figure 5-1 shows that the addition of 12 zone 2 bundles will not significantly change the peak power density of zone 2. This is possible since the bundles can be added in the same row as the bundles in which the peak power density for zone 2 occurs.

Relatively simple adjustments in the zoned fuel loading are possible to correct for errors in operational reactivity or power distribution predictions.

A combination of fuel shuffling and zone 2 reloading are possible. For example, the power level of the reactor could be increased by a combination of fuel shuffling and zone 2 reloading.

Because of the additional flexibility, the transition from the initial clean loading to an equilibrium fuel cycle may be easier than for the unzoned core.

5.4 COMPARISON OF UNZONED AND ZONED CORES

To evaluate the advantages and disadvantages of the two zone enrichment core for

the FFTF Backup Design, it is compared here with the unzoned core. The unzoned core design contains 12 more driver fuel bundles than the zoned core, while all other dimensions and number of channel-types remain the same.

It should be noted that the unzoned core design requires fuel shuffling to meet the design limit on peak linear power density of 13.6 kW/ft, while the zoned core design does not require fuel shuffling. Since the magnitude of the peak neutron flux which can be achieved is dependent on whether fuel shuffling is assumed or not, and since the fuel in the zoned core could be shuffled almost as easily as the fuel in the unzoned core, the comparison of the zoned and unzoned core will assume that fuel shuffling is allowed in the zoned core also. This will allow a consistent comparison of inherent nuclear properties even though the design assumption of the zoned core is no fuel shuffling.

The most important difference between the two core design is the total reflector control worth, with the zoned core having $\sim 24\%$ more available worth than the unzoned core. With equal refueling schemes, the zoned core has a slightly higher peak total neutron flux than does the unzoned core.

Table 5-4 lists some of the important nuclear parameters for both the zoned and unzoned core designs. The data presented in the previous quarterly report was based on analyses using BAS02 cross section data while all zoned core calculations used BAS03 cross section data. To indicate the differences from cross section data, Table 5-4 includes unzoned core parameters which were calculated with BAS02 cross sections. Because of uncertainties in the physical properties of irradiated B_4C at high temperatures, tantalum is considered as a possible alternate control material. Several calculated values for a tantalum control system are also listed in Table 5-4.

5.5 DOPPLER COMPARISON WITH PNL

A series of Doppler calculations were completed on a simplified FFTF Backup model for comparison with PNL Doppler calculations. The Doppler coefficients were calculated for this model by successive k calculations (U-238 at 700° and 1400°K); GE using the BISYN code, a 2-D synthesis diffusion theory code, with 16 energy groups, and PNL using the 2D13 diffusion code (in R-Z geometry) with 13 energy groups. A comparison of the two calculations

are shown in Table 5-5 indicating good agreement.

5.6 CRITICAL EXPERIMENT

The feasibility and calculated strength of the reflector control in the FFTF Backup Design can be verified only by a critical experiment. The proposed critical experiment, reported in the last quarterly report, is an effort to obtain experimental data against which to compare and calibrate analytical techniques and nuclear design data, consistent with the PNL Phase B critical program.

TABLE 5-4
NUCLEAR PARAMETERS FOR THE ZONED AND UNZONED
FFTF BACKUP CORE DESIGNS ASSUMING FUEL SHUFFLING

(Values are for an equilibrium burnup cycle)

	BAS03 Cross Sections		BAS02 Cross Sections
	Zoned	Unzoned	Unzoned
Total Number of In-Core Channels	127	139	139
Burnup Reactivity, $\Delta k/k$	2.3%	2.3%	2.3%
Peak Linear Power Density kW/ft at Limiting Condition (b.o.c.)	13.6	13.6	13.6
Peak Neutron Flux, $n/cm^2\text{-sec}$ at Maximum Condition (b.o.c.)	1.01×10^{16}	0.99×10^{16}	0.99×10^{16}
Average U-238 Doppler Coefficient $T \frac{dk}{dT}$, over Burnup Cycle			
$T \frac{dk}{dT}$, Sodium-In	-0.0052	-0.0055	-0.0061
$T \frac{dk}{dT}$, Sodium-Out	-0.0032	-0.0034	-0.0038
Sodium Loss Effect			
Total Core	-2.2\$	-3.1\$	-5.1\$
Maximum Positive Effect		+0.64\$	+0.43\$
Reflector Control Strength			
$B_4^{10}C$, 59 vol %	35\$	28\$	29\$
Ta , 59 vol %	15.6\$	13.4\$	
$B_4^{10}C$, 35 vol %	32.5\$		

TABLE 5-5
COMPARISON OF GE AND PNL CALCULATED U-238 DOPPLER COEFFICIENTS

	PNL	GE
Sodium-In, 10% Pu-240, No Fission Products, No Reflector Control	---	-0.00569
Sodium-In, 12% Pu-240, Fission Products in, No Reflector Control	-0.00568	---

SECTION VI

NUCLEAR SHIELDING AND HEATING

6.1 PENETRATION SHIELDING

6.1.1 Shielding Guidance for the Design of the FFTF Top Head

To assist designers in making preliminary layouts of the FFTF top head, a parametric study of the number of steps and thickness of annular gaps for plugs shielding duct-type penetrations ranging from 1 inch to 220 inches (18 feet) in diameter was completed. This study assumed the geometry illustrated by the following sketch:

In the preceding figure, good design practice requires that

$$A \geq 4B$$

$$L_1 = L_2 = L_3 = L_4$$

$$E \leq B$$

Figures 6-1 through 6-4 illustrate the calculated dose rates at the exit of the upper annular gap as a function of:

- Annular width or gap between plug and penetration.
- Plug diameter.
- Number of steps in the plug.

For the very large diameter plugs, the annulus was assumed to be a long slot, in order to obtain an upper limit on the expected dose rate at the annular exit. The equation used to calculate annular gap attenuation is applicable only if:

$$R^2 - r^2 \ll L^2$$

This condition is violated for gaps exceeding 0.5 inch where the plug diameters exceed 84 inches. However, as shown by Figures 6-3 and 6-4, the annular gap attenuation calculation predicts a higher dose rate than the slot attenuation calculation. Hence, the results from the annular calculation appear to be conservative, even when $R^2 - r^2$ is not small compared to L^2 .

Special notes relating to each of the plug diameter calculations are given in the following paragraphs:

6.1.1.1 One-Inch Diameter Plug

With no steps a minimum exit dose rate of 3,800 mR/h was calculated assuming a 0.1-inch annular gap. This is 151 times the design limit.

With one step, if one chooses the 0.42-inch gap indicated by Figure 6-1, then the upper stepped diameter becomes

FIGURE 6-1. DOSE RATE VERSUS ANNULAR WIDTH (GAP) FOR A ONE STEP PLUG HAVING A 1-, 3-, OR 6-inch DIAMETER

FIGURE 6-2. DOSE RATE VERSUS ANNULAR WIDTH (GAP) FOR 48-inch DIAMETER PLUGS HAVING ONE AND TWO STEPS

FIGURE 6-3. DOSE RATE VERSUS ANNULAR WIDTH (GAP) FOR 84-inch DIAMETER PLUGS HAVING TWO AND THREE STEPS

FIGURE 6-4. DOSE RATE VERSUS ANNULAR WIDTH (GAP) FOR THE 220-inch DIAMETER TOP SHIELD PLUG ASSUMING EITHER TWO OR THREE STEPS

$$1 \text{ inch} + 8 \times 0.42 = 4.36 \text{ inches (A = 4B)}.$$

The annulus for a 4.36-inch diameter plug should not exceed 0.28 inch (interpolating Figure 6-1).

An annular duct (1-inch diameter) plus a 24-inch length plug ($r \geq 1.5$ inches) will have an exit dose rate of about 19 mR/h.

However, it is better design practice to insert a shield plug into the lower 24-inch length, 1-inch diameter duct.

6.1.1.2 Three-Inch Diameter Plug

With no steps, a minimum exit dose rate of 6,300 mR/h was calculated assuming a 0.1-inch annular gap. This is 250 times the design limit.

6.1.1.3 Six-Inch to 220-Inch Diameter Plugs

No special notes are necessary, except as noted on Figures 6-1 through 6-4, and stated in preceding paragraphs.

6.2 POWER GENERATION AFTER SCRAM

6.2.1 Core Power Generation versus Time After Reactor Scram

Emergency shutdown cooling requirements are dependent on heat generation rates

following reactor scram. The principal sources of heat generation in the core following a scram are:

- Fission power due to delayed neutron emission.
- Fission product decay power.
- U-239 and Np-239 decay power.

Heating caused by neutron activation gamma was omitted. However, this heating is expected to be small in comparison to fission product decay power.

Curves of reactor power versus time after scram are illustrated by Figures 6-5 through 6-10. The first two figures summarize the three principal sources of core power for total periods following scram extending to 36 seconds and 300 seconds.

The accuracy of the curves representing each of the principal sources of power after shutdown is estimated to be:

- Fission power from delayed neutron emission $\pm 30\%$
- Fission product decay power $\pm 10\%$
- U-239 and Np-239 decay power $\pm 50\%$

$$\begin{aligned} \text{Total Core Power} & \left\{ \begin{array}{l} \pm 20\% \text{ for } t_s \leq 20 \text{ sec} \\ \pm 15\% \text{ for } t_s > 20 \text{ sec} \end{array} \right. \\ \text{After Reactor Scram} & \end{aligned}$$

Figure 6-7 illustrates the effect of scram worth on fission power after shutdown due to delayed neutron emission. The three curves include no reactivity feedback effects from Doppler, fuel expansion or contraction, or coolant temperature changes. Figure 6-8 illustrates the effect of such feedback assuming a normal coolant flow coastdown. This feedback assumed the following reactivity coefficients:

$$T \frac{dk}{dT} (\text{Sodium-In}) = (-) 0.005$$

FIGURE 6-5. SHUTDOWN POWER GENERATION VERSUS TIME AFTER REACTOR SCRAM SIGNAL

FIGURE 6-6. SHUTDOWN POWER GENERATION VERSUS TIME AFTER REACTOR SCRAM SIGNAL

FIGURE 6-7. DELAYED NEUTRON POWER VERSUS TIME AFTER REACTOR SCRAM SIGNAL

FIGURE 6-8. DELAYED NEUTRON POWER VERSUS (1) TIME AFTER REACTOR SCRAM SIGNAL, AND (2) SCRAM WORTH (WITH AND WITHOUT FEEDBACK)

**FIGURE 6-9. U-239 AND N_p-239 DECAY HEAT POWER VERSUS COOLING TIME—
FOLLOWING AN IRRADIATION PERIOD OF 10 DAYS OR MORE**

FIGURE 6-10. PLOT OF ANALYTICAL FIT TO: FFTF FISSION PRODUCT DECAY POWER VERSUS COOLING TIME FOR $T \leq 300$ SECONDS

Fuel Axial Expansion = (-) 0.0005

$$\frac{dk(\$)}{dT(^{\circ}F)}$$

Coolant Expansion (Including Core
Structural Expansion) = (-) 0.003

$$\frac{dk(\$)}{dT(^{\circ}F)}$$

Figure 6-9 illustrates U-239 and Np-239 decay power. For the 36-second decay period illustrated by the first figure, the U-239 and Np-239 decay power could have been neglected, being less than 10% of the fission product decay power. However, for decay times in the range of 2 hours to 30 hours the Np-239 decay power is equal to 25% of the fission product decay power.

Finally, Figure 6-10 is an analytical fit to measured fission product decay power for up to 300 seconds (5 minutes) following reactor scram. This curve is based on measurements of fission product decay energy from the thermal neutron fission of U-235. A preliminary evaluation indicates these data are applicable for the fast fission of Pu-239. ⁽¹⁾

6.3 HEAT GENERATION-FUEL TRANSFER

6.3.1 Decay Heat Generation in a Fuel Bundle Following Reactor Shutdown

During fuel bundle transfer from the FFTF core to the decay storage pool, heat will be generated in the fuel bundle and transfer thimble because of the absorption of radiation emitted from radioactive fission products and Np-239 decay. To evaluate fuel bundle cooling requirements during such transfers, it is necessary to know the heat generation rate at the time of the transfer.

For purposes of this study, it was assumed that fuel transfers would take place in the interval of 8 to 24 hours following reactor shutdown. It was further assumed that the full power irradiation period would not be less than 6 months nor greater than 16 months. [To simplify the evaluation, the assumption was also made that the fuel was irradiated continuously for the stated period, with no intermediate shutdowns or operation at reduced power levels. This assumption is conservative.] Finally, the heat generation rates are based on fission product decay heat measurements from the thermal neutron fission of U-235. As noted previously, a preliminary evaluation indicates these data may be used to estimate fission product decay heat rates from the fast fission of Pu-239. ⁽¹⁾

Figure 6-11 and Table 6-1 summarize the results of this evaluation, which shows a range of heat generation rates from a high of 22.9 kW to a low of 15.4 kW. The real significance of these heat generation rates is the subject of a thermal-hydraulics analysis.

The geometry assumed in calculating the amount of gamma radiation which escapes from the bundle and thimble is a cylindricized fuel bundle and flow channel surrounded by a 1.238-inch annulus of sodium and a 0.333-inch wall thickness of stainless steel (thimble). The cylindricized fuel assembly itself has an equivalent diameter of 3.524 inches.

In making the calculations, seven energy groups of fission product decay gammas were considered. No beta radiation was assumed to escape. Concerning the Np-239 decay heat generation, none of the beta or gamma radiation was assumed to escape.

FIGURE 6-11. FFTF HEAT GENERATION RATE IN A FUEL BUNDLE AND TRANSFER THIMBLE VERSUS (1) TIME AFTER SHUTDOWN (8 TO 24 hours)
(2) IRRADIATION TIME (6 TO 16 months)

TABLE 6-1

FISSION PRODUCT AND Np-239 DECAY HEAT ABSORBED (kW) IN AN FFTF FUEL
BUNDLE AND FUEL TRANSFER THIMBLE VERSUS IRRADIATION PERIOD
AND SHUTDOWN TIME

Assumed Power Generation Rate in Fuel Bundle:
3.509 MW Thermal (Approximate Core Average) Fission Product

Irradiation Period	Shutdown Time (h)	Decay Heat Generated			Np-239 Decay Heat	F. P. Gamma Radiation Escaping	Total Absorption Rate
		Beta (kW)	Gamma (kW)	Total (kW)	Total (Beta + Gamma) (kW)	(or Leakage Rate) (kW)	Rate (kW)
6 months	8	8.63	10.95	19.58	3.92	1.49	22.01
	16	6.38	8.80	15.18	3.59	0.94	17.83
	24	5.16	7.73	12.89	3.26	0.78	15.37
10 months	8	9.02	11.22	20.24	3.92	1.53	22.62
	16	6.77	9.06	15.83	3.59	0.96	18.46
	24	5.58	7.97	13.55	3.26	0.81	16.00
16 months	8	9.26	11.30	20.56	3.92	1.54	22.04
	16	6.99	9.17	16.16	3.59	0.97	18.77
	24	5.79	8.08	13.87	3.26	0.82	16.32

SECTION VII

SAFEGUARDS

7.1 ACCIDENT ANALYSIS

A design objective is provision of test locations in the reactor core with a peak neutron flux level of 1.0×10^{16} n/cm²-sec. To attain this with ceramic fuel (UO₂-PuO₂) requires that fuel be operated so that its peak temperature is close to the fuel melting point. From safety considerations one of the primary requirements is the assurance that fuel pin failure (cladding rupture) is highly improbable. The design consequently becomes a trade-off between safety requirements and flux requirements. The nuclear and safety parameters of the backup core have been selected to permit a reasonably conservative approach to be taken to safety evaluation and still meet the design requirement on neutron flux. Because of time and manpower limitations, the analyses

conducted do not represent an exhaustive parametric study of all variables over their full range but provide a set of reference calculations to demonstrate the general safety characteristics of an unzoned core. A change to a two-zone core with the same fuel design criteria is not expected to change the results of the analysis appreciably.

7.1.1 Accident Transients

The potential accident transients that have been identified for further consideration are listed in Table 7-1.

As a partial evaluation of this list of accidents, the following scram-transient analyses of the core have been performed;

a. Reactivity insertion from single bundle meltdown,

b. Reactivity insertion from control rod "ejection"

c. Reduction of primary coolant flow corresponding to primary coolant system failures and fuel channel blockage.

7.1.2 Analytical Procedure

The analytical procedure followed uses the FORE II computer code to generate the temperature and power distribution in the core during a transient.

The following summary is reprinted from GEAP-5273, the program manual.

FORE II is a coupled thermal hydraulics - point kinetics digital computer code designed to calculate significant reactor parameters under steady-state conditions, or as functions of time during transients. The transient may result from a programmed reactivity insertion or a power change. Variable inlet coolant flow rate and temperature are considered. The code calculates the reactor power, the individual reactivity feedbacks, and the temperature of coolant, cladding, fuel, structure, and additional material for up to seven axial positions in three channel types which represent radial zones of the reactor. The heat of fusion accompanying fuel melting, the liquid metal voiding reactivity, and the spatial and the time variation of the fuel cladding gap coefficient due to changes in gap size are considered. FORE II is valid only while the core retains its initial geometry.

The significant material properties are specified in the input as functions of temperature or pressure, and the external effects as a function of time by tables. The feedback reactivity includes contributions due to the Doppler effect, density changes, dimensional changes (includes bowing and deformation), coolant voiding and control rods. Any single-phase coolant may be used. Although the code is biased toward fast reactors, it can be used for a thermal spectrum reactor as a steady-state heat transfer code and as a transient point kinetics code.

TABLE 7-1

DESIGN TRANSIENTS

<u>Number</u>	<u>Name</u>
1.0	Core Disassembly Transient
2.0	Ejection of Control Assembly with Scram
3.0	Single Fuel Assembly Meltdown
4.0	Fuel Assembly Drop-In
5.0	Control Assembly Withdrawal
6.0	Moderator Addition
7.0	Cold Sodium Addition
8.0	Sodium Voiding by Entrained Gas
9.0	Sodium Fire in Closed Inner Containment
10.0	Sodium Fire in Outer and Inner Containment
11.0	Sodium Spray Fire
12.0	Large Leak in Primary Coolant System
13.0	Large Leak in Secondary Coolant System
14.0	Reductions of Primary Coolant Loop Flow
15.0	Reductions of Secondary Coolant Loop Flow
16.0	Reductions of Forced Air Cooler Flow
17.0	Loss of Generated Electrical Power
18.0	Large Leak in Closed Test Loop Primary Coolant System
19.0	Large Leak in Closed Test Loop Secondary Coolant System
20.0	Reductions of Closed Test Loop Primary Coolant Flow
21.0	Reductions of Closed Test Loop Secondary Coolant Flow
22.0	Reductions of Closed Test Loop Forced Air Cooler Flow
23.0	Reductions of Open Test Loop Coolant Flow

The programming language is FORTRAN IV. A computer with a 32-K storage capacity is required for the operation of FORE II.

For the analyses conducted, the three channel types represented were:

- a. Central channel including local power peaking caused by nonuniformities and tolerances;
- b. Central channel excluding local power peaking;
- c. Average power channel.

7.1.3 Fuel Damage Limits

These were presented in the Third Quarterly⁽¹⁾ in detail and can be summarized as follows:

Peak fuel temperature to be less than fuel melt temperature except for ultra-low probability accidents such as control rod shoot-out.

Peak cladding temperature limit 1300°F except for ultra-low probability events such as rod shoot-out and guillotine pipe failure.

Peak fuel temperature for rod shoot-out to be such that less than 40% of cross-sectional area of a new fuel pin becomes molten.

7.1.4 Trip Setting

1. Reactor is controlled to be $\pm 2\%$ from indicated power.
2. Tolerance on indicated power 8%.
3. Maximum actual S.S. power in core for $108\% \pm 2\%$ operation = 110%
4. Trip setting on flux monitors 110% indicated power.
5. Trip occurs at actual power of $< 120\%$.

7.1.5 Control Reactivity

Figure 7-1 shows the delay time, control worth, and insertion rate used in the analyses. A control system with a 10\$ scram worth at 1 g acceleration and 0.150 sec delay is the current reference for drive design. The curve used for analysis is conservative to allow for possible variations between anticipated and real drive behavior.

7.1.6 Flow Coastdown

Figure 7-2 shows the curve assumed. It is taken from the FERMI pump characteristics, since transient systems analyses have indicated its suitability. These were presented in the Third Quarterly Report.⁽¹⁾

7.1.7 Reactivity Transients

Figures 7-3, 7-4, and 7-5 show the percent melting at core hot spot, peak cladding temperature, and outlet sodium temperature as a function of ramp rate for an insertion of 1.5\$ and 0.5\$. The 1.5\$ was selected as the approximate maximum worth of the pair of elements in a reflector control drive assembly, and 0.5\$ as the worth associated with a single bundle meltdown. Calculations were performed for Doppler feedback coefficient ($T dk/dT$) values of -0.006 and -0.004. The Doppler coefficient is currently estimated to be about -0.006 but because of uncertainties in this estimate -0.004 is selected for reference calculations.

From these curves it can be seen that a reactivity ramp up to 2\$ per second can be tolerated without any fuel melting and 4\$ per second causes about 40% of the fuel pin to be molten at the core hot spot. The amount of over-temperature that can be tolerated on the cladding is difficult to define with precision. The capability of cladding to withstand the fission gas pressure is decreasing rapidly with

FIGURE 7-1. CONTROL REACTIVITY

FIGURE 7-2. PUMP COASTDOWN (FERMI—12000 gpm)

FIGURE 7-3. PERCENT MELTING AT CORE HOT SPOT REACTIVITY TRANSIENTS

FIGURE 7-4. PEAK CLADDING TEMPERATURE DURING REACTIVITY TRANSIENTS

FIGURE 7-5. PEAK COOLANT OUTLET TEMPERATURE REACTIVITY TRANSIENTS

temperature. It is anticipated that 100°F over-temperature would result in negligible cladding damage. Figure 7-4 shows the peak cladding temperature rises very rapidly with ramp rate and to reduce the probability of cladding rupture and the consequent problems associated with this such as fuel sodium interactions the ramp rate associated with accidents such as rod shoot-out should be limited to 2.5\$/sec even though the fuel melting criteria would allow up to 4\$/second.

7.1.8 Rod Shoot-Out

The control drive assembly shall be designed so that rod shoot-out is a virtually incredible event requiring at least two independent failures, however, the mechanism used to raise the rod is a potential energy source, and also the hydraulic forces on the element are in an upward direction. The hydraulic forces will be less than the element weight due to orificing of the coolant at the bottom of the channels.

For the current reference, the worth of a double reflector control rod in the most reactive geometrical arrangement is approximately 1.5\$, with a 3-foot stroke.

To keep the average ramp rate at 2\$/second, the time for ejection should not be less than 0.75 second. Assuming a constant acceleration $a < 25/t^2 = 6/0.75^2 = 10.6 \text{ ft/sec}^2 \sim 1/3 \text{ g}$. The upward forces on the moving subassembly in the control drive assembly must be limited to $4/3 \times$ the weight of the moving assembly, for any credible failure or malfunction.

7.1.9 Bundle Slump

The reactivity worth of single bundle meltdown is estimated at 50¢ for a slumping of the upper third of the fuel into the middle third. The rate is determined by free fall under gravity resulting in an average ramp

rate of 2\$/second, which results in no fuel melting. A conservative reference point is plotted on the figures for a 4\$/second ramp rate.

7.1.10 Flow Transients

Figure 7-6 shows the outlet temperature of a hot channel and the peak cladding hot spot temperature for a normal scram initiated at 110% power. This would cover the case of a scram signal from loss of site power, secondary system failures, etc. For an accidental overpower trip at 120% power, the dotted curves of Figure 7-6 show the temperature response of hot channel outlet coolant and local cladding hot spot. The peak cladding temperature of less than 1300°F is considered acceptable during a transient.

Figures 7-7 and 7-8 show the temperature response of the reactor coolant to varying degrees of rapid blockage of a single central fuel subassembly and the time for the event to be sensed by the channel outlet temperature instrumentation. These indicate the response time on outlet thermocouples need not be very fast and flux will be the fast signal for power transients.

7.2 SAFEGUARDS DURING REFUELING

A series of FORE II runs were made in which a bundle of nominal worth (2.20\$) and above nominal (3.00\$) were inserted into a just subcritical core at refueling temperature without scram. A range of Doppler coefficient from -0.004 to -0.008 was used along with a core coolant flow rate of 5% and 10%. A 1 g and 1/2 g drop rate was assumed and sodium boiling occurred in 1 to 3 seconds for all cases. The analysis was not taken to the point of a core slumping energy release but since coolant boiling occurs, an explosion of some magnitude induced by the reactivity insertion of sodium voiding and fuel slumping is quite probable. It

FIGURE 7-6. PEAK COOLANT AND CLADDING TEMPERATURES NORMAL SCRAM WITH FLOW COASTDOWN

FIGURE 7-7. COOLANT TEMPERATURE RESPONSE TO BLOCKAGE

FIGURE 7-8. TIME TO REACH BOILING IN HOT CHANNEL AS FUNCTION OF BLOCKAGE

is felt that the consequence to the design requirements on the refueling cell would be sufficiently severe that engineered safeguards to avoid criticality during refueling is the approach which should be followed.

7.3 SUBCRITICALITY MEASUREMENT

Reasonably accurate measurements of the subcritical reactivity are desirable for the safe operation of FFTF during shutdown, refueling, and startup procedures. These measurements are possible using a variety of methods. Several possible methods are; subcritical source multiplication or withdrawal of calibrated control rods, measurement of the response to applied perturbations, or measurement of the intrinsic neutron fluctuations or noise. Because of the potential advantages of determining subcriticality from the neutron fluctuations, a preliminary study has been initiated to evaluate the practicality of this scheme as applied to the FFTF Backup Design. The results of this evaluation will be:

- a. The selection of the most satisfactory noise analysis technique.
- b. An estimate of the feasibility of neutron noise analysis as a means of measuring subcritical reactivity.

- c. Possible advantages and disadvantages of noise analysis compared to more established methods.

The neutron noise analysis method that appears to be best suited for application to FFTF is the Two Detector, Two Channel Cross Correlation technique suggested by W. Seifritz of Karlsruhe.⁽⁵⁾ Since noise analysis involves stochastic rather than deterministic processes, the accuracy of a noise analysis measurement is a direct function of the length of data recording time. Preliminary analysis of this aspect of the neutron noise method indicates that for reasonable detector size and location near the FFTF core, acceptable accuracy may probably be obtained in a time less than or comparable to that required to transfer a fuel element to the fuel storage pool and be replaced by a fresh element.

Future work will include further analysis of the statistical accuracy obtainable with noise analysis, preliminary investigation of hardware and electronics problems, and a quantitative comparison of noise analysis with more conventional techniques for measuring subcriticality.

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